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Mirror Advanced Reactor Study Interim Design Report

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ABSTRACT

The status of the design of a tenth-of-a-kind commercial tandem-mirror fusion reactor is described at the midpoint of a two-year study. When completed, the design is to serve as a strategic goal for the mirror fusion program. The main objectives of the Mirror Advanced Reactor Study (MARS) are: (1) to design an attractive tandem-mirror fusion reactor producing electricity and synfuels (in alternate versions), (2) to identify key development and technology needs, and (3) to exploit the potential of fusion for safety, low activation, and simple disposal of radioactive waste. In the first year we have emphasized physics and engineering of the central cell and physics of the end cell. Design optimization and trade studies are continuing, and we expect additional modifications in the end cells to further improve the performance of the final design.

1. INTRODUCTION

The Mirror Advanced Reactor Study (MARS) is a major two-year study of a commercial tandem mirror reactor, conducted by the Lawrence Livermore National Laboratory (LLNL) for the U.S. Department of Energy, with TRW as a prime industrial partner, General Dynamics, EBASCO, and SAI as subcontractors, and the University of Wisconsin as a university partner. Grumman Aerospace is also contributing to the engineering effort. The main objectives of MARS are: (1) to design an attractive fusion reactor producing electricity and synfuels in a tenth-of-a-kind commercial stage of development; (2) to identify key development and technology needs for the commercial tandem mirror reactor and its forerunner, the Fusion Power Demonstration reactor (FPD); and (3), to exploit the potential of fusion for safety, low activation, and simple disposal of radioactive waste. Currently at the midpoint of this study, we describe the general characteristics of the MARS reactor; however, some design trade studies are still being explored. Many MARS features have been part of past mirror experiments or can be tested in near-term mirror facilities such as TARA, the Tandem Mirror facility at MIT, TMX-U, the Tandem Mirror Experiment-Upgrade at LLNL, and MFTF-B, the Magnetic Fusion Test Facility at LLNL.

MARS (Fig. 1.1-1) is a linear magnetic-mirror fusion device using electrostatic plugs to confine a steady-state fusion plasma in a long solenoid (typically 150 m long to produce 1500 MWe net). Mirror devices with the topology of the MARS plugs have been part of fusion research since the beginning of the program in the early 1950s. High-beta quasi-steady-state mirror plasmas were first demonstrated at LLNL in 1975 in the 2XIIB single-mirror device. The tandem mirror concept began in 1976 as an improvement over single-mirror concepts. It has two mirrors, one at each end of a solenoid. The basic principles of electrostatic confinement in tandem mirrors were first demonstrated at the proof-of-principle level in the TMX and Gamma-6 experiments in 1979. The main fusion plasma in the central cell of MARS is self-sustained by alpha heating (ignition), while continuous injection of neutral beams and electron cyclotron resonant heating (ECRH) are required to maintain the plug electrostatic confining potentials of 150 kV for the ions and 200 kV for the electrons. The plug injection power is reduced by the use of thermal barriers. Thermal barrier formation and microstability will be tested in the TMX-U experiment during the next two years and at near reactor-level energies

Fig. 1.1-1. MARS reactor design.

in MFTF-B beginning in 1986. Plasma exhaust and vacuum pumping are accomplished in large vacuum tanks at the ends, where direct conversion of escaping ion and electron energy recovers about as much electricity as is consumed by the plug beams and ECRH. A small direct-converter module was successfully operated as part of TMX in 1980.

The present MARS end plugs (Fig. 1.1-2), which are the same axicell type that will be used in TARA and MFTF-B, rely on proven yin-yang magnets for high-beta magnetohydrodynamic (MHD) stability. The MARS yin-yang size and field are comparable to the MFTF-B yin-yang recently tested at LLNL. All reactor magnets are steady-state because no plasma current needs to be driven to provide plasma equilibrium or stability. The transition coils are the same generic double-ellipse-type that were recently designed for MFTF-B to cancel parallel components of plasma diamagnetic current in the central cell and to control radial transport in the transition regions.

Steady-state fueling in the tandem mirror reactor most likely will be accomplished by neutral beams in the plugs and by injection of high velocity (10^4 -m/sec) pellets near the ends of the central cell. Impurities and thermal alpha ash are kept to tolerable concentrations ($< 10\%$) by radial transport. While the axicell is designed to reduce neoclassical (resonant) diffusion for most central-cell ions, residual transport due to the double-ellipse transition coils can be designed to induce radial ion flux comparable to end losses, sufficient for $n_\alpha/n_{DT} \lesssim 10\%$. The alpha ash and impurities diffuse into an outer radial plasma zone, or halo, which is not electrostatically plugged. This natural diverter sweeps the impurities into the large direct converter tanks, thus minimizing plasma bombardment and surface heating of the first wall in the central cell.

Characteristic MARS parameters are listed in Table 1.1-1. Two particular features stand out. First, the plasma radius (0.43 m) is small because of high beta, which permits relatively small-bore magnets (2.4-m inner radius central solenoids, 30-cm inner radius 24-T hybrid coils, 3-m major radius yin-yangs). Thus, the intermediate step FPD can be constructed with full-scale reactor plug magnets at reasonable coil cost and development. Second, significant recirculating electric power is required to drive the plugs,

Fig. 1.1-2. MARS axicell configuration.

Table 1.1-1 Characteristic MARS parameters.

Central cell length	150 m
Plasma radius	0.43 m
Solenoid field	4.7 T
Peak axicell mirror field	24 T (16 T SC + 8 T Cu insert)
Yin-yang mirror field	6 T (8.5 T on the conductor)
Average central beta	40%
Average central density	$3 \times 10^{20} \text{ m}^{-3}$
First wall loading	5 MW/m^2 (max at $r_{\text{min wall}} = 0.6 \text{ m}$)
Fusion power	3500 MW
Plug neutral beam power absorbed	76 MW each plug (4 MW) ^a
Plug ECRH power absorbed	28 MW each plug
Plug ICRH power absorbed	20 MW each plug
Normal insert coil power	63 MWe each plug
Total recirculating power	590 MWe (380 MWe) ^a
Direct converter power	445 MWe
Net electric power produced	1360 MWe (1570 MWe) ^a
Recirculating power fraction	0.30 (0.19) ^a
Magnet stored energy	50 GJ
Magnet + shield + blanket mass	20,000 tons

Assumed Efficiencies

Direct converter	0.55
475-kV sloshing ion beams	0.66
97-kV pump neutral beams	0.69
ECRH	0.65
Axicell ICRH	0.50
Thermal cycle	0.39
Overall system (plant)	0.30

^aAssuming improved transition pumping using radial transport (drifts) instead of 97-kV pump beams. The only remaining neutral beams are the 475-kV sloshing ion beams.

particularly the transition neutral beam pump power. The direct converter output keeps P_{net} and plant efficiency respectable, but recirculating power is high, which will make reactor cost/ kW_e (net) sensitive to the neutral beam and rf heating costs. However, with improved transition pumping such as the use of radial drifts, the recirculating power might be cut nearly in half (quantities that change have an asterisk in Table 1.1-1). An attractive current scheme of employing AC perturbations in the transition regions is being evaluated for the engineering requirements of frequency, bandwidth, and amplitude. Assuming that the engineering requirements look feasible and that the scheme can be tested in TMX-U or MFTF-B, AC drift pumping could qualify as the MARS baseline pumping method early in FY 1983.

At this point in the MARS study, we can already identify several key development and technology items that will be needed: Negative-ion beams (at least for the 475-kV sloshing beams), high-field 24-T hybrid solenoid coils with unshielded normal inserts, direct converters, and high-velocity (10^4 -m/sec) pellet injectors. Improved transition pumping may require new hardware, but that cannot be specified as yet. Among the long-lead-time items necessary to enable a MARS-type FPD to be built, the negative-ion beams are most crucial. New materials for blankets and other structures are also long-lead-time items for development, but we mainly need to select and screen promising candidates for final testing in the later phases of the FPD (end of the 1990's).

During the past nine months of the MARS study, we have made significant advances in designing higher temperature, higher efficiency blankets with low tritium inventory and reduced activation. The University of Wisconsin's $\text{Li}_{17}\text{Pb}_{83}$ blanket, building on extensive previous work from the UW-Witamar design, has been improved and optimized for lowest-cost electricity production for MARS (Fig. 1.1-3). With a novel, high performance double-wall heat-exchanger, this design could have an overall steam-cycle efficiency of 40%. Combining this efficiency with a high blanket energy multiplication (1.39), a thin blanket (38 cm thick), and smaller-radius solenoid coils (210 cm inner conductor radius), this design is expected to minimize the capital cost of the reactor per kW of net electricity produced. Although costing, reliability, safety, and remote maintenance are detailed subjects to be addressed more

Central cell
(normal portion)

Fig. 1.1-3. University of Wisconsin's lithium lead blanket design.

during FY 1983, the simplicity and ruggedness of this design is expected to set the lowest-cost standard for electricity production in MARS. The primary critical feasibility issue for this design is the deposition of the corrosion products in the flowing $\text{Li}_{17}\text{Pb}_{83}$ liquid metal loop at 500°C. Data on this critical materials and liquid-metals-handling issue for $\text{Li}_{17}\text{Pb}_{83}$ are being addressed by laboratory-scale experiments over the next few years.

Beginning as a proposal concept for achieving higher temperatures for more efficient electricity and/or synfuels production, the TRW blanket design (Fig. 1.1-4) has rapidly evolved into a technically credible blanket capable of delivering $\geq 50\%$ of the blanket heat in the form of 900°C, 80 atm helium. Using $\text{Li}_{17}\text{Pb}_{83}$ as a first wall coolant, neutron multiplier, and supplemental tritium breeder, this design has good breeding at low tritium inventory, with overall performance ($\eta_{th} = 44\%$ at $M = 1.16$ blanket multiplication) for electricity production comparable to the UW $\text{Li}_{17}\text{Pb}_{83}$ design. It also generates more than enough high-temperature helium to drive a thermal SO_3 decomposer in a thermochemical synfuel process. Critical issues for this design are the reliability, complexity, and cost for the blanket and associated balance of plant, and for synfuels, tritium isolation from the synfuel product.

In both the UW and TRW blanket designs, the use of $\text{Li}_{17}\text{Pb}_{83}$ helps keep tritium inventory low, but tritium permeation is instead the issue to address in the heat exchanger design. Ferritic steels have been shown to be compatible for use in the magnetic fields of the central cell.

Because lithium-oxide has been the solid breeding material most often studied in the worldwide fusion effort, and because liquid-metal technology may itself become an issue, Grumman has investigated a dry, He-cooled solid Li_2O blanket design to see if this type of blanket could be compatible with a MARS central cell. They have developed a canister-type Li_2O blanket (Fig. 1.1-5) that can operate at the high wall loadings ($\Gamma_{wall} = 5 \text{ MW/m}^2$) required for MARS. Like the UW and TRW designs, this design can use ferritic steels for longer life and vanadium alloys for lower activation. The critical issue for this design, as with all previous Li_2O designs, is sintering of the high temperature Li_2O , resulting in unknown, but perhaps large, inventories of tritium. Connected with this problem is the overall thermal cycle efficiency at the lower He temperatures and higher He pumping power required to cool the

Fig. 1.1-4. High-temperature blanket design.

Fig. 1.1-5. Li_2O blanket design.

first wall. It is essential to obtain more experimental data on tritium release and sintering of Li_2O at higher temperatures to further advance this type of blanket.

To focus more effort in FY 1983 on the design of end plug heating systems, remote maintenance of blankets and end plug components, and the design of facilities for rad waste handling and disposal, it will be necessary to select a reference case blanket for electricity production, based on comparisons of performance, cost, and simplicity of remote handling.

In addition to the primary task of designing and costing a reference case MARS plant that produces electricity, the MARS team intends to modify the MARS design in FY 1983 to produce synfuels. As the prime industrial partner and systems integrator for MARS, TRW has responsibility for the synfuel task, with assistance from other synfuel groups. TRW has reviewed existing blanket and synfuel processes and has made the following recommendations to the MARS executive committee and DOE:

1. MARS can and should pursue a synfuel plant design in FY 1983.
2. The synfuel process should be thermochemical water-splitting using the sulfur-iodine process with a thermal SO_3 decomposer.
3. The TRW/GD high temperature blanket should be used.

The high temperature electrolysis (HTE) process is not recommended for MARS because the blanket concepts for use with high temperature steam are inadequately developed. However, because of the simplicity and potential performance of HTE, we recommend that research on a suitable blanket for high temperature steam continue independently.

To date, we have devoted a modest effort to the generic fusion problems of safety, activation, remote maintenance, and waste disposal. Once blanket concepts are selected for electricity and synfuels production, we can devote more effort to these issues. The UW and TRW blankets use nonflammable blanket coolants and have low tritium inventories, which should reduce the severity of possible accidents. Afterheat and loss-of-coolant accidents are just now being assessed to determine if backup coolant loops will be required. An important accomplishment in the waste area is the preliminary assessment of how well various structural steel alloys would satisfy NRC rules (10CFR61) for

Table 1.1-2 Waste disposal ratings (WDR) for the NRC's 10CFR61 rules.
First wall: MARS (TRW) @ 5 MW/m² and 3 yr (parent elements in parentheses).

Alloy	WDR ^a						Total Alloy
	Ni-59	Ni-63	Nb-94	Mo-93	Tc-99	Mn-53	
SS316	22.7(Ni)	71.4(Ni)	1.5(Mo)	25.5(Mo)	3.67(Mo)	0.351(Fe)	125
PCA	26.4(Ni)	82.9(Ni)	1350(Nb)	18.2(Mo)	2.67(Mo)	0.354(Fe)	1480
HT-9	0.818(Ni)	2.57(Ni)	0.5(Mo)	9.09(Mo)	1.33(Mo)	0.464(Fe)	15
2.25 Cr-1Mo	0.818(Ni)	2.57(Ni)	0.5(Mo)	9.09(Mo)	1.33(Mo)	0.509(Fe)	15
Tenelon	--	--	--	--	--	0.4(Fe/Mn)	0.40
Nomagne	3.64(Ni)	11.3(Ni)	--	--	--	0.459(Fe/Mn)	15
NM-1	--	--	--	--	--	0.46(Fe/Mn)	0.46
25-5-1	1.95(Ni)	6.14(Ni)	3150(Nb)	9.09x10 ⁻⁵ (Nb)	--	0.411(Fe/Mn)	3159
32-7	0.364(Ni)	1.18(Ni)	--	--	--	0.39(Fe/Mn)	1.93
JUS289-N	--	--	--	--	--	0.431(Fe/Mn)	0.43
JUS289-V	--	--	--	--	--	0.430(Fe/Mn)	0.43

$$^a\text{WDR} = \sum_i A(i)/L(i),$$

A(i) = activity of ith isotope in Ci/m³, and

L(i) = maximum permissible activity of ith isotope (Ci/m³) from 10CFR61.

If WDR < 1, material can be disposed of by near-surface burial without processing.

near-surface burial (Table 1.1-2). Satisfying such rules would greatly simplify remote replacement, reprocessing, and disposal of activated commercial reactor components. One of our goals with our fusion reactor design is to satisfy such rules while at the same time streamlining the required remote maintenance procedures, reducing the required number of hot-cell facilities, and minimizing the risk of accidents to the plant workers and to the public. All three current blanket designs use low-nickel ferritic steels (such as HT-9), which come closer to near-surface burial guidelines than does stainless steel, but which still exceed those guidelines by a factor of 10 to 20. We have identified three choices for consideration: (1) Processing the material for suitable dilution in alloys or concrete. This would entail more hot-cell facilities and would generate larger volumes of waste to transport and bury. (2) Isotopic tailoring of the molybdenum component in the alloy, which may be expensive. (3) Developing new alloys to meet the NRC criteria without having to process the blanket material. This choice will require the longest time to develop materials and therefore may be impractical for a nearer-term device such as FPD. However, for the long term, new alloys would benefit a large number of commercial reactors like MARS because the use of these alloys could eliminate reprocessing steps and reduce on-site facilities for such reprocessing. A major limitation with choice 3 is that the alloys identified in Table 1.1-2 lack sufficient data on neutron damage properties to permit redesign now of any of the MARS blankets. Therefore, prudence dictates that remote maintenance and waste processing facilities be designed for the MARS reference case blanket according to choice (1) above; i.e., 20X dilution of HT-9. However, to illustrate the advantages of new alloys that meet NRC criteria without post-irradiation processing, a simpler, direct-disposal scheme, such as that sketched in Fig. 1.1-6, will also be designed for comparison. This simpler remote-handling and disposal scheme would also be available if choice 2 turned out to be cost-effective.

Fig. 1.1-6. Radioactive waste direct disposal scheme.

2. PLASMA ENGINEERING

2.1 MAGNETIC CONFIGURATION

2.1.1 Central-Cell Design

2.1.1.1 Magnet Description

The central cell magnet set consist of 48 identical solenoidal coils with axial spacing (coil midplane to midplane) of 3.16 m. Conductor bundles for each coil are 0.884 m wide and 0.932 m thick with an inner radius of 2.44 m. The conductor bundle has three layers, as shown in Fig. 2.1-1, each with a different bulk current density. The total current in each central cell magnet is 12.1 MA. The nominal, on-axis central-cell magnetic field is 4.7 T.

2.1.1.2 Design Considerations

To permit the use of niobium-titanium superconductor in the central cell magnets, the maximum conductor field strength was specified to be less than 8 T. Figure 2.1-2 shows contours of constant magnetic field strength superimposed on the cross section of a central-cell conductor bundle, spaced 0.25 T apart. The figure shows the peak field strength at the inner radius and midplane of the coil to be about 7.1 T.

For a self-supporting conductor in a solenoidal coil, the conductor hoop stress is $\sigma = JBr/f$, where J is the bulk current density, B is the local axial field strength, r is the local radius, and f is the conductor packing fraction. For the central cell coils, σ was specified to be less than 280 MPa (40 ksi). Table 2.1-1 gives the calculated hoop stress at the inner surface of each conductor bundle layer, using the current densities from Fig. 2.1-1, the field values from Fig. 2.1-2, and a conductor packing fraction of 0.8.

Because the central-cell magnet set comprises axially separated coils, there is an axial ripple superimposed on the nominal 4.7-T solenoidal field. For the magnet set described above, the minimum and maximum on-axis field strengths are 4.67 and 4.85 T, respectively, giving a field ripple of 3.8%.

Fig. 2.1-1. Central-cell coil conductor bundle.

Fig. 2.1-2. Magnetic field contours in central cell coil.

Table 2.1-1. Hoop stress at inner surface of each conductor bundle layer of central cell coil.

	$J(\text{MA/m}^2)$	$B(\text{T})$	$\sigma(\text{MPa})$
Inner layer	10.78	7.1	230
Middle layer	11.91	5.7	230
Outer layer	19.10	3.5	250

Magnetic field ripple in the central cell causes a reduction in the MHD stability limit for β in the central cell. This is because plasma pressure p is axially uniform, and B is minimum where normal curvature is unfavorable and maximum where it is favorable. Thus, the p/B -weighted unfavorable curvature dominates, and the central-cell makes a small but negative contribution to stability. (See Section 2.2 for a more complete discussion of MHD stability.) Stability calculations for two values of central-cell field ripple yielded a central-cell β limit, $\beta_c = 0.7$ for no ripple, and $\beta_c = 0.65$ at 7% ripple. We were guided by these calculations to choose 4% as a design value for ripple, with the expectation that such ripple will not degrade the stability more than a few percent.

A study of axial coil spacing vs conductor bundle inner radius for constant field ripple is shown in Fig. 2.1-3. For this study, the nominal central-cell field was 4.7 T, the field ripple was $\sim 4\%$, the conductor bundle cross section was square, and the cross section dimensions and current density were chosen to limit the maximum conductor field to $< 8\text{T}$ and the hoop stress to $< 280\text{ MPa}$. The MARS baseline design point, $r_i = 2.44\text{ m}$ and $\lambda = 3.16\text{ m}$, is circled on Fig. 2.1-3. Note that the MARS baseline design for central-cell magnets is dimensionally consistent with the high-temperature blanket design (Section 4.3) but not with the LiPb blanket (Section 4.2). For the LiPb blanket, the coil axial spacing remains at 3.16 m but the inner radius is reduced. This will increase the ripple somewhat above 3.8%, but the additional degradation of stability is expected to be small.

Fig. 2.1-3. Axial coil spacing vs inner radius for constant field ripple.

There is a mutual attraction between any two solenoidal coils on the same axis with like current directions. A central-cell coil immersed in a line of many other coils has no net magnetic body force acting upon it. However, depending on the energizing/deenergizing sequence for the MARS coils, large net body forces can be expected on central-cell coils. As an upper limit, we have calculated the net axial body force on an energized central-cell coil at one end of a long string of identical energized coils. The forces are given in Table 2.1-2 for each conductor bundle layer. The face pressure is the axial force divided by the area of the end of the annular conductor bundle layer.

Table 2.1-2. Central cell coil forces.

	Axial force (N)	Face pressure (MPa)
Inner layer	1.85×10^7	5.0 (720 psi)
Middle layer	3.03×10^7	5.8
Outer layer	7.36×10^7	9.2

2.1.2 End Plug Design

2.1.2.1 End Plug Configuration

The MARS end plug coils, along with one quadrant of the magnetic flux bundle, are shown in Fig. 2.1-4. The solenoid at the left of the figure is the axicell. The resistive insert coils, which produce a mirror region in the axicell, are not visible in the figure.

The two coils following the axicell are transition coils of the double-ellipse type, as evidenced by the flaring of the flux bundle, first in the horizontal plane, then in the vertical plane. This feature is important for limiting the radial transport of passing ions (see Section 2.2).

Fig. 2.1-4. MARS end plug.

The final coil pair is the yin-yang anchor, which maps the elliptical flux bundle of the second transition coil through a minimum-B mirror cell. This cell provides the primary good magnetic curvature for MHD stability (hence the name "anchor"), and is also the location of the thermal barrier and the confining potential peak.

The variation of magnetic field along the axis of the MARS reactor, as calculated by the magnetic field code EFFI, is shown in Fig. 2.1-5. The axicell mirror fields are 21.1 and 24.3 T; the axicell midplane field is 17.4 T. The minimum transition region field is 1.44 T. The anchor mirror fields are 6.8 and 6.1 T; the anchor midplane field is 2.1 T. Note that these fields are slightly different from the nominal values used in the plasma performance analysis (see Section 2.10). In the final MARS design, these two sets of field values will be made consistent.

2.1.2.2 Axicell Coils

The axicell is an axisymmetric mirror cell immediately adjacent to the end of the central cell. The purpose of the axicell is to limit the velocity distribution of central cell ions that can pass to the end plug, thus reducing the requirement for trapped particle pumping in the transition and barrier regions. It does this in two ways: the high mirror field is a magnetic reflector and the potential of the mirror-confined axicell plasma is an electrostatic reflector.

The high magnetic field strengths of the axicell are produced by a hybrid coil arrangement consisting of a superconducting outer coil and normal-conducting insert coils, all simple solenoids. The design of these coils has evolved with the axicell physics scenario. An earlier MARS design, using neutral beam injection to sustain the axicell plasma, had an axicell coil set consisting of four separate coils: two insert coils and two superconducting outer coils. The axial gap between these coils was used for neutral beam injection. The present MARS design, with an ICRH-sustained axicell, does not need an axial gap in the outer coils. An axially continuous superconducting coil provides a background magnetic field slightly above 15 T (nearly uniform over the volume of the axicell plasma) and the insert coils provide the incremental mirror fields.

Fig. 2.1-5. On-axis magnetic field strength in MARS.

Figure 2.1-6 shows the conductor bundle cross sections of the axicell magnets and the contours of constant magnetic field strength. The axicell is centered at $z = 81.2$ m and the two insert coils are centered at $z = 80.2$ and 82.2 m. The minimum inner radii are 0.32 m for the 21-T insert coil, 0.30 m for the 24-T insert coil, and 1.4 m for the superconducting coil. The cross sections for the insert coils are 0.282×0.698 m and 0.420×0.858 m, and the bulk current density is 2800 A/cm^2 . These cross sections and current densities are for an externally cooled conductor design for the insert coils.

In revised design using an internally cooled conductor, now chosen as the MARS baseline, the cross sections are 0.317×0.720 m and 0.471×0.896 m, and bulk current density is 2500 A/cm^2 . The constant-B contours for the internally cooled design are very similar to those in Fig. 2.1-6. The internally cooled design, although having higher resistive losses, was selected to avoid dielectric breakdown in the high field and radiation environments. The total coil currents for the insert coils are 5.5 and 10.1 MA. The cross section for the superconducting coil is 1.03×5.30 m and is divided into four equal thickness layers with bulk current densities of 1050 (inner layer), 1050, 1428, and 2222 A/cm^2 . The total coil current for the superconducting coil is 78.5 MA.

As shown in Fig. 2.1-6, the peak conductor field strength in the axicell superconductor is between 15 and 16 T, and occurs at the inner surface and axial midplane of the coil. With the insert coils not energized, the peak conductor field strength increases to 16.0 T.

2.1.2.3 Transition Coils

The transition coils are located at $z = 86.1$ and 90.6 m (centers of curvature of major arcs). The first transition coil has equal major and minor arc radii of 1.25 m. Major arcs are 210 deg in sweep angle; minor arcs are 180 deg. The cross section of the conductor bundle is 0.46×0.46 m, and the bulk current density is 3000 A/cm^2 , for a total coil current of 6.4 MA.

The second transition coil has a major arc radii of 2.0 m and a minor arc radii of 1.25 m. Major arcs are 240 deg in sweep angle; minor arcs are 180 deg. The cross section of the conductor bundle is 0.53×0.53 m, and the bulk current density is 2900 A/cm^2 , for a total coil current of 8.1 MA. The peak conductor field in each of the two transition coils is 6.8 T.

Fig. 2.1-6. Axicell coils and magnetic field contours.

2.1.2.4 Anchor Coils

The yin-yang anchor coils are located at $z = 97.1$ and 98.1 m (the centers of curvature of the major arcs). The two coils are identical, with major arc radii of 3.0 m and minor arc radii of 0.8 m. Major arcs are 160 deg in sweep angle, and minor arcs are 180 deg. The cross section of the conductor bundles is 0.32×1.85 m, and the bulk current density is 2750 A/cm^2 , for a total current in each coil of 16.3 MA. The peak conductor field in each anchor coil is 8.5 T at the inner surface of the minor arcs.

2.2 PHYSICS CONSTRAINTS OF THE MAGNETIC CONFIGURATION

Identifying the physics constraints of axicell magnetic configurations and developing methods to satisfy those constraints has been an area of intense effort. For MARS, we have not yet satisfied all of the constraints, and our continuing work will concentrate on the remaining problems. In this section we discuss the nature of these constraints and the status of our efforts to satisfy them. A more detailed discussion of the constraints and a demonstration of a satisfactory solution for MFTF-B has been completed.¹

We last discussed the physics constraints of the magnetic configuration for an axicell reactor in Ref. 2. Since then, the problem has changed in a number of ways:

1. The relocation of the thermal barrier and confining potential peak to the anchor changed the plasma pressure function. Most significantly, the pressure in the transition region becomes non-negligible because of the passing ions there. The pressure in the transition region (normalized to that in the central cell) is now estimated to be $0.5 B/B_{m2}$, where B is the local field in the transition region and B_{m2} is the outboard axicell mirror field (24 T in MARS). Because the transition region has mostly bad curvature, this new pressure function makes the MHD stability criterion more difficult to satisfy.
2. The work of Cohen and Byers³ has shown that to avoid large stochastic radial transport, we must balance positive and negative geodesic curvature ($I \cdot O$ in the transition region) so that it nearly nulls the line integral of $I \cdot O$ from the axicell to the location of the potential peak. This constraint is the reason for the introduction of the double-ellipse transition coil design. Satisfying this constraint will also nearly null the parallel current through the central cell, which is the geodesic curvature-related criterion previously considered.²
3. The $m = 1$ ballooning criterion for MHD stability⁴ has been shown to be more severe than the flute interchange criterion. However, we have seen that the $m = 1$ criterion approaches the interchange criterion if two conditions are met: (a) the magnetic flux between the first wall and the

plasma is not large compared to that contained within the plasma, permitting effective wall stabilization (true for MARS); and (b) the good curvature on a 45-deg field line in the anchor is symmetric about the anchor midplane. The latter condition ensures that stabilization on all field lines, in particular the 0- and 90-deg field lines, is provided locally at each end of the machine, important for z-dependent ballooning. In the MARS study, we have continued to use the interchange criterion as a measure of MHD stability, but with the above condition as an additional constraint.

2.2.1 Radial Transport

The MARS double-ellipse transition coil design has been adjusted to null the line integral of the geodesic curvature up to the location of the potential peak,

$$\int_{\text{axicell}}^{\text{potential peak}} \mathbf{E} \cdot d\mathbf{s} = 0. \quad (2.2-1)$$

Figure 2.2-1(a) shows the axial profile and Fig. 2.2-1(b) shows the progressive integral of the geodesic curvature function. Note the balancing of positive and negative contributions in both the transition ($z = 82.2$ to 94 m) and anchor ($z = 94$ to 101 m) regions.

Byers³ has shown that large stochastic radial transport would occur in MARS even with $\int \mathbf{E} \cdot d\mathbf{s} = 0$, unless the radial electric field is negative in the transition region. The negative electric field causes a cancellation between the electric- and curvature-driven azimuthal drifts, so that an ion remains on a given field line and experiences the balanced geodesic curvature. Using a model electric field profile, Byers showed good (nonstochastic) particle behavior in MARS with a centerline transition potential of -50 kV and a grounded halo. Fortunately, this is consistent with the potential biasing of the plasma specified for rotation control in the central cell (see Section 2.2.4).

Recent calculations with Byers's code have shown that good particle behavior is possible in MARS even with an incomplete nulling of the geodesic curvature integral as long as the transition electric field is negative. We will use this latitude to make design revisions that will increase the MHD stability of the system (see Section 2.2.3).

Fig. 2.2-1. (a) Geodesic curvature; (b) progressive integral of geodesic curvature.

2.2.2 Parallel Current

Near-nulling of the central-cell parallel current is important to prevent distortion of the otherwise circular flux surface. How nearly the parallel current must be nulled to prevent damaging flux distortions, especially with the annealing effects of finite larmor radii, is an area of ongoing investigation. Because the plasma pressure is proportional to B in the transition region and nearly symmetric about the anchor midplane (see Fig. 2.2-2), and because the anchor geodesic curvature is much less than that in the transition, it follows that satisfying Eq. (2.2-1) will also nearly null the central-cell parallel current, given by

$$J_{\parallel \text{ central cell}} = \int_{-L}^L \frac{(P_{\parallel} + P_{\perp})}{B} \kappa ds.$$

Figure 2.2-3 shows the near-nulling of the central-cell parallel current in MARS.

2.2.3 MHD Stability

The interchange stability criterion is given by

$$\int_{-L}^L \frac{(P_{\parallel} + P_{\perp})}{B} \kappa ds < 0, \quad (2.2-2)$$

where κ is the normal curvature of the field line. In practice, we integrate this function on a 45-deg field line from $z = 0$ to L . In addition to satisfying Eq. (2.2-2), we require that κ be nearly symmetric about the anchor midplane to ensure that the $m = 1$ ballooning limit will approach the interchange limit.

Figure 2.2-4 shows the normal curvature along the length of the machine. Note the small positive and negative contributions due to the central-cell field ripple, the small negative contribution due to the simple mirror axis-cell, the mostly negative contribution of the transition region, and the double-humped positive contribution of the anchor. Figure 2.2-5(a) shows the

Fig. 2.2-2. Plasma pressure normalized to 1.0 in the central cell.

Fig. 2.2-3. Parallel current.

Fig. 2.2-4. Normal curvature.

Fig. 2.2-5. (a) P/B-weighted normal curvature; (b) progressive integral of P/B-weighted normal curvature.

axial profile and Fig. 2.2-5(b) shows the progressive integral of the p/B -weighted normal curvature. Balancing the positive and negative contributions in Fig. 2.2-5(a) results in a nulled integral at the end of the machine, thus signifying stability for the β values chosen. The β values used in this calculation (all peak, on-axis $\beta_{\perp}$ values) were central cell $\beta = 0.23$, axicell $\beta = 0.4$, and anchor $\beta = 0.65$ (consistent with the mirror mode limit). The central-cell β_c value is considerably less than the specified MARS design value of 0.7.

Our primary goal in satisfying the physics constraints of the magnetic configuration is to demonstrate MHD stability at $\beta_c = 0.7$ while simultaneously satisfying the requirements for radial transport and parallel current. We are encouraged that satisfactory radial transport does not seem to require a complete nulling of the geodesic curvature. We have, in fact, already developed a magnet design (very similar to the present MARS design) with interchange stability at $\beta_c = 0.7$ that gives satisfactory radial transport despite its noncancelling geodesic curvature. However, this design has nonsymmetric R in the anchor, and $m = 1$ ballooning is suspected (although not yet verified). Another approach to improved MHD stability is to sustain a high- β mirror-confined species (probably by ECRH) in the good curvature magnetic well of the transition (at $z = 90$ m, see Figs. 2.1-5 and 2.2-5a). Sample calculations have shown stability with $\beta_c = 0.5$. In this report we assume that we will be successful in finding a satisfactory design solution at $\beta_c = 0.7$, and we use this value in our plasma performance calculations.

2.2.4 Central-Cell Plasma Rotation

A mirror machine plasma becomes MHD unstable if the radial electric field E_r is too large. The effect is derived by considering net azimuthal electric currents through a flute perturbation; these currents come from the guiding center drifts (although the currents are modified by the conservation of magnetic flux through the flute). The guiding center drifts are produced by centrifugal, electric, and gradient B (normal curvature) forces. The net azimuthal currents can charge up the sides of the flute, causing a secondary electric field E_{ϕ} which makes the flute $E_{\phi} \times B$ drift either outward (growth) or inward (damping), depending on the direction of the azimuthal current. Thus, to ensure that the current is in the stabilizing direction, the following criterion must be satisfied:

$$\int_{-L}^L \frac{p_{\parallel} + p_{\perp}}{B} R ds < - \frac{m_i n_i L_c}{B_c^4 r_p^2} E_r^2 ,$$

where m_i is ion mass, n_i is ion density, L_c is central cell length, B_c is central-cell magnetic field, r_p is plasma radius, and E_r is the central-cell radial electric field. Note that if $E_r = 0$, then the criterion becomes Eq. (2.2-2). MARS would violate the criterion with an uncontrolled central-cell radial electric field. The solution proposed for this design is to bias the plasma potential with respect to ground by lowering the potential at both ends of the machine. For MARS, the central-cell potential (on-axis) is reduced to 29 keV with respect to ground.⁵

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2.3.1 Trapping Rates

As center-cell ions pass through the transition region, collisions will cause some of them to scatter in a sufficient pitch angle to become trapped in this region. Without a provision for removing or pumping these trapped ions, the density will increase and desired plasma conditions will be destroyed. The high density of trapped ions will cause the potential in the transition region to increase, and, because many of the trapped ions have turning points in the anchor, they will also increase the density and potential at the thermal barrier. The decreased thermal insulation between center-cell and warm electrons will cool these electrons and lower the confining potential of the center-cell ions. An additional effect, which may occur first, is to raise the pressure in the transition region high enough to cause MHD instability.

Several schemes have been suggested for pumping the trapped ions. In the case of TMX-U and MFTF-B, charge exchange off a pump beam is used. The pump beam energy and direction are chosen so that the charge-exchanged beam atoms, now ions, pass into the central cell while the trapped ions, now neutral, pass radially out of the plasma to the halo or wall. Other schemes under consideration for MARS involve the use of rf magnetic fields to remove the ions radially.

To specify the neutral beam current or rf pump parameters, we must compute the trapping current of passing ions. To do so, we must solve for the distribution functions of the relevant ions from the Fokker-Planck equation. Futch and LoDestro¹ have used a numerical code to compute trapping rates in square-well magnetic and potential fields for a variety of mirror ratios and trapped (n_t) and passing (n_p) ion densities. The relationship between the densities is generally expressed with the quantity

$$g_t = 1 + \frac{n_t}{n_p}.$$

The Futch-LoDestro results are well-represented by the expression

$$\left. \frac{dn}{dt} \right|_t = \frac{n_p^2 \{0.935 [1 + 0.0441R] g_t\}^{10/3}}{n_p (g_t - 1)^{7/3}}, \quad (2.3-1)$$

where R is the mirror ratio, $n_{Tp} = 6.27 \times 10^{11} T^{3/2} / \ln \Delta$, $\ln \Delta = 34.9 - \ln[n/T_p E]^{1/2}$ with E = ion energy at the bottom of the well, T_p is the passing ion temperature, and $n = n_p + n_t$.

Futch and LoDestro also studied the effect of magnetic field shapes on the trapping rates with the aid of the bounce-averaged Fokker-Planck code.² In this work they assumed uniform pumping throughout the trapping region. To fit their results, we can multiply Eq. (2.3-1) by the correction factor H , which is a function of the mirror ratio, the field shape, and g_t . For a transition mirror ratio of 16, typical of MARS, their results indicate an H factor of about 2.7. However, the field from MARS coils is much more like a square well, so we would expect a much smaller H factor. In addition, their assumption of uniform pumping is not appropriate to a real machine where one would generally pump at certain selected locations. Indeed, charge-exchange pumping at the bottom of the transition section is not at all desirable since it requires the highest energy beam to overcome the potential of the axicell and allow the charge-exchanged beam particles to pass into the central cell. Pumping near the mirror is advantageous from that standpoint since a relatively much smaller potential must be overcome and a lower energy neutral beam can be used. However, it is not clear that a single pump near the mirror can pump the ions in the transition section. The deeply trapped ions, i.e., those with turning points near the bottom of the magnetic field, are not "seen" by a beam (or other pumping device). Thus, these trapped ions cannot be directly charge-exchanged out of the transition section, but must first suffer enough deflection in pitch angle via Coulomb collisions to bring them into the part of phase space with which the pump intersects. Strong enough pumping could distort the ion distribution function enough to remove these deeply trapped ions, but the required current might be impractical.

To study the effect of pumping at selected locations on the trapping rate, we use the Fokker-Planck code.² The term $-vf$, where f is the distribution function, is included in the equations to represent pumping and is a function of axial position. In this case, v is adjusted to obtain a steady-state solution with the specified mirror and axicell potentials, passing ion density and temperature, mirror ratio, etc. In its present version, the Fokker-Planck code assumes monotonic magnetic field and potential profiles, and symmetry about the midplane. We can approximate the actual

situation by neglecting the potential peaks at the inner anchor mirror and at point A. In addition, we replace the magnetic field profile with a nearly flat but slightly decreasing field of value almost equal to the transition field (see Fig. 2.3-1). This appears to be a gross simplification of the actual field, but it should be noted that most of the ion trapping is expected to occur near the axicell mirror where the passing and trapped densities are highest. The center-cell passing ions are assumed to be Maxwellian and are launched from the center of the axicell.

We have run several cases corresponding to different pumping locations with or without an axicell. Only a case that closely corresponds to the current MARS parameters will be presented. Input parameters are collected in Table 2.3-1.

The pump is localized to a range of magnetic fields between 7.8 and 10 T, as shown in Fig. 2.3-1. Results for the distribution function at steady state, as well as the trapped density and potential dependence on z , are shown in Figs. 2.3-2 through 2.3-4. The two different boundaries in Fig. 2.3-2 correspond to the center of the axicell, indicated by the number 2, and the high-field axicell mirror, indicated by the number 1. The Maxwellian passing ions are enclosed by the lower of curves 1 and 2 and are indicated by the contours parallel to the θ axis.

The trapping rate for this case was found to be $2.419 \times 10^{14} \text{ cm}^{-3} \text{ s}^{-1}$. Other quantities of interest are $g_t = 3.148$, $E = 155 \text{ keV}$, $n = 5.43 \times 10^{12} \text{ cm}^{-3}$, and $R = 16.47$. With the aid of these numbers, we find the trapping rate from Eq. (2.3-1) to be $2.046 \times 10^{14} \text{ cm}^{-3} \text{ s}^{-1}$. The equivalent H factor is 1.18, only a small amount above the value of unity for a square-well field and potential. For the baseline MARS case, an H factor of unity was used, leading to a trapping rate of 295 A at each end.

Fig. 2.3-1. Magnetic field used for trapping-rate calculations.

Table 2.3-1. Parameters for calculating trapping rates in the transition section.

Axicell vacuum magnetic field (T)	17.3
Axicell beta	0.40
Axicell mirror field (T)	24.0
Transition magnetic field (T)	1.477
Equivalent transition length (m)	18
Passing ion density at axicell midplane (cm^{-3})	2.155×10^{14}
Passing ion temperature (keV)	35
Electron temperature (keV)	24
Potential at axicell midplane (kV)	126.2
Potential at axicell mirror (kV)	82.1
Potential at bottom of transition section (kV)	0.0

Fig. 2.3-2. Distribution function of ions at transition section.

Fig. 2.3-3. Trapped density in transition section.

Fig. 2.3-4. Electrostatic potential in transition section.

2.3.2 Charge-Exchange Pumping

2.3.2.1 Overview

Once the current of passing ions trapped in the transition region I_{trap} has been determined by the method outlined in Section 2.3.1, additional work in two specific areas must be undertaken to compute the actual beam power required for charge-exchange pumping. The first is the calculation of the actual neutral beam current I_{pump} that must be supplied to the plasma to pump a given I_{trap} (the ratio of I_{pump} to I_{trap} is approximately 2.5 to 1); the second is the beam energy required to allow the injected beam to fuel the central cell. The required beam power is $P_{\text{pump}} = E_{\text{pump}} I_{\text{pump}}$.

2.3.2.2 Pumping Current

The pump beam current that must be supplied to the plasma for charge-exchange pumping is greater than the trapped ion current for three reasons: (1) a beam particle has a finite probability of ionization as well as charge exchange; (2) both trapped and passing ions can undergo charge-exchange events with the beam; and (3) a charge-exchange neutral that was a trapped ion can be reionized before it escapes the plasma. The relationship between I_{pump} and I_{trap} can be written as

$$I_{\text{pump}} = \frac{g_p}{(g_p - 1)} \frac{1}{p_{\text{cx}}} f_{\text{ion}} I_{\text{trap}},$$

where

- g_p = $(n_{\text{pass}} + n_{\text{trap}})/n_{\text{pass}}$ at the pump beam target location,
- p_{cx} = probability of a beam particle undergoing charge exchange, and
- f_{ion} = reionization factor, > 1 .

The combination $g_p/(g_p - 1)$ is just $(n_{\text{pass}} + n_{\text{trap}})/n_{\text{trap}}$, which increases the current as a result of unavoidable passing ion pumping. For the MARS base-line, this factor is about 1.5. From estimates of σ_v for co- and counter-streaming ion distributions, the value of p_{cx} has been taken to be approximately 0.7. From previous work on charge-exchange pumping,³ the reionization factor has been taken to be about 1.2. This number was based on a simple

calculation accounting for the charge-exchange neutral traveling the short way through an elliptical flux bundle. Here, our pump beam is aimed at a circular plasma, which has a longer chord length for a neutral to escape. This suggests that f_{ion} may be larger than 1.2. A detailed calculation should be done to determine a more accurate value for f_{ion} . Combining these correction factors, we see that I_{pump} exceeds I_{trap} by a factor of about 2.6.

2.3.2.3 Pump Beam Energy

To compute the required pump beam energy, we must know the magnetic field and potential at the pump beam injection point. Once an axial position and injection angle have been chosen consistent with good pump beam access, the magnetic field design gives us the field at that position. Quasi-neutrality at that point is used to calculate the potential. Knowing this, the injection energy is then defined by the requirement that the parallel velocity be greater than zero at all positions between the pump beam injection point and the central cell. Once this injection energy has been computed, a calculation of beam penetration tells us whether the axial location of the pump beam allows for adequate pumping of trapped particles near the axis. If the penetration is inadequate, the axial position is adjusted (usually to larger z values) until the condition is remedied.

The requirement that the beam particles pass into the central cell reduces to the most restrictive of two criteria: (1) that the particles pass the 24 T mirror; and (2) that the particles pass the midplane of the axicell. The injection energy is then taken to be the larger of the energy to pass the 24-T peak

$$E_{inj,1} = \frac{(\phi_{pi} - \phi_{pb})}{(1 - \frac{B_{bar}}{B_{pi}} \sin^2 \theta_{inj})} ,$$

or the energy to pass the axicell midplane

$$E_{inj,2} = \frac{\phi_{pi}}{(1 - \frac{B_{mid}}{B_{pi}} \sin^2 \theta_{inj})} ,$$

where

B_{bar} = peak mirror field in axicell,
 B_{mid} = midplane plasma field in axicell,
 B_{pi} = plasma field at pump beam location,
 θ_{inj} = injection angle of pump beam,
 ϕ_{pi} = potential difference (> 0) between axicell midplane
and pump beam location, and
 ϕ_{pb} = potential difference between axicell midplane and outer
axicell mirror.

2.3.3 Alternative Pumping Methods

2.3.3.1 Introduction

Because the neutral beam power in the transition pump contributes the largest amount of recirculating electric power in MARS, we are now in the process of designing a practical means of inducing sufficient controlled amounts of radial diffusion in the transition regions to eliminate or reduce the 140-MW (trapped) neutral beam requirement. We are working on a promising adaptation of Baldwin's original drift-pump scheme⁴ to the double-ellipse type of transitions that have been incorporated in the MFTF-B and MARS axicell plug configurations shown in Figs. 2.3-5(a) and (b). This idea is currently being tested in the LLNL "alpha-beta" drift code, which uses EFFI-generated magnetic fields and various model electric fields as functions of field B (z) and flux $\alpha = Br^2/2$ (see Ref. 5). These calculations will calibrate estimates, derived here, of the radial diffusion coefficient as a function of the AC field perturbation amplitude and frequency, as well as the conditions for driver frequency bandwidth necessary to ensure that the perturbation-induced kicks are random and not superadiabatic. These calculations are made for MARS parameters such that the equilibrium radial electric fields do not by themselves cause stochastic behavior of passing ions. Assuming suitable resolution of these issues, we can then specify the key engineering requirements for AC drift pumping and assess the practicality of testing this scheme in TMX-U and MFTF-B. If the engineering design appears to be reasonably credible, this scheme would then qualify for adoption as the MARS baseline pumping method.

(a)

(b)

Fig. 2.3-5. (a) MARS end plug magnets (double ellipse transition with one-quarter flux bundle); (b) MFTF-B end plug magnets.

2.3.3.2 Description of the AC Drift Pumping Concept

Figures 2.3-6(a-d) show axial profiles of field B , potential ϕ , geodesic curvature $\mathbf{I-O}$, and normal curvature, respectively, for one plug of the MARS reactor, corresponding to the magnet set shown in Fig. 2.3-5(a). This drift-pumping scheme exploits the unique design feature of double ellipse transitions, namely, that the radial drifts of both passing and trapped ions are nearly canceled when they pass through two large, but opposite-sign, geodesic curvature ($\mathbf{I-O}$) peaks associated with the double transition fans [see Fig. 2.3-6(c)]. Each geodesic curvature spike causes the deuterons that pass through with characteristic parallel energy ($\delta\phi + T_{ic} \approx 140$ keV) to drift normal to the flux surface by approximately

$$\frac{1}{2} \frac{\delta\alpha}{\alpha} = \frac{\delta r}{r} = \frac{M_i v_{\parallel}}{Zq B r^2} \int \mathbf{I-O} \, ds \approx 0.28 \quad (\text{one spike}), \quad (2.3-2)$$

with each change in flux or equivalent radius mapped to the central cell corresponding to the sign of each $\mathbf{I-O}$ peak. Because these $\mathbf{I-O}$ curvatures have an azimuthal $\cos(2\phi)$ amplitude variation, any drift in azimuth while an ion transits between the opposing $\mathbf{I-O}$ peaks tends to break the cancellation and triggers net radial steps for ions passing through. This is why large radial-equilibrium electric fields must be avoided to have tolerable radial transport.⁶ However, this is also why small, random, azimuthal kicks resulting from a stochastic AC perturbation field strategically located between the peaks would also cause greatly amplified radial step sizes compared with those arising directly from the AC perturbations alone. This is the essential leverage that permits Baldwin's AC coil to operate in double ellipse transitions with much smaller coil currents than had been previously estimated. Equation (2.3-2) leads one to expect comparable step sizes and induced radial transport for both passing and trapped ions (with little or no selectivity, as was the case for charge-exchange pumping), as well as comparable diffusion rates for equivalent Z/M_i impurities and thermal alphas (in contrast to charge-exchange pumping, which cannot remove $Z > 1$ ions in the MARS configuration). In Section 2.4.1, we discuss the requirements for achieving steady state by pumping alphas in the transitions, which implies a relationship between relative concentrations of DT fuel ions and thermal alphas and their

Fig. 2.3-6. Axial profiles of: (a) field B ; (b) potential ϕ ; (c) geodesic curvature κ ; and (d) normal curvature κ_n .

respective radial diffusion coefficients. The main uncertainty is the number, bandwidth, or sweep rate of the AC perturbation frequencies required to ensure random-phase successive encounters of an ion with the perturbation.

2.3.3.3 Estimate of AC Perturbation Amplitudes

We now estimate the AC perturbation amplitude required to transport a trapped deuteron in a collision-filling time, τ_{pump} , of approximately 0.077 s for WARS parameters. The coil configuration shown in Fig. 2.3-7(a) is similar to the Nagoya Type III coil proposed by Baldwin, but with radial feed to avoid any induced B_z components that might cause parasitic heating of ions and electrons by transit-time magnetic pumping. It has been shown that such heating is small, and that the induced $B_{\perp}$ (plasma) $\approx B_{\perp}$ (vacuum).⁷ Assuming that polarized electron currents short out E_z at low frequencies, we expect to induce a $m = 1$ transverse electric field with comparable $\tilde{E}_r$ and $\tilde{E}_{\phi}$ components over the plasma cross section. We are interested here in the $\tilde{E}_r$ component that gives rise to an azimuthal $\tilde{E}_r \times B_z$ drift. To maximize the azimuthal kicks $\delta\beta$ in the vicinity between the peaks, we prefer the bucking coil configuration in Fig. 2.3-7(b), centered between the peaks in the 45 deg plane, with $\lambda_2 \approx L_{\text{FO}} \approx 4.8$ m, the distance between peaks [Fig. 2.3-6(c)]. We then pick a characteristic coil frequency corresponding to an ion transit time between the FO peaks, $\omega_0 \approx L_{\text{FO}}/v_i \approx 7 \times 10^5 \text{ s}^{-1}$ ($f_0 \approx 100$ kHz). The random azimuthal kick generated by the perturbation

$$\delta\beta \approx \left(\frac{\tilde{E}_r}{B_t r_t} \right) \left(\frac{L_{\text{FO}}}{v_i} \right)$$

is set equal to the azimuthal kick required to transport the ion in a time τ_{pump}

$$\delta\beta = \left(\frac{\tau_{\text{bounce}}}{\tau_{\text{pump}}} \right)^{1/2} \left(\frac{\delta r}{r} \right)^{-1} \text{ single spike}$$

This yields the required perturbation electric field (MKS units)

Fig. 2.3-7. Schematic of single (a) and double (b) AC perturbation coils, showing induced E .

$$\tilde{E}_r \approx B_t r_t \left(\frac{v_{\parallel}}{L_{I-O}} \right) \left(\frac{\tau_{\text{bounce}}}{\tau_{\text{pump}}} \right)^{1/2} \left(\frac{\delta r}{r} \right)^{-1} \text{single spike}, \quad (2.3-3)$$

where B_t and r_t are the field and radius, respectively, of the transition region between the peaks, τ_{bounce} is the bounce time of a trapped ion (time between successive encounters with the perturbation), and the $(\delta r/r)$ single spike is given by Eq. (2.3-2). Relating the induced $\tilde{B}$ and $\tilde{E}$ fields to Faraday's law gives

$$\tilde{E}_r = \omega_b \tilde{B}_{\perp} L_{I-O} / 2. \quad (2.3-4)$$

Equating Eq. (2.3-3) and (2.3-4) then gives

$$\frac{\tilde{B}_{\perp}}{B_t} = \left(\frac{2r_t}{L_{I-O}} \right) \left(\frac{v_{\parallel}}{\omega_b L_{I-O}} \right) \left(\frac{\tau_{\text{bounce}}}{\tau_{\text{pump}}} \right)^{1/2} \left(\frac{\delta r}{r} \right)^{-1} \text{single spike}. \quad (2.3-5)$$

For MARS parameters, $r_t \approx 0.64$ m, $L_{I-O} \approx 4.8$ m, $v_{\parallel} = 3.6 \times 10^6$ m/s, $\tau_{\text{bounce}} = 18 / v_{\parallel} = 5.1 \times 10^{-6}$ s, $\tau_{\text{pump}} = 0.077$ s, and $(\delta r/r)_{\text{single spike}} \approx 0.28$, Eq. (2.3-5) yields $\tilde{B}_{\perp}/B_t \approx 0.0078$. In contrast, without the aid of the geodesic curvature, the perturbation field amplitude required to pump the ions with radial steps induced by the perturbation alone would have to be

$$\begin{aligned} \frac{\tilde{B}_{\perp}}{B_t} &= \frac{\tau_{\text{bounce}}}{\tau_a} \left(\frac{5 \Delta \omega}{\tau_{\text{pump}} \omega_b^2} \right)^{1/2} \\ &\approx \frac{18}{4.8} \left[\frac{5}{0.077 (1.5 \times 10^5 \text{ s}^{-1})} \right]^{1/2} \approx 0.078 \end{aligned}$$

or about one order of magnitude greater. Here we assume a 100% bandwidth requirement, $\Delta \omega / \omega_b$, and an azimuthal drift frequency, ω_b , for the parameters shown in Fig. 2.3-6. In this case, negative transition electric fields cause $E \times B$ drifts opposite to the normal curvature drift that is dominated by the bad I-O curvature associated with the transition fans. In addition to the

advantage of reduced perturbation amplitude, the use of AC drift pumps in double ellipse transitions may need less than $\Delta\omega \sim \omega$ bandwidth to ensure stochasticity in the induced kicks. One could guess that a frequency spread on the order of

$$\frac{\Delta f}{f} < \frac{L_{IO}}{L_{\text{bounce}}} \approx 25\%$$

might suffice, but this, of course, is one of the main subjects to be examined with the LLNL alpha-beta drift code.

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2.4 ASH REMOVAL

To obtain good reactor performance (ignition), most of the 3.5-MeV alphas produced by DT fusion must be magnetically trapped in the central cell until they slow down to thermalized energies of about 40 keV. After they slow down, they must be disposed of so they do not dilute the reacting plasma and waste the available plasma beta. Computations indicate that the tolerance of thermalized alphas and impurities is only about 10% of the central-cell plasma density.

For a reactor generating 3,500 MW of fusion power, the rate of alpha production is 200 A. Of these, the fraction magnetically trapped in the central cell is $\cos \theta_{LC}$, about 94%. The loss-cone angle θ_{LC} is determined by the magnetic field ratio

$$\sin \theta_{LC} = \left[\frac{B_C (1 - \beta_C)^{1/2}}{B_{max}} \right]^{1/2} = 0.33.$$

If an alpha scatters into the loss cone at an energy larger than $2\phi_C = 292$ keV, it may be lost. Fokker-Planck computations indicate about 25% additional alpha losses from coulomb scattering. The rate of production of thermalized alphas is therefore $200 \text{ A} \times 70\% = 140 \text{ A}$.

Thus, the allowable lifetime of a thermalized alpha is

$$\tau_\alpha < \frac{0.1\% n_{ci} \pi_C^2 L_C q}{140 \text{ A}} = 3.5 \text{ s}.$$

Thermalized alphas will be very well contained electrostatically because of their double charge and low energy compared with the confining potential ϕ_C . The only removal technique appears to be some type of radial transport, which should not unduly dispose of either the fast alphas or the reacting DT ions. We have chosen drift pumping of alphas and impurities passing through and trapped in the transition cell as our baseline model for ash removal.

2.4.1 Transition Region Drift Pumping

Under certain conditions, radial transport in the transition cell is more effective than radial transport in the central cell because of the large geodesic curvature in the transition, which induces transverse grad-B drifts.

Using two different drift codes, we have computed radial drifts up to a maximum of several centimeters with each bounce, depending on conditions. If these radial drifts are fully stochastic, this leads to a large rate of radial diffusion. Some control over the diffusion rate is possible by controlling the stochasticity of the motion.

The fraction of central-cell ions in the region of velocity space that allows them to pass through the transition cell is

$$F = [1 - \cos \theta_{LC}] \exp(-Z\Delta\phi_{pc}/T_{ic}) = 3.85\% \text{ for 35-keV DT ions,} \quad (2.4-1)$$

or $\quad = 1.9\% \text{ for 35-keV alphas.}$

As a result of coulomb scattering, each thermalized central-cell ion visits this region of velocity space several times during its trapped lifetime. If the radial diffusion coefficient is appropriate, the ions will be transported radially at the desired rate.

On the other hand, fast alphas (above 1 MeV) have small-pitch-angle coulomb collisions and do not enter this region of velocity space, except for the 30% loss mentioned above. Of this 30%, only 6% are alphas at the full energy of 3.5 MeV and born with pitch angles smaller than 19 deg. Under MARS conditions, coulomb scattering is significant only for alphas of energy below 1 MeV.

Although it is not essential, we are hoping to discover a set of conditions under which the radial transport of thermalized alphas and impurities is faster than the radial transport of DT ions. It is sufficient for power balance that these transport coefficients merely be equal, such that about 10 DT ions are pumped out with each alpha. There is some hope that alphas can be pumped selectively compared with DT ions by taking advantage of differences in the stochasticity caused by differences in the azimuthal drift speeds.

2.4.1.1 Stochasticity

The azimuthal drift $\Delta\phi$ of a passing ion will be several radians per bounce, depending on radial electric fields and other conditions. The direction and magnitude of the radial displacement $\Delta r = \int v_r dt$ will depend on the azimuthal angle ϕ , roughly in proportion to $\sin 2\phi$. If the azimuthal drift is

very slow, the motion will be periodic, and no net radial transport will occur. On the other hand, if the azimuthal drift is fast enough, the motion will be stochastic. In this case, the radial displacement per bounce Δr will be a random variable and cause radial diffusion. The criterion for collisionless stochasticity is

$$\Delta r \frac{\partial}{\partial r} (\Delta \phi) > \frac{1}{m}, \quad (2.4-2)$$

where $m = 2$ for the quadrupole field of the transition.¹

Several factors will make it necessary to control the radial plasma potential gradient and thereby control the $E \times B$ drift speed. In theory, it is possible to adjust the plasma potential gradient so that the motion of the alphas is stochastic and the motion of the DT ions is mostly periodic. The following two facts make this possible:

- The azimuthal grad-B drift due to the non-uniform plasma β is in the direction opposite to the direction of the azimuthal $E \times B$ drift. Consequently, it is theoretically possible to adjust the drift speeds to partially cancel each other.
- The grad-B drift speed is proportional to $W_{\perp}/Z$, while the $E \times B$ drift speed is the same for all particles. Therefore, the condition that tends to cancel the total drift for DT ions will not cancel the drift for alphas at equal energy. This raises the possibility that the fraction F_2 of alphas moving stochastically will be larger than the corresponding fraction of DT ions, depending on the distributions of plasma potential, plasma β , and $W_{\perp}$.

2.4.1.2 Diffusion Coefficients

We are working to discover a set of conditions under which the average diffusion coefficient for radial transport will be

$$\langle D_{\alpha} \rangle = r_c^2 / \tau_{\alpha} > 460 \text{ cm}^2/\text{s for thermalized alphas}$$

but

$$= \langle D_{DT} \rangle = r_c^2 / \tau_{DT} < 1600 \text{ cm}^2/\text{s} \text{ for DT ions.}$$

The local diffusion coefficient $D = \Delta r^2 / \tau$ is a function of several parameters: r , Ψ , $W_\perp$, $W_\parallel$, Z , and A . We can write scaling laws to perform the averaging so that

$$\Delta r = \int v_{\text{grad-B}} dt \propto \frac{v_{\text{grad-B}}}{v_\parallel} \propto \frac{\frac{W}{Z}}{\left(\frac{W}{A}\right)^{1/2}} = W^{1/2} \frac{A^{1/2}}{Z}. \quad (2.4-3)$$

Also, $\Delta r \propto r \cos 2\Psi$ if the long-thin approximation is valid.

The time τ is the interval between stochastic bounces. This is related to the bounce period τ_b , but is longer for two reasons: (1) Only a fraction (F_1)--defined by Eq. (2.4-1)--of the central-cell ions is in the passing region of velocity space. (2) Only a fraction (F_2) satisfies Eq. (2.4-2), and therefore, undergoes stochastic bounces, i.e., $\tau = \tau_b / F_1 F_2$,

where

$$\tau_b = \frac{L_c}{v_\parallel} \propto \frac{A}{W}^{1/2}. \quad (2.4-4)$$

By combining Eqs. (2.4-3) and (2.4-4), we have a scaling law

$$D \propto W^{3/2} A^{1/2} r^2 \cos 2\psi \frac{F_1 F_2}{Z^2}. \quad (2.4-5)$$

We are searching for a condition such that $D_\alpha / D_{DT} > 460/1600$.

From Eq. (2.4-5) we find that this requires that

$$\left(\frac{W}{W_{DT}}\right)^{3/2} \left(\frac{F_{2\alpha}}{F_{2DT}}\right) \gtrsim 1.83. \quad (2.4-6)$$

Here we have cancelled the factor $r_1^2 \cos 2\psi$, which is the same for all species, and have used the appropriate masses A and charges Z for alphas and DT ions. The factor F_1 is given by Eq. (2.4-1) for alphas and DT ions.

Equation (2.4-6) shows that we have two ways of achieving this requirement:

- We take advantage of the fact that some of the radial diffusion occurs as the alpha is slowing down. At energies below $2\phi_c \approx 300$ keV, some of the alphas scatter into the passing velocity space where radial transport occurs. Also, all alphas in the transition have an energy larger than the DT energy by the amount of transition potential drop $\phi_c \approx 100$ kV. Therefore, on the average $W_\alpha > W_{DT}$.
- The factor F_2 quantifies the effect of stochasticity. As mentioned in Section 2.4.1.1, there are ways of enhancing the stochasticity of alphas while reducing the stochasticity of DT ions.

From the above considerations, we feel assured that the required ratio of diffusion coefficients is attainable. Now we must evaluate the magnitude of the diffusion rate.

2.4.1.3 Diffusion Equation

To compute quantitatively the diffusion requirements, we must solve the diffusion equation for the case in which the diffusion coefficient is a function of r . Under the long-thin approximation, $\Delta r = kr$ and $D_{trans} = \Delta r^2/\tau = k^2 r^2/\tau$, where k is a constant of proportionality that will be determined by drift computations. Here, D_{trans} is the diffusion coefficient due to radial transport in the transition cell and D_{class} is the classical diffusion coefficient. Near the z -axis, D_{trans} becomes very small and it is necessary to consider other types of diffusion as well: $D_{total} = D_{trans} + D_{class}$.

The radial diffusion flux vector $\underline{r}$ is given by $\underline{r} = -D_{total} \nabla n$ ions/cm²-s, and the required rate of particle injection per unit volume is

$$J = -\nabla \cdot \underline{r} = -\frac{1}{r} \frac{\partial}{\partial r} (r \underline{r}) \quad \text{ions/cm}^3\text{-s.} \quad (2.4-7)$$

Equation (2.4-7) can be integrated two times, with the assumption that J is independent of r . This assumption is required only to attain an analytic

solution of the diffusion equation. Actually, J will depend on r for both DT ions and alphas. The solution is the following, subject to the boundary conditions $dn/dr = 0$ at $r = 0$ and $n = 0$ at $r = r_c$:

$$n(r) = \frac{J\tau}{4k^2} \ln \left[\frac{\frac{k^2 r_c^2}{\tau} + D_{\text{class}}}{\frac{k^2 r^2}{\tau} + D_{\text{class}}} \right]. \quad (2.4-8)$$

The containment time for either alphas or DT ions is

$$\tau_{\text{con}} = n_c/J > 1 \text{ s for DT ions} \quad (2.4-9)$$

< 3.5 s for alphas.

Equations (2.4-8) and (2.4-9) can be combined to give the radial density profile $n(r)/n_c$, which is shown in Eq. (2.4-10) and Fig. 2.4-1. The shape of this profile is realistic for alphas, for which transverse diffusion is dominant. However, the shape of the DT density profile will be more flat-topped than Fig. 2.4-1 indicates, because end losses will probably be more important than transverse diffusion, so that

$$\frac{n(r)}{n_c} = \frac{\tau}{4k^2 \tau_{\text{con}}} \ln \left[\frac{\frac{k^2 r_c^2}{\tau} + D_{\text{class}}}{\frac{k^2 r^2}{\tau} + D_{\text{class}}} \right]. \quad (2.4-10)$$

We can solve Eq. (2.4-10) for τ_{con} with $r = 0$ and $\tau = \tau_b/F_1 F_2$, thereby ascertaining the requirements for the stochasticity parameter F_2 . This is shown in Fig. 2.4-2:

$$\tau_{\text{con}} = \frac{\tau_b}{4k^2 F_1 F_2} \ln \left[1 + \frac{k^2 r_c^2}{\tau D_{\text{class}}} \right].$$

Fig. 2.4-1. Radial density profiles $n(r)/n_c$ under the following assumptions: (1) radial transport is the dominant loss mechanism for thermalized alphas and/or D-T ions; (2) under the long-thin approximation, $\Delta r_{ms} = Kr$; (3) particle injection is independent of the radius for convenience of an analytic solution to the diffusion equation.

Fig. 2.4-2. Dependence of radial confinement time J of D-T ions and alphas on the stochasticity parameter F_2 .

In Figs. 2.4-1 and 2.4-2 we use the following values, which are realistic for passing alphas under MARS conditions:

	<u>DT Ions</u>	<u>Alphas</u>
τ_{con}	$> 1 \text{ s}$	$< 3.5 \text{ s}$
D_{class}	$100 \text{ cm}^2/\text{s}$	$100 \text{ cm}^2/\text{s}$
F_1	0.0385	0.0191
r_c	40 cm	40 cm
$k = \Delta r/r$	0.1	0.1
(preliminary result from drift computations)		
τ_b	10^{-4} s	10^{-4} s

Under these conditions, the stochasticity parameter F_2 must be no greater than 0.15 for DT ions and not less than 0.02 for disposal of alpha ash. It appears that these conditions will be easy to achieve for alpha ash disposal. Some study will be required to avoid excessive radial transport of DT ions. The parameters k and F_2 are not precisely known at this time.

2.4.1.4 Drift Computations

We are conducting a series of drift computations to attain a better understanding of the dependence of the transverse drifts Δr and $\Delta \phi$ on magnetic field shapes and parameters such as r , ϕ , $W_{\perp}$, $W_{\parallel}$, E_r , and ϕ . Two codes with guiding-center drift approximations are being used. In both cases ions are started in the center cell with a pitch angle small enough to penetrate the 24-T axicell field and are reflected by the electrostatic potential at point A after passing through the transition cell. After one reflection, the ion is stopped and another is launched. The transverse displacements Δr and $\Delta \phi$ are measured at the plane of launching the ions in the center cell. Both codes agree on the order of magnitude of Δr and $\Delta \phi$ as functions of r .

GCDRIFT is an updated and more accurate version of the old MAFCO-II drift code.² Its main limitation is that three-dimensional electric fields are very crudely modeled in the present version.

A newer code³ includes self-consistent electric fields, but it is based on the long-thin approximation, which may not be valid far from the z-axis in the transition cell. We are now determining the range of validity of this approximation.

2.4.1.5 Drift Pumping of Trapped Ions in the Transition

After thermalization, alphas tend to diffuse from the central cell, first into the region of velocity space occupied by passing ions and then into the region occupied by trapped ions. It probably will be necessary to pump both regions of velocity space. The theory outlined in Section 2.4.1 is relevant to both populations, with several differences.

Because of the shorter bounce period τ_b for the trapped ions, and therefore smaller azimuthal drift per bounce $\Delta\phi$, it is more difficult to satisfy the stochasticity condition in Eq. (2.4-2) for trapped ions than for passing ions. On the other hand, if the motion is stochastic, the shorter bounce period has a tendency to increase the diffusion coefficient D .

2.4.2 Alternative Ash Removal Methods

Several other methods of ash removal have been investigated. These can be divided into three categories, which are listed below in order of importance:

1. Enhanced drift pumping of trapped alphas in the transition by AC magnetic field perturbations.
2. Radial transport due to magnetic perturbations at the ends of the central cell.
3. Charge-exchange pumping of alphas passing through the barrier.⁴

2.4.2.1 Enhanced Drift Pumping of Trapped Alphas by AC Perturbations

Pumping of trapped ions by AC perturbations at frequencies near the drift frequency will work in principle with alphas and impurities as well as with DT ions. This technique for DT ions is described in Section 2.3.3. If trapped alphas and impurities are effectively pumped from the transition, the central cell alphas and impurities will diffuse into the transition rapidly enough to maintain the alpha density in the central cell at the required 10%.⁴ However, the grad-B drift speeds, proportional to $1/Z$, will be different for the various ion species. Consequently, it may be necessary to broaden the frequency range at which the AC perturbations are excited.

2.4.2.2 Radial Transport Due to Magnetic Perturbations at the Ends of the Central Cell

The theory of this technique is almost identical to the theory discussed in Section 2.4.1. It would be possible to install some type of magnetic perturbations at the end of the central cell to enhance the radial diffusion of central cell ions with turning points near the perturbations. Several versions of perturbation coils have been investigated for this purpose, as shown in Fig. 2.4-3. We investigated both relatively small resistive coils close to the plasma that are exposed to neutron bombardment and large superconducting coils outside the neutron shield.

To avoid premature pumping of fast alphas trapped in the central cell, we have considered installing the perturbations at the point where the local mirror ratio R is about 2.5. Therefore, only about 23% of the fast alphas would be affected by the perturbations, and the fast alpha loss would not be severe. In the notation in Section 2.4.1, the pitch angle of ions affected by the perturbation would be a maximum of $F_1 = 1 - \cos \theta = \sin^{-1} R^{-1/2} = 39.2$ deg and the fraction of alphas affected would be $F_1 = 1 - \cos \theta = 23\%$.

However, this requires a rather large value of B_z (6.75 T) and a proportionately high perturbation field. We found radial displacements Δr of only a few millimeters per bounce. For these reasons we concluded that enhanced radial diffusion by perturbations in the central cell is less effective than enhanced diffusion of ions passing through the transition, in spite of the larger value of F_1 . Other possible liabilities, not yet investigated,

Superconducting coils outside the neutron shield
(solenoid radius 2.8 m; plasma radius 0.4 m)

Copper coils inside the neutron shield (radius 0.6 m)

Fig. 2.4-3. Four examples of perturbation coils for pumping thermalized alphas: (a) two versions of large-diameter superconducting coils outside the neutron shield; (b) two versions of resistive coils close to the plasma within the neutron shield.

are the effects of these perturbations on plasma stability. We consider it worthwhile to preserve this technique as a standby. The order of magnitude of Δr was sufficient for alpha disposal in the examples investigated.

2.4.2.3 Charge-Exchange Pumping of Alphas in the Transition

We have shown that under certain conditions it is possible to transfer an electron from a neutral beam atom to a He^{++} in the transition such that the resulting He^+ is able to escape over the potential hill.⁴ The electron capture must occur after the He^{++} has been accelerated by $2\Delta\phi$, which must be sufficient to permit the escape of the He^+ .

The conditions required for this do not apply to the MARS baseline model. One of the conditions is that the charge-exchange neutral beam must be directed outward, toward the end of the tandem mirror. This does not apply to MARS, but is compatible with certain other tandem mirror designs. Once again, such an alternative should be retained as a backup.

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2.5 ANCHOR SLOSHING IONS AND PUMPING

2.5.1 Ion Distribution Function

To calibrate models of ion confinement and pumping power in the anchor, it is necessary to calculate the ion distribution function. These calculations of the are carried out with the bounce-averaged Fokker-Planck code,¹ which assumes symmetric potential and magnetic field profiles. Although the anchor field is symmetric, the potential is not. Also, the sloshing ions are injected only on the inboard side of the anchor, at A', which adds another asymmetry to the problem. Nonetheless, we can model each half of the anchor, taking the appropriate potentials from the point model plasma code, as described in Section 2.10. The potentials and magnetic fields that we used in this Fokker-Planck study differ slightly from the parameters given in Section 2.10, and are collected in Table 2.5-1.

The several classes of ions that must be considered can be explained with the aid of Fig. 2.5-1, which shows the $v_{\parallel}$ - $v_{\perp}$ space of ions at the anchor mid-plane. Four regions of velocity space are apparent: (1) the part that confines the high-energy sloshing ions and low-energy trapped ions in the anchor; (2) a region containing ions that span the space from the transition section to the anchor; (3) a region containing ions that span the space from the axicell through the transition section to the anchor; and (4) a region containing ions that span all regions from the center cell to the anchor. The latter ions are generally known as passing ions. We see from Fig. 2.5-1 that these passing ions cannot scatter directly into the anchor via small-angle collisions, but must first be trapped in the axicell or transition spanning class. Figure 2.5-1 also shows that the ion distribution function in the anchor will be most strongly influenced by the distribution function in the adjacent transition-anchor spanning region. However, the distribution function in the latter region is essentially determined by collisions in the transition region, since the density and length there are so much larger. Thus, we can impose as a boundary condition on the anchor ions the previously computed transition ion distribution function (see Section 2.3.1). One such function mapped to the anchor inboard mirror is shown in Fig. 2.5-2. It is evident that the distribution function can be represented by a product of Gaussians in $v_{\parallel}$ and $v_{\perp}$, which is done in the calculations for the anchor ions.

Fig. 2.5-1. Anchor midplane.

Fig. 2.5-2. $V_{\perp}$ - $V_{\parallel}$ space at anchor midplane showing boundaries between ions lost out end, central-cell-axicell-transition-anchor spanning (or passing) ions, axicell-transition-anchor spanning ions, transition-anchor spanning ions, and anchor trapped ions.

The solution for the anchor ions depends critically on the sloshing-ion injection current and on the pumping strength and location. To pick out the desired solution, it is necessary to impose constraints on the steady-state result. The constraints imposed correspond to one set of parameters for a MARS case: midplane deep passing uensity = $3.57 \times 10^{12} \text{ cm}^{-3}$, midplane sloshing ion + trapped and spanning ion density = $1.34 \times 10^{13} \text{ cm}^{-3}$ and the ratio of peak (at A) to midplane density of 3.29. Injection and pumping rates are then adjusted until these values are attained.

Results obtained in this way are collected in Table 2.5-2. Cases 4 and 5 include no transition spanning ions; we see that the pumping and beam currents are about half of the corresponding values with transition-spanning ions (cases 1 and 2). Evidently, the presence of these ions enhances the collision rates, thus causing higher loss rates of ions. Pumping extending over 3 m (case 1) is also clearly superior to pumping over 8 m (case 3) since the latter removes more of the sloshing ions before they have scattered to large pitch angles, thus requiring a higher sloshing ion current. The total absorbed beam power with case 1 or 2 is about 1.7 times the estimate with the point-model calculations (Section 2.10), a discrepancy that will be corrected in future calculations.

Anchor midplane distribution functions for case 1, as analyzed with inboard and outboard parameters, are shown in Figs. 2.5-3(a and b). The density distribution as a function of z for case 1 is shown in Fig. 2.5-4. Of importance for analysis of microstability to the drift cyclotron loss-cone (DCLC) and two-stream modes are the perpendicular and parallel distribution functions defined by

$$f_{\perp}(v_{\perp}) = \int f(v_{\perp}, v_{\parallel}) dv_{\parallel} \quad f_{\parallel}(v_{\parallel}) = 2\pi \int v_{\perp} f(v_{\perp}, v_{\parallel}) dv_{\perp}$$

[$f_{\perp}$ and $f_{\parallel}$ are shown in Figs. 2.5-5(a and b) at the anchor midplane]. One criterion for microstability is that $f_{\perp}$ always decrease monotonically. We see in Fig. 2.5-5(a) that this criterion is not met for the anchor ion distribution function at the midplane. However, the warm plasma in the distribution function can stabilize the drift cyclotron modes if the temperature of the warm component is large enough, even if the distribution function is not monotonic. We have not tested this distribution for stability, but we can apply

Fig. 2.5-3. Distribution function $2\pi v^2 \sin \theta f(v, \theta)$ at anchor midplane for (a) outer and (b) inner half anchor.

Fig. 2.5-4. Density distribution in anchor.

Fig. 2.5-5. (a) Perpendicular $f_{\perp}$ and (b) parallel $f_{\parallel}$ distribution function at anchor midplane.

Table 2.5-1. Potentials and magnetic fields used in Fokker-Planck study of anchors.

<u>Location</u>	<u>ϕ(kv)</u>	<u>β-reduced field (T)</u>
Center cell	198	2.574
Axicell mirror	198	21.000
Axicell midplane	232	7.668
Axicell mirror	182	24.000
Pump beam location	170	11.8
Transition section	106	1.981
Inner mirror	152	6.000
Injection point A' (475-keV ions)	144	4.027
Anchor midplane	21	1.283
Point A	333	2.359
Outer mirror	0.00	6.000

Table 2.5-2 Beam and pumping currents in anchor. A cubic density profile is assumed.
Anchor cross sectional area at midplane = $1.223 \times 10^4 \text{ cm}^2$.

Case	Pumping	Passing ions	Anchor-transition spanning ions	Required pump current (A/end)	Total required absorbed beam current (A/end)	Absorbed beam power anchor (MW/end)
1	-150 to 150 cm	Maxwellian	From transition calculation	6.5	12.1 (outboard) 12.0 (inboard)	5.7
2	-50 to +50 cm	Maxwellian	From transition calculation	6.2	11.8	5.6
3	-400 to +400 cm (uniform)	Maxwellian	From transition calculation	8.1	14.7	7.0
4	-150 to +150 cm (uniform)	None	None	2.1	4.1	1.9
5	-150 to +150 cm (uniform)	Maxwellian	None	3.7	7.4	3.5

the results for the anchor of MFTF-B (Ref. 2, Fig. F-2), which gives stability boundaries as functions of the ratios of warm-to-hot densities and temperatures. From examination of Fig. 2.5-5(a), we find that there is more than enough warm plasma but its temperature is too low, thus leading to an instability known as the two-component mode. However, we have observed that unstable distributions such as this will relax to a marginally stable state, i.e., enough rf-induced diffusion will take place to heat the warm ions sufficiently to achieve stability. Some additional beam current must be supplied to make up for the concomitant loss of hot ions.

2.5.2 Adiabaticity

To produce a sloshing ion distribution with a peak-to-midplane density ratio of about 3:1, which is sufficient for anchor microstability, a yin-yang with about a 3:1 vacuum mirror ratio is required. A yin-yang using a NbTi superconductor and LHe I cooling cannot have a mirror field above about 6 T, thus giving a 2-T midplane vacuum field. This is the lowest field in the MARS magnetic configuration that will confine high energy particles.

There is a lower limit for the anchor midplane field below which the adiabatic invariant $\mu = E_{\perp}/B$ of a given plasma particle is no longer a constant of the motion. Under these circumstances, the particle undergoes random changes in μ that have the same effect as enhanced pitch angle scattering. This enhanced scattering increases the losses of the particle in question. Such departure from adiabatic behavior must be avoided.

The more energetic and massive a particle is, the more restrictive the lower limit on the anchor midplane field will be. This is why the sloshing ions have been singled out for careful study. The injection energy at the inboard turning point of the sloshing ions is 475 keV. At the anchor midplane, after falling through a potential difference of about 130 keV, these ions have an energy of slightly more than 600 keV. In the plasma performance code, the anchor midplane field is computed to keep these ions adiabatic, given an injection energy, plasma beta, anchor length, and yin-yang field shape and mirror field. The formula for adiabaticity has been derived from relevant analytic and numerical studies of ion adiabaticity.³ This formula for the sloshing-ion injection energy may be written

$$E_{inj,A'} < 3.1 \times 10^{-3} \frac{B_{b,vac}^3}{B_{mp}} (1 - \beta_p)^{3/2} (L_A^2) \left(\frac{2}{A}\right) - (\phi_b - \phi_{A'}),$$

where

$E_{inj,A'}$	= sloshing-ion injection energy (keV),
$B_{b,vac}$	= anchor midplane vacuum field (T),
B_{mp}	= anchor mirror field (T),
β_b	= anchor/barrier beta value,
L_A	= anchor length (cm),
A	= sloshing ion mass (AMU),
ϕ_b	= potential difference between axicell midplane and anchor midplane (keV), and
$\phi_{A'}$	= potential difference between axicell midplane and sloshing beam injection point--"point A'" (keV).

For a sloshing deuterium injection energy of 475 keV, an anchor beta of 65%, a length of 7 m, and a mirror field of 6 T, the required anchor midplane field is 2.26 T. This is the value specified for MARS.

2.5.3 Beam Trapping

The efficiency of trapping the sloshing beam in the plasma is the lowest of all beams in MARS, about 45%. The trapping efficiency f_T is computed by the formula

$$f_T = 1 - \exp \left[- \int_0^{d_{eff}} dr \frac{n(r)}{v_{beam}} (\langle \sigma v \rangle_{ion} + \langle \sigma v \rangle_{cx}) \right],$$

where

d_{eff}	= the effective path length the beam traverses (cm),
$n(r)$	= the (radially dependent) plasma density (cm ⁻³),
v_{beam}	= the beam speed (cm/s),
$\langle \sigma v \rangle_{ion}$	= the reaction rate parameter for ionization, both from the ions and electrons (cm ³ /s), and
$\langle \sigma v \rangle_{cx}$	= the reaction rate parameter for charge exchange (cm ³ /s).

The reaction rate parameter should be the result of averaging σv over the sloshing distribution and the background distributions, neither of which can be strictly modeled as a beam distribution (delta function) or a Maxwellian. We do not know the detailed forms of the distribution, so we assume that an average between a beam and a mirror-confined distribution models the process to sufficient accuracy. When we perform this average and replace the radial average by the major radius of the elliptical plasma times the volume-averaged density at the injection point, the formula for f_T becomes

$$f_T = 1 - \exp \left[- 1.85 \times 10^{-14} \left(\frac{\gamma}{\gamma + 2} \right) n_{A'} r_{A'} \left(\frac{A}{E_{inj,A'}} \right)^{0.91} \right],$$

where

- γ = radial profile factor for density, i.e., $n(r)$,
- $n_{A'}$ = total density at injection point A' , $n_{A'} = n_{slosh,A'} + g_{A'} n_{pass,A'}$ (cm^{-3}),
- $r_{A'}$ = major radius of ellipse at injection point mapped through magnetic flux conservation from central cell (cm),
- A = mass of sloshing ion (AMU), and
- $E_{inj,A'}$ = sloshing beam injection energy (keV).

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2.6 ECRH PHYSICS

2.6.1 General Considerations

The field in the anchor has been modelled using a paraxial yin-yang model with the following parameters:

- Vacuum mirror ratio $R_{\text{vac}} = 2.7$;
- Mirror-to-mirror distance $\ell_p = 8.0 \text{ m}$;
- Ellipticity 5 ;
- Minimum field $B_p = 2.2 \text{ T}$.

With these values, the other parameters in the model become:

- $\gamma = 16.4$;
- $k_p = 2.01 \times 10^{-3} \text{ cm}^{-1}$;
- $k_m = 5.06 \times 10^{-3} \text{ cm}^{-1}$.

A scale length of 20 cm was introduced to model the density decay in the radial direction.

The field is described by

$$B_x = \left[-k_p f(z) - \frac{1}{2} \frac{\partial f}{\partial z} \right] x,$$

$$B_y = \left[k_p f(z) - \frac{1}{2} \frac{\partial f}{\partial z} \right] y, \text{ and}$$

$$B_z = f(z),$$

where

$$f(z) = \frac{1}{2} B_p \left(1 + \frac{\gamma}{4} \right) \left[\frac{1}{1 + k_m^2 \left(z + \frac{1}{2} \ell_p \right)^2} + \frac{1}{1 + k_m^2 \left(z - \frac{1}{2} \ell_p \right)^2} \right].$$

The peak electron density was set at $n_{e,0} = 2.8 \times 10^{13} \text{ cm}^{-3}$, corresponding to a plasma frequency of $f_p = 47.5 \text{ GHz}$.

Such a field has on-axis values of:

- 2.2 T at $z = 0$ (midplane of yin-yang);
- 2.32 T at $z = \pm 200 \text{ cm}$ (point A);
- 6.0 T at $z = \pm 400 \text{ cm}$ (yin-yang boundary).

The midplane of the yin-yang (point B) coincides with a minimum of the potential, while point A is situated at the potential peak.

As a preliminary step, a ray-tracing and absorption calculation was performed at point A. The absorbing electrons have a temperature of 89 keV and the beta-corrected field is 2.3 T at $\beta = 0.5$. At these temperatures, the relativistic shift and broadening of the resonance is estimated to be 40% of the nominal resonance frequency, resulting in a launching fundamental frequency of about 40 GHz. Furthermore, the results of these preliminary calculations showed that, with this combination of parameters, absorption and penetration of the extraordinary mode will be fair. This can be explained by the finite density effects on the absorption, which tend to considerably reduce the absorptivity close to the plasma frequency.

A similar study at point B--the midplane of the anchor--is not feasible because the mean energy of the electrons is 460 keV, and at this energy level no reliable absorption model is available, at least not for frequencies at or below the fundamental cyclotron frequency (28 GHz with beta correction and an estimated 40% reduction for relativistic shift).

2.6.2 Relativistic ECRH Frequency Shifts

The preliminary calculations lead us to believe that, in steady-state operation, a combination of parameters may be found that provides both acceptable penetration and absorption. The spatial distribution of the absorbed energy remains a major problem, however, as excessive heating of the plasma surface may cause temperature profile inversion and possible MHD instability. "Burn in" due to increased plasma transparency at relativistic energies may alleviate this problem.

To achieve penetration to the plasma core, it is necessary to heat at a much lower frequency than the nominal resonance. This implies that the heated electrons are heavily Doppler (or relativistically) shifted, and this in turn can drastically alter the distribution function, possibly leading to micro-instabilities.

2.6.3 ECRH at the Second Harmonic

To minimize losses and avoid the use of metallic parts and transparent windows as much as possible, a quasi-optical energy transfer system can be used to transport the wave energy from its source to the plasma (see Section 3.2.4). Lowering the applied frequency to 28 GHz in the midplane of the anchor and 40 GHz at the potential peak brings one dangerously close to the lower limit of the range where quasi-optical techniques can be successfully applied. We are investigating the use of second harmonic absorption as the principal heating mechanism. This implies the use of gyrotrons operating at higher frequencies that, however, are either available (60 GHz) or are under development (120 GHz). The main advantage in adopting these higher frequencies will be operation farther in the optical regime and the accompanying reduction of the launching system dimensions. The consequences for the power density in this system remain to be investigated.

Until very recently, we had planned to fuel and heat the MARS axicell using 200-keV neutral beams. Because of the space required for the magnet support structure and neutron shielding of the axicell superconductor, sufficient duct space for the neutral beams could not be maintained. We therefore decided to switch to ICRH of the axicell plasma. The fuel for the axicell is then the cold passing ions that are heated by the radio frequency (RF) fields and trapped in the mirrors.

A number of authors have already considered RF heating in tokamaks and mirrors. Stix¹ and Kesner² have analyzed the use of the MHD or fast wave for second-harmonic ICRH of the ions with a view toward two-component operation of tokamaks. Kesner has further looked at bulk heating of ions by the fundamental frequency in a single-cell mirror with potential application to the plugs of the Phaedrus tandem mirror. Experiments have been carried out on Phaedrus with fundamental³ and harmonic⁴ heating of the plugs, as well as fundamental heating of the center-cell plasma.⁵

The harmonics of the ion cyclotron frequency preferentially heat higher energy ions, while the fundamental heats the bulk ions. This result becomes apparent using the approximate velocity-space diffusion coefficient which scales as $(\rho_i k_{\perp})^{2n-2}$, where ρ_i is the ion Larmor radius, $k_{\perp}$ is the perpendicular wave number, and $n = 1, 2, 3, \dots$ is the number of the harmonic ($n = 1$ is fundamental). Thus, the higher the harmonic, the higher the energy of the ions that will be heated. To effectively trap the passing ions, we could use heating by the fundamental. However, this could cause unwanted heating of the passing central-cell ions that are already adequately heated by the alpha particles. Thus, the initial plan is for heating at the second harmonic of tritium, or about 180 MHz in the vacuum field and 136 MHz in the plasma field. Tritium is chosen over deuterium because of its better confinement time against Coulomb scattering. If Fokker-Planck studies indicate insufficient trapping to feed the axicell, it may be necessary to add heating at the fundamental frequency.

Several problem areas must be analyzed in connection with RF heating. These include antenna design, wave propagation, wave conversion (e.g., to ion Bernstein modes), absorption by electrons via transit-time effects and Landau damping, and the effect of the RF fields on the ion distribution function.

Previous theoretical studies of ICRH of fusion plasmas have used quasi-linear theory to describe the effect of the RF field on the distribution function.^{1,2} This theory applies when the plasma is sufficiently collisional to destroy the correlations of particles with the RF field. The very high density of the axicell should render this approximation valid. We can then use quasi-linear diffusion coefficients in velocity space in a numerical code that solves the Fokker-Planck equation.⁶ Considerable work has already been done concerning the use of quasi-linear approximations with this code to study ECRH.⁷

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2.8 PLASMA STARTUP

The discussion of the physics aspects of MARS has so far been concerned with steady-state operating conditions. Obviously, we have to consider the problem of initiating the plasma and, by heating and fueling, bringing it to the desired operating condition. We have not yet fully evaluated the startup process, but some initial work on both the beginning and final phases of startup has been done.

2.8.1 Initial Startup Phase

The startup sequence envisioned for MARS is similar to that planned for TMX-U¹ and the axicell version of MFTF-B.² Plasma stream guns in the end walls create a low-density ($n \approx 10^{12} \text{ cm}^{-3}$) stream plasma in both anchors. Startup neutral beams are then injected into each anchor to provide a hot, mirror-confined anchor plasma ($n \approx 1.6 \times 10^{12} \text{ cm}^{-3}$, $E \approx 15 \text{ keV}$). The anchor plasma density is deliberately kept low at this phase so that the electrons can be heated with about 10 MW of ECRH power. The required startup neutral beams are 20 to 25 keV and 200 A at each end. The anchor plasma provides MHD stability for the machine and some potential for confinement of the central cell ions so that the central cell is microstable.

Microstability of the anchor plasma is initially assured by the plasma stream. To maintain microstability when the stream guns are turned off, we first create a central cell plasma whose axial loss is sufficient to stream-stabilize the anchors. This requires gas puffing and ICRH into the central cell. Once the central cell loss is large enough to stream-stabilize the anchors, the plasma stream guns are turned off.

The central-cell plasma must be heated at low density to reduce the trapping rate in the thermal barriers before the thermal barriers can be established. This is because of the limit on the available barrier pumping current. The combination of reduced collisionality to establish the thermal barriers and sufficient loss-rate for stream-stabilizing the anchor provides an "operating window" for the central cell plasma. This window is illustrated in Fig. 2.8-1 in the parameter space of central cell density and ion temperature for a given electron temperature. The shaded region defines the central-

Fig. 2.8-1. Operating window in the central cell for creation of the thermal barrier.

cell operating window before the thermal barriers can be established. Also shown are the lines of constant ICRH power required to sustain the central-cell plasma at that density and temperature against end loss.

Once the central-cell plasma is in the operating window, ECRH in the anchor can be turned on to heat the electrons. As the electron temperature rises, the anchor density will rise as a result of reduced ion drag. Once the anchor is at a sufficient density to trap the higher-energy sloshing ion beam, this beam can be turned on, the transition region pump beams can be started, and ECRH can be applied to begin thermal barrier operation. The axicell plasma is begun when required for reducing the trapping rate in the thermal barrier. The plasma now has all the elements of the desired configuration; the final step is to bring the plasma to the design operating point. This phase will likely require more ICRH power than Fig. 2.8-1 shows, although the required power has not yet been evaluated.

2.8.2 Final Startup Phase

In this subsection, we examine the requirements for startup of the MARS plasma after the thermal barriers have been established--the so-called final startup phase. At first, the axicell and barrier plasmas exist at their full beta values, but the central-cell plasma is initially of lower beta, density, and temperature than the operating point. The goal is to keep the barriers at roughly constant parameters while bringing the central cell up to the final conditions. To study startup, the steady-state plasma performance code is exercised at intermediate operating points through which a plausible startup path might pass. For each intermediate operating point, the sustaining end cell powers are computed. For a particular scenario to be deemed "successful," these powers should not exceed the operating point powers.

Table 2.8-1 shows the cases run using the physics model. Table 2.8-2 defines the parameters of Table 2.8-1. Note that the final reactor performance (i.e., Q) is higher than the baseline reported elsewhere in this volume. This is because at the time these cases were run, we did not appreciate the importance of achieving adequate pump beam penetration. This tends to increase both axicell power and pumping power, reducing performance. The chosen values of central-cell beta shown were determined to keep the central-cell plasma radius constant for the given P_{fus} and ion temperature. The cases

Table 2.8-1. Physics cases used to model the final startup phase.

P_{fus}	T_c	β_c	I_{cc}	P_{axi}	$P_{slosh}(f_{trap})$	$P_{ECRH,A}$	$P_{ECRH,B}$	P_{pump}	Q
600	25	0.29	0.4	14.2	2.3 (0.36)	0.83	60	11.7	6.7
800	25	0.33	0.5	13.5	2.9 (0.39)	1.4	60	17	8.5
1000	30	0.37	0.6	14.4	4.31 (0.43)	2.	70	18	9.2
1500	32	0.46	0.7	14.1	6.6 (0.47)	3.4	80	27	11.4
2000	35	0.53	0.9	14.4	11.1 (0.51)	5.3	97	33	12.4
2500	35	0.59	1.1	13.9	12. (0.52)	5.8	97	45	14.1
3000	35	0.65	1.2	13.2	13.5 (0.52)	6.3	97.4	60	15.7
3500	35	0.7	1.4	12.8	13.8 (0.52)	6.1	92.3	80	17.1

Table 2.8-2. Startup parameter definitions.

Parameter	Definition
P_{fus} (MW)	D-T fusion power produced in central cell
T_c (keV)	Central-cell ion temperature
β_c	Central-cell beta value
I_{cc} (kA)	Cold refueling current required in central cell
P_{axi} (MW)	Sustaining power required in axicell
P_{slosh} (MW)	Sustaining power required to produce the sloshing ions in the barrier region
f_{trap}	Trapping fraction of the sloshing neutral beam
$P_{ECRH,A}$ (MW)	ECRH power required to sustain the warm electrons at the confining potential peak for ions
$P_{ECRH,B}$ (MW)	ECRH power required to sustain the hot electrons at the thermal barrier
P_{pump} (MW)	Charge-exchange pumping power in transition region
Q	Plasma gain factor P_{fus}/P_{inj} , where P_{inj} is the sum of the above sustaining powers

were run down to about 600 MW of fusion power at which point solutions became difficult to obtain with the code. This could have been caused by one of two things: (1) the starting guesses for computed plasma parameters were not good choices for powers below 600 MW; or (2) some kind of supplementary heating in the central cell is needed below this power level. More work is necessary to resolve this question. If the second possibility turns out to be the correct one, the results presented here suggest that the reactor can be "throttled" by backing off on injected fuel and end cell power so that it puts out as little as 1/6 of its rated output, in the same operating mode as full power operation. This should be a desirable feature in establishing the utility acceptability of the tandem mirror.

Examining the cases presented in Table 2.8-2, we see that the sustaining fueling current and powers are either monotonically increasing functions or independent of P_{fus} . There is a slight increase ($\sim 10\%$) of both the axicell power and the ECRH power used to produce the barrier hot electrons. We feel that these bumps can be removed with better programming of β_c and the ratios of total barrier density to the central-cell density passing into the barrier region. Considerably more work is necessary to bridge the gap between the initial and the final startup phases.

REFERENCES FOR SECTION 2.8

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2.9 END LEAKAGE PHYSICS

2.9.1 Electrostatic Direct Conversion

Almost all of the trapped injected power plus the alpha power streams out the ends of a tandem mirror reactor in the form of energetic charged particles. The total current of lost ions must exactly equal the current of electrons for the plasma to remain neutral. By separating the positive and negative currents, we can directly recover electric power and improve the efficiency of the reactor.

If P_F is the fusion power and $P_{in} = P_F/Q$ is the total trapped injected power, the power carried out by charged particles is

$$P_{chg} = P_F (1/Q + 0.2) - P_{rad} - P_{cx}$$

A small amount of power, P_{rad} , is radiated to the walls--mostly as bremsstrahlung from the central cell and as synchrotron radiation from the barriers. Also, the pump beams result in P_{cx} of fast neutrals striking the walls in the vicinity of those beams. The remainder of P_{chg} is carried out by charged particles, either to the ion collector at one end, to the electron collector at the other end, or radially to the halo plasma.

2.9.2 Distribution of Electrical Potential

The potential of the central cell is positive by an amount ϕ_e relative to the end walls. This positive potential difference results from the fact that electrons tend to escape faster than the more massive ions that are confined by the potential ϕ_c . The plasma properties are unaffected by our choice of reference potential as long as the differences in potential are unaffected. We have chosen to ground the vacuum vessel and the halo dumps. Because the halo plasma is a very good electrical conductor, this effectively grounds the entire halo. Then, to avoid strong radial electric fields in the central cell, we have adjusted the potential of the central cell plasma to near that of the halo by lowering the potential of the end walls as shown in Fig. 2.9-1. The end walls in this case consist of the electron collector at one end and the first grid before the ion collector at the other end. These are the parts of the end walls that affect the electron transport.

Fig. 2.9-1. Magnetic field and potential in the end cells. Relative to the grounded halo plasma, the central cell potential is positive by about T_{ec}/e .

Some radial variation in the central cell potential still exists, partly because of the radial variation of potential in the end cell plasmas, which are also enclosed by the grounded halo. We rather arbitrarily assume a potential difference of T_{ec}/e between the axis in the central cell plasma and the halo. There is therefore a radial electric field in the central cell plasma on the order of T_{ec}/er_p , where r_p is the plasma radius. This electric field, in the presence of the longitudinal magnetic field, causes the central cell plasma to rotate and produce an outward centrifugal force on the particles. The destabilizing effect of this outward force is negligible as long as the electric field is as small as T_{ec}/er_p . However, if we had not reduced the potential in the central cell, the electric field would have been on the order of ϕ_e/r_p and would have had a serious destabilizing effect (see Section 2.2.4). Stability of the central cell plasma therefore requires that the potential of the electron collector and the first grid at the ion collector be about ϕ_e negative relative to the vacuum vessel and the halo plasma. We have chosen to set them at $T_{ec}/e - \phi_e$.

2.9.3 Selective Leakage

Central cell ions are confined axially by the positive potential peaks in the end plugs (see Fig. 2.9-1). They escape by diffusing up in energy until they can pass over the peaks. We can cause the selective leakage of ions out one end by raising the potential peak at the other end. The fractional increase in potential $\Delta\phi_c/\phi_c$ must be a few times the probable fractional increase in ion energy in one transit through the central cell. That is,

$$\Delta\phi_c \gg \phi_c \sqrt{\frac{L_c}{v_i \tau}},$$

where L_c is the length of the central cell, v_i is the mean longitudinal velocity of the ions, and τ is their average confinement time. In MARS, this only requires $\Delta\phi_c \gg 1$ kV. The practical condition for $\Delta\phi_c$ is, therefore, that $\Delta\phi_c$ be held larger than any likely accidental variation in ϕ_c that could selectively leak the ions out the wrong end.

The suppressor grid at the ion collector reflects electrons and causes the selective leakage of central cell electrons out the other end. As Fig. 2.9-2 shows, any electron energetic enough to reach the grids at the ion

Fig. 2.9-2. Comparing the potential variation in the two ends: $V_{ec} = \phi_e + T_{ec}/e$, $V_{g1} = V_{ec}$, $V_{g2} = V_{ec} - 4 T_{ec}/e$.

collector is also energetic enough to reach the electron collector at the other end. Once an electron gains enough energy to reach the ends, its kinetic energy as it passes through the central cell will be at least $e\phi_e$ and its scattering rate will be very low. It can then easily pass from one end of the machine to the other and be collected at the electron collector before it can penetrate the suppressor grid.

The first grid at the ion collector is at the same potential as the electron collector, while the suppressor grid is more negative by about $4 T_e/e$. This rather large potential difference is required to reflect all but about e^{-4} of the electrons, because the reflection takes place where the magnetic field is weak. The perpendicular energy ($\sim T_e$), which otherwise would not affect the reflection, is converted to directed energy by the decrease in magnetic field.

The selective leakage of ions out one end and electrons out the other results in a net longitudinal current of more than 2000 A. This current produces an azimuthal $B_\theta \sim 10^{-3}$ T, which is negligible.

2.9.4 The Halo Plasma

A halo plasma is necessary to prevent high-Z impurities from entering the core plasma. Because the confinement time of electrostatically confined ions varies approximately as $e^Z \phi / T_i$, high-Z ions are confined better than fuel ions. If allowed to enter, they would accumulate, diluting and cooling the plasma.

For the same reason, alpha particles tend to accumulate in the central cell. In the present design, alphas are removed by grad-B-induced radial diffusion in the transition regions (see Section 2.4). When they reach the radius of the halo plasma, they are on flux lines that pass outside of the confining potential. They can then flow along the flux lines to the halo dumps in the end tanks.

About 70% of the alpha particles must be removed by radial diffusion into the halo; about 5% are born in the magnetic loss cone and are lost immediately out the ends, while another 25% scatter into the loss cone after partially thermalizing with the central cell plasma. Also, because radial diffusion is not selective, some fuel ions are removed radially. About 10 fuel ions are

removed for each alpha since the density ratio is about 10:1. The production rate of alphas is $I_\alpha = 2P_F/E_F = 400$ A (electrical). Therefore 280 A flows into the halo accompanied by about 1400 A of fuel ions. Each alpha and each fuel ion carries $3/2 T_i$ of energy into the halo where T_i is the ion temperature at the surface of the core plasma. Here we assume $T_i = T_{ic}$. The total power flow out of the core plasma and into the halo is therefore 80 MW if $T_{ic} = 35$ keV. In general, the radial current of alpha particles is $I_{\alpha,r} = f_r I_\alpha$, where $I_\alpha = 2P_F/E_F$ and f_r is the fraction of the alphas that are removed radially. The total radial current is then $I_r = I_{\alpha,r} (1 + \frac{1}{2c_\alpha})$, where $c_\alpha = n_\alpha/n_{DT}$ is the alpha density ratio in the central cell. Also, the radial alpha power is $H_{\alpha,r} = I_{\alpha,r} E_{\alpha,r}/2$, where $E_{\alpha,r}$ is the average energy carried out by each radial alpha. The total radial power is $H_r = I_{\alpha,r} (E_{\alpha,r} + 3 T_{ic}/2c_\alpha)/2$. Our preliminary estimates are $f_r = 0.7$, $c_\alpha = 0.1$, and $E_{\alpha,r} = 3/2 T_{ic}$.

Because the halo dumps are at ground potential, no electrical power is recovered from the halo. Most of the 80 MW appears as heat at the halo dumps, while a small fraction of it adds heat to the first wall.

2.9.5 Power Balance

Steady-state charge neutrality requires that the net electrical current out of the reactor be zero. Furthermore, P_{chg} , the power carried out by charged particles, is known. These two facts allow us to solve for the particle currents in terms of the different potentials, particle energies, and input powers. Setting the electron current equal to the total electrical ion current gives

$$I_{e,th} + I_{e,b} = I_{i,th} + I_{s1} + f_r I_\alpha \left[1 + \frac{1}{2c_\alpha} \right] + (1 - f_r) I_\alpha,$$

where $I_{e,th}$ is the current of escaping central-cell electrons, $I_{e,b}$ is the current of ECRH-heated superthermal electrons, $I_{i,th}$ is the end-loss current of fuel ions from the central cell, I_{s1} is the current of the sloshing ions from the anchor, and the last two terms represent the total radial current and the end loss alpha current.

We can write P_{out} , the total power carried out by charged particles, by considering the case where all particles are collected at ground potential. Then, assuming $E_{\alpha,r} = 1.5 T_{ic}$,

$$\begin{aligned}
P_{\text{out}} = & [(1 - f_r) I_\alpha \left(\frac{E_{\alpha, \text{end}}}{2} + T_{\text{ec}} \right) + f_r I_\alpha \left(1 + \frac{1}{c_\alpha} \right) \frac{3T_{\text{ic}}}{4} \\
& + I_{\text{s1}} (E_{\text{s1}} + e\phi_c + T_{\text{ec}}) + I_{\text{e,b}} (E_{\text{e,b}} + e\phi_b - e\phi_{\text{pc}} - T_{\text{ec}}) \\
& + I_{\text{e,th}} e\phi_e + I_{\text{i,th}} (e\phi_c + T_{\text{ic}} + T_{\text{ec}})] \frac{1}{e} .
\end{aligned}$$

When the particles are collected at nonzero potentials, the power flowing out of the reactor is of course not changed. The algebraic sum of the heat produced plus the electrical power recovered remains equal to P_{out} .

Because $P_{\text{out}} = P_{\text{chg}}$ is known, we can solve these two equations for $I_{\text{i,th}}$ and $I_{\text{e,th}}$ in terms of the potentials, energies, and currents that are calculated by the physics code. (Alternatively, we could obtain $I_{\text{i,th}}$, and therefore $I_{\text{e,th}}$, from the n_i values calculated by the physics codes.) The values of the currents $I_{\text{i,th}}$ and $I_{\text{e,th}}$ are needed to calculate the heat and electrical power produced when the charged particles are recovered.

2.9.6 The Electron Collector

We assume that selective leakage allows all escaping thermal electrons to be collected at the electron collector. However, the loss currents I_{s1} of sloshing ions, $(1 - f_r) I_\alpha$ of end-loss alphas, and $-I_{\text{e,b}}$ of ECRH-heated superthermal electrons are non-selective. These currents are divided equally between the two ends. The total net current I_{ec} to the electron collector is therefore

$$I_{\text{ec}} = -I_{\text{e,th}} + \frac{1}{2} I_{\text{s1}} + \frac{1}{2} (1 - f_r) I_\alpha - \frac{1}{2} I_{\text{e,b}} .$$

The current $-I_{\text{e,th}}$ of escaping thermal electrons is evaluated from the power and particle balance. For I_{s1} we take all of the injected and trapped sloshing-ion current, since these ions have a much higher probability of escaping out the end than they have of entering and being trapped in the plasma. That is, $I_{\text{s1}} = P_{\text{s1}}/E_{\text{s1}}$, where P_{s1} is the trapped sloshing ion power and E_{s1} is the injection energy. We tentatively assume that 10% of the hot, ECRH-heated barrier electrons escape out the ends, while the other 90% thermalize before

escaping. We take $I_{e,b} = 0.1 \times e P_{\text{ECRH},b} / E_{e,b}$, where $P_{\text{ECRH},b}$ is the ECRH power absorbed by barrier electrons, and $E_{e,b}$ is the mean energy of the hot electrons in the barrier.

The electric power recovered from the electron collector is $P_{ec} = I_{ec} \times V_{ec}$, where $V_{ec} = -\phi_e + T_{ec}/e$ (see Fig. 2.9-2). The heat generated at the electron collector H_{ec} is obtained from the currents and impact energies of each of the species. The heat is given by

$$H_{ec} = I_{e,th} \frac{T_{ec}}{e} + \frac{1}{2} I_{s1} \left(\phi_e + \phi_c + \frac{E_{s1}}{e} \right) + \frac{1}{2} (1 - f_r) I_{\alpha} \left(\phi_e + \frac{E_{\alpha, \text{end}}}{2e} \right) \\ + \frac{1}{2} I_{e,b} \left(-\phi_e - \phi_{pc} + \phi_b + \frac{E_{e,b}}{e} \right),$$

where $E_{\alpha, \text{end}}$ is the mean energy of the alphas as they escape out the magnetic loss cone. It is calculated from

$$E_{\alpha, \text{end}} = \frac{f_{lc} E_{\alpha 0} + (1 - f_r - f_{lc}) E_{\alpha, lc}}{1 - f_r},$$

where $E_{\alpha, lc}$ is the average energy of the alphas as they scatter into the loss cone and $E_{\alpha 0} = 3520$ keV is the energy of those alphas that are born in the loss cone. We have assumed that $f_r = 0.7$ and $E_{\alpha, lc} = 400$ keV. Here, f_{lc} is the fraction of the alphas that are born in the loss cone, and $f_{lc} = 1 - (1 - 1/R)^{1/2}$, where R is the β -increased mirror ratio for the central cell. For MARS, where $B_c = 4.7$ T, and $B_{m2} = 24$ T, and $\beta_c = 0.7$, the mirror ratio is $R = 9.32$ and $f_{lc} = 0.055$. In that case, $E_{\alpha, \text{end}} = 972$ keV.

A single grid is required at the electron collector to suppress the emission of secondary electrons from the collector. The grid must therefore depress the potential by at least 20 V (i.e., a few times T_{es}/e for secondary electrons) everywhere in front of the collector. The potential of the cold plasma in the end tank will adjust until the loss rate for cold electrons is equal to that for cold ions. Its value will therefore depend on the probability that a cold ion can strike either the grid or the collector plate,

compared to the probability that a cold electron is lost to the collector. These probabilities depend on the geometry and potential of the grid as well as on the strength of the magnetic field. A sheath that is a few Debye lengths thick forms on the plasma side of the grid. It is the drop in potential across this sheath that adjusts to equalize the loss rates. A sketch of the variation in potential with distance out from the collector is shown in Fig. 2.9-3(a).

Figure 2.9-3(b) shows a sketch of some equipotential contours near the grid and defines the dimensions r , a , and d that determine a grid's performance. The grid plane is spaced a distance $d = 30$ cm from the collector plate to allow the spacing between tubes, $a = 20$ cm, to be less than d but much greater than the tube diameter $2r = 1$ cm. This provides high grid transparency with tubes that are large enough for convective cooling. By applying 300 V between the grid and the collector, essentially all secondary electrons can be turned back, even between grid tubes and in the presence of the cold plasma. We model the cold plasma by applying an electric field equal in magnitude to $T_e/e\lambda_{De}$ to represent the sheath on the plasma side of the grid. Here, $\lambda_{De} \sim 3$ mm is the Debye length and $T_e \sim 10$ eV is the temperature of the cold electrons. The interface between plasma and sheath is then given approximately by the equipotential contour where the potential is equal to the plasma potential. Because the plasma potential can differ from that of the collector by only a few times T_e/e , this technique should locate the sheath to within a few λ_{De} of its true location. The cold plasma density is determined by the balance between production and loss rates and is estimated in Section 2.9.8.

The cold plasma results in a net current flow from collector to grid, and is therefore a drain on the 300-V power supply. The power consumed is completely negligible, and for power flow analysis, the collector and the grid can be treated as a single unit. For this reason and because the primary electrons are poor ionizers, the vacuum requirement at the electron collector end is less stringent than at the ion collector end where the pressure is 2×10^{-5} Torr.

Fig. 2.9-3. (a) The variation in potential near the electron calculator and its single grid; (b) grid configuration with equipotential contours. Potential is given in volts relative to that of the electron collector ($V_{ec} = 0.176$ kV).

2.9.7 The Ion Collector End

The currents of primary particles arriving at the entrance grid to the ion collector can be written analogously to those at the electron collector. The net incident current is

$$I_{inc} = I_{i,th} + \frac{1}{2} I_{s1} + \frac{1}{2} (1 - f_r) I_{\alpha} - \frac{1}{2} I_{e,b},$$

where $I_{i,th}$ is the current of end loss thermal fuel ions, all of which are selectively leaked out the one end. The other currents are as defined for the electron collector.

The entrance grid (g1) intercepts a fraction $1 - T_{g1}$ of I_{inc} , and the suppressor grid (g2) intercepts $T_{g1}(1 - T_{g2})I_{inc}$, where T_{g1} and T_{g2} are the transparencies of the two grids. The large negative potential of g2 depresses the potential around each element of g1 and prevents the release of secondary electrons from g1. Secondary electrons from g2 all flow to the ion collector.

In addition to the intercepted fractions of I_{inc} , the grids also collect all of the currents of cold ions and electrons produced by ionization and charge exchange in the end tank. The cold electrons all go to g1, and, because of deflection and trapping in the potential well near g2, most of the cold ions go to g2. Because they impact g2 with about 100 keV of energy, the cold ions also release secondary electrons that flow from g2 to the ion collector. We assume that the bombardment keeps the grid material free of oxides and absorbed gas, and therefore take a rather optimistic value of $\gamma \approx 2$ for the secondary electron emission coefficient for the "cold" ions as well as for the primary ones.

As can be seen in Fig. 2.9-2, g1 is held at the potential of the electron collector, while g2 is 100 kV more negative. The potential of the ion collector is set as high as possible without reflecting any of the primary ions. Figure 2.9-2 also shows that all primary ions are accelerated by the difference in potential $\phi_e + \phi_c$ before reaching g1. To allow for 10-deg scattering of the incident ions by the grids, we set the ion collector potential at $(\phi_e + \phi_c) \cos^2 (10 \text{ deg})$ above that of g1. The grid parameters in MARS are chosen to limit the scattering to less than 10 deg. Relative to ground, V_{g1} of g1, V_{g2} of g2, and V_{ic} of the ion collector are

$$V_{g1} = -\phi_e + \frac{T_{ec}}{e},$$

$$V_{g2} = -\phi_e - \frac{3T_{ec}}{e}, \text{ and}$$

$$V_{ic} = (\phi_e + \phi_c) \cos^2(10 \text{ deg}) - \phi_e + \frac{T_{ec}}{e}.$$

The net currents collected by each of these three electrodes are

$$I_{g1} = (1 - T_{g1})I_{inc} - \frac{I_c}{2} - \frac{I'_c}{12},$$

$$I_{g2} = T_{g1}(1 - T_{g2}) \left[\left(I_{inc} + \frac{I_{e,b}}{2} \right) (1 + \gamma) - \frac{I_{e,b}}{2} \right] + \left(\frac{I_c}{2} + \frac{3I'_c}{4} \right) (1 + \gamma), \text{ and}$$

$$I_{ic} = T_{g1}T_{g2}I_{inc} - T_{g1}(1 - T_{g2})\gamma \left(I_{inc} + \frac{I_{e,b}}{2} \right) - \gamma \left(\frac{I_c}{2} + \frac{3I'_c}{3} \right) - \frac{2I'_c}{3}.$$

Here, γ is the secondary electron emission coefficient and I_c and I'_c are currents of cold ions (and electrons) that result from the ionization of gas in the end tank. The difference between I_c and I'_c is caused by the regions where the ionization occurs, and is discussed later. Notice that $I_{g1} + I_{g2} + I_{ic} = I_{inc}$, as it should, since both secondary emission and cold plasma result only in currents from one electrode to another, with no net output current.

The electrical power recovered (+) or consumed (-) at each electrode is obtained by multiplying the current collected by the electrode voltage

$$P_{g1} = I_{g1}V_{g1}, P_{g2} = I_{g2}V_{g2}, P = I_{ic}V_{ic}.$$

We can determine the heating of each electrode from the currents and impact energies of each species at each electrode such that

$$H_{g1} = (1 - T_{g1}) \left[I_{i,th} \left(\phi_e + \phi_c + \frac{T_{ic}}{e} \right) + I_{s1} \left(\frac{\phi_a + \phi_c + \frac{E_{s1}}{e}}{2} \right) \right. \\ \left. + (1 - f_c)I_\alpha \left(\frac{\phi_e + \frac{E_{\alpha, end}}{2e}}{2} \right) + I_{e,b} \left(\frac{\phi_b - \phi_e - \phi_{pc} + \frac{E_{eb}}{2}}{2} \right) \right] +$$

$$I_c' \left[\frac{4T_{ec}}{9e} + \left(\frac{V_{ic} + \phi_e - T_{ec}/e}{12} \right) \right],$$

$$H_{g2} = T_{g1}(1 - T_{g2}) \left[I_{i,th} \left(\phi_e + \phi_c + \frac{T_{ic}}{e} + \frac{4T_{ec}}{e} \right) \right.$$

$$+ I_{s1} \left(\frac{\phi_e + \phi_c + \frac{4T_{ec}}{e} + \frac{E_{s1}}{e}}{2} \right)$$

$$+ (1 - f_r) I_\alpha \left(\frac{\phi_e + \frac{E_{\alpha, end}}{2e} + \frac{4T_{ec}}{e}}{2} \right)$$

$$+ I_{e,b} \left(\frac{\phi_b - \phi_e - \phi_{pc} + \frac{E_{e,b}}{e} - \frac{4T_{ec}}{e}}{2} \right) \Big]$$

$$+ I_c \left(\frac{4T_{ec}}{2e} \right) + I_c' \left(\frac{\frac{28T_{ec}}{e} + V_{ic} + \phi_e - \frac{T_{ec}}{e}}{12} \right),$$

$$H_{ic} = T_{g1}T_{g2} \left[I_{i,th} \left(\phi_c + \frac{T_{ic}}{e} + \frac{T_{ec}}{e} - V_{ic} \right) \right.$$

$$+ I_{s1} \left(\phi_c + \frac{T_{ec}}{e} + \frac{E_{s1}}{e} - V_{ic} \right)$$

$$+ (1 - f_r) I_\alpha \left(\frac{T_{ec}}{e} + \frac{E_{\alpha, end}}{2e} - V_{ic} \right)$$

$$+ I_{e,b} \left(\phi_b - \phi_{pc} - \frac{T_{ec}}{e} + \frac{E_{e,b}}{e} + V_{ic} \right) \Big]$$

$$+ \gamma T_{g1}(1 - T_{g2}) \left[I_{i,th} + (1 - f_r) \frac{I_\alpha}{2} + \frac{I_{s1}}{2} \right] \left(\frac{3T_{ec}}{e} + \phi_e + V_{ic} \right)$$

$$+ \left[\frac{\gamma I_c}{2} + (2 + 3\gamma) \frac{I_c'}{4} \right] (V_{ic} + \phi_e + \frac{3T_{ec}}{e}) - \left(\frac{34}{9} \right) I_c' \left(\frac{T_{ec}}{e} \right).$$

We have assumed here that g_2 is $4T_{ec}/e$ more negative than g_1 , that no central-cell electrons reach the ion collector, and that all primary ions not intercepted by a grid are collected on the ion collector.

2.9.8 Cold Plasma

Cold ions and electrons are produced in the end tanks as the primary ions and electrons pass through the background gas. The plugging potential prevents the cold ions from entering the confined plasma, and the huge difference in the magnetic field restricts the flow of the cold electrons into the confined plasma. Therefore, most of the cold electrons and all of the cold ions must flow to the electrodes at the ends.

First, consider the current I_c of cold plasma produced before the first grid (g_1) at the ion collector end. The cold electrons are reflected by g_2 , while the cold ions are accelerated through g_2 and reflected by the ion collector. As a result, $-I_c$ of cold electrons flows to g_1 and $+I_c/2$ of cold ions flows to both g_1 and g_2 . Each ion is assumed to release $\gamma \approx 2$ secondary electrons that flow to the ion collector because the electric field on the collector side of the grid is strong.

Another smaller current I_c' of cold ions and electrons is produced between g_1 and the ion collector. The electrode to which an ion or electron goes depends on where it is created.¹ The result is that g_1 receives a net $-I_c'/12$, g_2 receives $+3I_c'/4$ and releases γ times that current of secondaries, and the ion collector receives the $3\gamma I_c'/4$ plus another $2I_c'/3$ of electron current. The energies that these particles have and the heat produced where they are collected is determined by averaging the potential differences through which they fall.

The magnitude of I_c is given by

$$I_c = n_0 \sigma_c L_c \left(I_{inc} + \frac{I_{e,b}}{2} \right),$$

where n_0 is the gas density, σ_c is the total cross section for the production of positive ions and free electrons, and L_c is the path length. We are assuming that the mean energy of the cold plasma electrons is low, and that

their contribution to I_c can be neglected. Ionization by fast electrons from the central cell as well as from the barrier is also negligible. Therefore, we take $\sigma_c = 1.7 \times 10^{-16} \text{ cm}^2$, appropriate for the 385-keV D^+ and T^+ , and multiply it by the fast ion current.² The effective length L_c is somewhat shorter than the distance from the anchor to the first grid because n_0 is small in the beam near the anchor. We take $L_c \approx 20 \text{ m}$. Then $I_c = 1.2 \times 10^4 \times p (I_{inc} + I_{e,b}/2)$, where p is the background gas pressure.

The current I'_c of cold ions produced between g1 and the ion collector is due to charge exchange near the collector. As the fast ions are decelerated σ'_c , the cross section for charge exchange (of D^+ and T^+ on DT gas) increases toward a maximum value of $8.5 \times 10^{-16} \text{ cm}^2$ at about 20 keV. The distance L'_c that ions move at low energy (where σ'_c is large) is only about 0.2 m, so that $I'_c = n_0 \sigma'_c L'_c (I_{inc} + I_{e,b}/2)$ has a value of only about $I'_c \approx I_c/20$.

In our calculations, we set

$$I_c = 1.2 \times 10^4 p (I_{inc} + \frac{I_{e,b}}{2})$$

and

$$I'_c = 6 \times 10^2 p (I_{inc} + \frac{I_{e,b}}{2}).$$

In MARS, the effect of I'_c is negligible, but that of I_c is not. At $p = 2 \times 10^{-5} \text{ Torr}$, the total heating of g2 is 30 MW, and 10 MW of that is due to I_c . Because the size of the direct converter is determined by grid heating, it could be reduced by only one-third even if I_c could be reduced to near zero. Reducing I_c by very much can only be done by reducing p , which implies increasing the vacuum pumping speed. Economics, therefore, determine the best pressure p , and it appears from preliminary cost estimates that the best value is $p \approx 2 \times 10^{-5} \text{ Torr}$ in the ion collector end.

At the electron collector end, the production rate for cold plasma is much less. Because there are no fuel ions from the central cell, most of the cold plasma results from the ionization of gas by the alpha particles that scatter into the loss cone and arrive in the ends with moderate energies. In MARS, about $I_{\alpha,ec} = 50 \text{ A}$ of alphas (particle current) go out each end with about 750 keV average energy. The electron collector in MARS also receives about $I_{e,th} = 2600 \text{ A}$ of 29-keV electrons. The cross sections² for ionization by these two species are $\sigma_\alpha = 5.4 \times 10^{-16} \text{ cm}^2$ (per He^{++}) and $\sigma_e \approx 1 \times 10^{-18} \text{ cm}^2$

(per e^-). We take $L \sim 15$ m and calculate the cold ion production rate in the electron collector end, $I_{c,eC}$, so that

$$I_{c,eC} = n_0 L (I_{\alpha,eC} \sigma_{\alpha} + I_{e,th} \sigma_e) \sim 18 \left(\frac{p}{2 \times 10^{-5}} \right),$$

where p is now the pressure (in Torr) in the electron end tank. Even if p is the same, the cold plasma production rate is much smaller in the electron end than in the ion end.

Cold plasma current causes little loss of power directly in the electron end, but the density of cold plasma determines the Debye length. It is therefore important for the design of the grid. We can estimate the cold electron density n_{ec} from

$$I_{e,eC} \sim n_{ec} v_{ic} A_{ec},$$

where $v_{ic} \sim 2 \times 10^4$ m/s is the average speed of the cold ions (5 eV, $2.5 M_H$) and $A_{ec} \sim 100$ m² is the area of the beam at the collector. This gives $n_{ec} = 6 \times 10^{13}$ m⁻³ if $p = 2 \times 10^{-5}$ Torr in the electron end and $\lambda_{De} \approx 3$ mm if $T_e \approx 10$ eV. This value of λ_{De} was used in Section 2.9.6 in estimating the voltage required for the single grid at the electron collector. The high negative voltage applied to the suppressor grid at the ion collector makes it less sensitive to λ_{De} .

2.9.9 Grids at the Ion Collector

The grids at the ion collector have two purposes: to turn back the primary electrons and to provide sinks for the cold plasma. Our grid design is intended to minimize the interception of primary ions while still performing these two functions.

Figure 2.9-4 shows a section through the direct converter and defines the notation. This is the configuration of a plane triode vacuum tube, with our gl taking the place of the cathode. If we ignore space charge temporarily, we can write the vacuum potential inside the triode as³

$$V(x,y) = A \ln(e^{2Ex} - 2e^{Ex} \cos Ey + 1) + B (d_{gg} + x) + C.$$

Fig. 2.9-4. Plane triode vacuum tube configuration of the ion collector and its two grids.

Here, the origin is at the center of a grid tube in g2 and the x-coordinate is normal to the g2-plane, $E = 2 \pi/a$, and A, B, and C are chosen to satisfy the boundary conditions

$$V(-d_{gg}, 0) = V_{g1}, \quad V(r, 0) = V_{g2}, \quad V(d_{gc}, 0) = V_{ic}.$$

The effect of g2 is weakest in the plane defined by $y = a/2$, and the minimum value of $V(x, a/2)$ occurs at $x = x' = -(a/2\pi) \ln[-(1 + 4\pi A/aB)]$. We require that $V(x', a/2)$ be sufficiently negative to turn back nearly all of the primary electrons. The electrons from the central cell arrive at g1 with a mean energy T_{ec} and are well-directed along the magnetic field because of the decrease in that field. Fokker-Planck (and Monte Carlo) calculations have shown that the primary electrons arriving at g1 have nearly a Maxwellian distribution, and therefore, the fraction that penetrates past g2 is approximately $\exp[e(V' - V_{g1})/kT_{ec}]$. Here, V' is the average of the potential minima in all $y = \text{constant}$ planes. Therefore, $V_{g2} < V' < V(x', a/2)$. For MARS we have chosen the grid parameters shown in Fig. 2.9-4, with $V_{g1} = -176$ kV, $V_{g2} = -276$ kV, and $V_{ic} = +164$ kV. This gives $V(x', a/2) = -234$ kV and $V' = -244$ kV. The value of the exponential, with $T_{ec} = 28.9$ keV, is 0.09, which is too high. However, the distribution deviates from a Maxwellian at high energy, and the actual penetration is much less than 0.09. We assume, therefore, that the 100-kV difference between the g1 and g2 potential is sufficient. To increase that difference would increase the heat H_{g2} and therefore the size of the direct converter.

Space-charge effects modify these results in three ways. First, the cold plasma produces a sheath that tends to shield the plasma from the grid potential. However, the cold electron temperature is less than 100 eV and the electric field just inside g1 is about 1.35 kV/cm. The cold plasma sheath therefore changes the effective d_{gg} ($= 50$ cm) by about 2 mm and changes the 100-kV difference in grid potentials by a few hundred volts. The effect is negligible. Second, the space charge of the primary electrons modifies the electric field between the two grid planes. The 50-cm spacing between planes and the 68-kV average difference in potential would allow 450 A/m^2 of 29-keV electrons before the potential were seriously affected by space-charge.⁴ In MARS, the current density of 29-keV electrons arriving at g1 is only about

charge effect is also negligible. The third space-charge effect is that of the fast ions where they slow down between g2 and the ion collector. Here again the effect is negligible. The current density of D^+ and T^+ ions is about 3.5 A/m^2 , while the 1-m spacing and the 408-kV average difference in potential would require 16 A/m^2 of $T_{1C} = 35\text{-keV}$ ions to be space-charge-limited.⁴

Therefore, the high potentials applied to the electrodes dominate over sheath potentials and other space-charge effects. The potential distribution calculated for the vacuum case is a good approximation to the true distribution.

The power density on any small area of the direct converter is proportional to the production rate of power in the magnetic flux tube intercepted by that area. And, because most of the direct converter power comes from the central cell, the production rate is proportional to the square of the density at the radius where the flux tube passes through the central cell. The mapping of magnetic flux is complicated by the diamagnetic effect of the central-cell plasma. We assume that both n_{1C} and β_C vary with radius r in the central cell as $1 - x^\gamma$, where $x = r/r_p$ and γ is a constant. The long-thin approximation then gives the radial variation of B in the central cell as

$$B = B_C \sqrt{1 - \beta_C (1 - x^\gamma)}.$$

For any position on the direct converter that maps from radius $r = r_p x$ in the central cell, the power density dP/dA is

$$\frac{dP}{dA} = B_{dc} \frac{dP}{d\phi} = \left(\frac{P}{A_{dc}} \right) \frac{f(\beta_C, \gamma, 1) (1+\gamma) (2+\gamma) (1-x^\gamma)^2}{\gamma^2 \sqrt{1 - \beta_C (1-x^\gamma)}}.$$

Here, P is the total power to the direct converter from the central cell, A_{dc} is the area of the direct converter, B_{dc} is the magnetic induction at the direct converter, $d\phi$ is the magnetic flux intercepted by area dA at the direct converter, and $f(\beta_C, \gamma, x)$ is defined by

$$f(\beta_C, \gamma, x) \equiv 2 \int_0^x \sqrt{1 - \beta_C (1-x^\gamma)} x \, dx.$$

The MARS parameters $\beta_c = 0.7$ and $\gamma = 3$ give $f(0.7, 3, 1) = 0.749$ and

$$\left(\frac{dP}{dA}\right)_{x=0} = \left(\frac{P}{A_{dc}}\right) 3.04.$$

Therefore, the power density at the center of the direct converter is about three times the average power density over the direct converter. Without the effect of β_c on B, the peak value would be only 20/9 times the average.

2.9.10 Direct Conversion Efficiency

Table 2.9-1 shows the MARS parameters, and Table 2.9-2 shows the resulting current, heat, and electric power recovered at each electrode. In Table 2.9-3 we itemize the currents and energies of the different species of charge particles leaving the reactor. Notice that these are electrical, not particle, currents. Because of the selective leakage of electrons out one end and ions out the other, the electron collector is a major component in the direct recovery system. The total electrical power recovered is $P = 445 \text{ MW}_e$, and the total heat generated at the electrodes (including the grounded halo dumps) is $H = 418 \text{ MW}_{th}$. Notice that $P + H = P_{chg} = 863 \text{ MW}$, so that all of the charged particle power is accounted for. The net efficiency of direct recovery η_{DC} is therefore

$$\eta_{DC} = \frac{P}{P_{chg}} = 0.515.$$

Note that in the overall reactor power balance (Section 2.10), we used a slightly different definition of η_{DC} , namely, $\eta_{DC} = P/P_{end}$, where P_{end} is less than P_{chg} by the halo power. Most of the 418 MW of heat could conceivably be used in a thermal cycle to generate more electricity.

Figure 2.9-5 shows a schematic electric circuit that makes use of the electric power and passively generates all of the required electrode voltages except V_{g2} . A 100-kV, 380-A dc power supply is required to maintain V_{g2} . The load resistors R_1 and R_2 can be combinations of gyrotrons and neutral beam injectors.

Fig. 2.9-5. Schematic of an electrical circuit that uses the directly recovered power. The load resistances R_1 and R_2 could be gyrotrons or neutral beam injectors.

Table 2.9-1. MARS parameters used to evaluate the direct recovery of power.

P_F	3500 MW
Q	14.2
T_{ic}	35 keV
T_{ec}	28.9 keV
ϕ_c	145.9 kV
ϕ_e	205.0 kV
ϕ_{pc}	24.55 kV
ϕ_b	203.7 kV
P_{s1}	8.3 MW
E_{s1}	475 keV
P_{ECRH}	53.3 MW
$E_{e,b}$	460.8 keV
$E_{\alpha,end}$	972 keV
P_{rad}	12.0 (synch) + 17.2 (brem) = 29.2 MW
P_{cx}	54.1 MW

Table 2.9-2. The voltage, current, heat and electrical power generated at each electrode.

	Halo	Grid 1	Grid 2	Ion collector	Electron collector	Total
$V(kV)$	0	-176	-276	164	-176	--
$I(A)$	1670	-4	378	554	-2598	0
$H(MW_{th})$	80.3	20.4	30.8	170.6	115.8	418.2
$P(MW_e)$	0	0.7	-104.2	90.9	457.5	444.9

Table 2.9-3. The electrical current and kinetic energy of each species of charged particles leaving the reactor.

Region Species	Halo, V=0	Ion end (g1) V = -176 kV	Electron end, V = -176 kV
Central-cell Electrons	0	0	-2660 A 28.9 keV
Barrier Electrons	0 435 keV	-5.7 A 435 keV	-5.7 A
Central-cell D ⁺ , T ⁺	1392 A 52.5 keV	865 A 386 keV	0
Sloshing (anchor) D ⁺	0	8.7 A 982 keV	8.7 A 982 keV
α particles	278 A 52.5 keV	59.6 A 1382 keV	59.6 A 1382 keV

2.9.11 Halo Plasma Physics

Although the radial diffusion of 1600 A and 80 MW of ions from the transition regions produces a thin halo plasma in the ends, this halo does little to protect the core plasma. However, the halo produced by the absorption of the synchrotron radiation from the central cell (~ 12 MW in MARS) can protect the core plasma.

The outside boundary of the core plasma is determined by the radial extent of the plugging potential in the anchors. Immediately outside of the confined plasma, a halo plasma must be maintained to protect the core plasma from recycled gas and impurities coming from the walls. Incoming high-Z impurity atoms must be ionized while they are on field lines that pass outside of the confining potential. If allowed to enter the core plasma, they would be confined better than fuel ions and would accumulate, because to escape over the confining potential ϕ_c requires that they be heated to an energy greater than $Ze\phi_c$. Radial transport in the transition regions would only remove them relatively slowly because of the $(\text{energy})^{3/2}/Z^2$ dependence of the diffusion coefficient (Section 2.4), and because they first would have to become passing ions. We feel, therefore, that the halo plasma must attenuate the incident impurities by at least three orders of magnitude.

A thin halo is created by the radial transport of most of the alphas plus 10 times as many fuel ions out of the transition regions. Once these ions drift out far enough to pass the confining potential, they stream freely out either end. They are in the halo for only one transit time to an end, and transfer only a small part of their energy to the halo electrons. In MARS, about 1600 A and 80 MW are carried out by these ions, and only about 0.3 MW is transferred (by drag) to the electrons. This sheet of ion current is at least two ion-gyroradii thick. The actual thickness depends on the radial profile of the confining potential, but it is clear that the complete halo must be much thicker and more dense than this ion sheet to give the required attenuation.

The transition regions are vulnerable to the trapping of cold ions. Trapped ions are removed by pump beams, which also release gas at the beam dumps and at the walls where the charge-exchange products strike. When these gas molecules enter the halo, fast Franck-Condon (F-C) atoms are produced that penetrate much deeper into the halo than the molecules would. It is the

attenuation of these fast atoms in the transition regions that sets the most severe requirement on the halo density. By flux mapping from the 17-cm-thick halo in the central cell, the thickness of the halo is $w = 13$ cm at the 9-T pump beam in the transition. The current of F-C neutrals that penetrates the halo must be less than 10% of the pump beam current that produces charge exchange. If, on the average, half of the incident molecules results in F-C's, and half of those F-C's are directed toward the core plasma, then the total current of F-C's toward the core is 1/3 of the incident current of gas atoms (after summing over the recycled F-C's). We therefore require that

$$\frac{1}{3} I_p (1-f + f \times 0.7) e^{-w/\lambda} < 0.1 \times I_p \times 0.7 ,$$

where I_p is the pump beam current, $f = 1$ is the trapping fraction, and 0.7 of the trapped beam results in charge exchange. The mean free path for F-C's, λ , is given by

$$\lambda = \frac{v_{FC}}{n_h \langle \sigma v \rangle}$$

and, from the above requirement, must satisfy $\lambda < w/1.2$. Here, $v_{FC} = 2 \times 10^6$ cm/s is the speed of 5-eV F-C's, n_h is the halo density, and $\langle \sigma v \rangle = 3 \times 10^{-8}$ cm³/s is the rate coefficient⁵ for the ionization of D and T atoms by 50-eV electrons. The required halo density is therefore $n_h > 6 \times 10^{12}$ cm⁻³.

The primary loss of power from the electrons is at the halo dumps in the ends, and, if $T_e < 100$ eV, it is limited by conduction. At $T_e = 50$ eV and $n_e = 6 \times 10^{18}$ m⁻³, about 8 MW is conducted out each end by electrons.⁶ Even at 1% concentration of high-Z impurities (such as iron) in this halo, the line radiation is only about 1 MW, less than 2 kW/m² on the first wall. The power required to ionize 4000 A (equivalent) of DT gas (from the pellet injectors and neutral beam injectors) is negligible by comparison. Therefore, the power required to maintain $T_e = 50$ eV in the halo is about 17 MW. The central cell electrons radiate about 12 MW of synchrotron radiation, and most of this comes from the outer layers where the fundamental frequency is the same as in the halo. The absorption coefficient⁷ in the central cell halo ($w = 0.17$ m, $n_h = 6 \times 10^{18}$ m⁻³, $T_e = 50$ eV) is 0.84, so that 10 of the 12 MW is absorbed on the first pass through. Since the first wall is a good reflector, most of the 12 MW is absorbed in the halo. Because of the steep $T_e^{3.5}$ scaling law for

electron thermal conduction, 12 MW absorbed by the halo electrons will maintain their temperature at $T_e = 45$ eV. We therefore assume that the power required to maintain the halo is supplied by the synchrotron radiation from the central cell and does not appear in the overall power balance.

Any complete physics model of the halo will require a knowledge of the boundary region between the core and outer halo. Not only is the radial variation of ϕ_c involved, but also the potentials of the outer regions of the ion collector and the inner regions of the halo dumps where this boundary layer is collected. Also, the electron confining potential ϕ_e depends on the electron temperature, which is different in the boundary region than in the core plasma.

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2.10.1 Plasma Parameters

We used a zero-dimensional, multi-region fluid code to self-consistently determine the parameters of the MARS plasma. The plasma model is an outgrowth of the model developed for MFTF-B analysis,¹ and is essentially the same as the model we used in our comparative end plug study last year.² Since the model has been described previously, we will simply present the results for MARS.

An essential feature of a tandem mirror with thermal barriers is the axial variation of plasma potential which, along with the magnetic configuration, provides the confinement for the central-cell fusion plasma. Calculation of the axial potential profile, consistent with the particle and energy balances, is the cornerstone of the plasma model. Figure 2.9-1 shows the axial potential and magnetic field profiles for MARS. The figure shows the nomenclature used in the plasma model for the various potential differences. Table 2.10-1 gives the z-position, vacuum magnetic field strength, and plasma potential at each of the cardinal points in MARS. The magnetic field values in the table and figure are those that were used in the plasma model, and, as mentioned previously, are not exactly the same as those calculated for the MARS magnetic configuration (plotted in Fig. 2.1-5). We bias the potential to near zero in the central cell to satisfy the constraints imposed by central-cell plasma rotation (see Section 2.2.4) and radial transport (see Section 2.2.1). The on-axis central-cell plasma potential is 29 keV above ground and the potential at the minimum field point in the transition region is 72 keV below ground. The β and density values are peak, on-axis values; the plasma model assumes a cubic radial density profile. The ion temperature T_i is a code input; 35 keV has been determined to be near-optimum for maximizing the plasma performance parameter Q . The electron temperature T_e is a calculated value. The hot plasma radius, where the hot fuel density goes to zero, defines the inner boundary of the halo plasma, and the first wall location allows for a halo thickness of 3 hot alpha (3.5 MeV) gyroradii. The cold fueling current is that portion of the DT fueling of the central cell that is to be provided by pellet injection near one end of the central cell. Most of the deuterium fuel is provided by the charge-exchange pump beam, and ionized

Table 2.10-1. MARS magnetic field strength and plasma potential.

Location	z(m)	B(T)	ϕ (keV)
Central cell	- 75 to 75	4.7	29 ($\phi_e = 205$)
Inboard axicell mirror	80.2	21	29
Axicell midplane	81.2	17.3	54 ($\phi_{pc} = 25$)
Outboard axicell mirror	82.2	24	10 ($\phi_{pb} = 44$)
Pump beam location	84.5	7.8	-13
Transition field minimum	90.5	1.6	-72 ($\phi_t = 126$)
Inboard anchor mirror	94.0	6.0	--
Point A'	95.0	5.4	-21 ($\phi_{A'} = 75$)
Anchor midplane (barrier)	97.5	2.3	-150 ($\phi_b = 204$)
Point A (potential peak)	99.5	3.5	175 ($\phi_c = 146$)
Outboard anchor mirror	101.0	6.0	0

Table 2.10-2. MARS central cell parameters.

P_{fusion}	3500 MW
B	4.7 T
β	0.7
$n_{O,DT}$	$4.2 \times 10^{14} \text{ cm}^{-3}$
$n_{O,\alpha}$	$4.2 \times 10^{13} \text{ cm}^{-3}$
$n\tau_{\text{ion}}$	$5.5 \times 10^{14} \text{ s/cm}^{-3}$
$n\tau_e$	$6.6 \times 10^{14} \text{ s/cm}^{-3}$
$n\tau_\alpha$	$1.0 \times 10^{14} \text{ s/cm}^{-3}$
T_i	35 keV
T_e	29 keV
L_c	150 m
Hot plasma radius	0.43 m
First wall radius	0.60 m
Neutron wall loading (Γ_{FW})	5.0 MW/m^2
Cold fueling current	1.9 kA

pump beam neutrals are in central-cell passing ion velocity space. The parameters of the central cell plasma, confined by the potential peak $\phi_c = 146$ keV, are given in Table 2.10-2. Table 2.10-3 lists the various plasma density and energy parameters of the MARS and end plug parameters.

2.10.2 Particle and Energy Injection

Table 2.10-4 gives the physics parameters of the neutral beam and rf heating systems for MARS.

The charge-exchange pump beams inject 740 A of 97-keV deuterium into the transition region at each end. The pump beam aiming point is $z = 84.5$ m, where the magnetic field strength is 7.8 T. The angle of injection is 25 deg from the axis. More than 99% of this beam is ionized in the plasma (there are five e-folds of beam attenuation through the plasma). Beam neutrals that are ionized by charge-exchange reactions with transition-trapped ions perform the desired pumping function--the new neutral is lost to the wall while the new ion is in passing, not trapped, velocity space. Ionization reactions by beam neutrals contribute to central-cell fueling, but not to pumping. Charge-exchange reactions with passing ions contribute to neither pumping nor fueling.

The anchor beams inject 20 A of 475-keV deuterium into each anchor to sustain the sloshing ion distribution. The injection point is chosen to be point A' ($z = 95$ m) instead of point A ($z = 99.5$, the potential peak location) to avoid the introduction of cold electrons at the location of the warm electron population. Points A and A' are the turning points for the sloshing ions. Because of the high beam energy and modest anchor plasma density, only 45% of the anchor beam is trapped in the plasma.

The axicell mirror-confined plasma is sustained by ICRH. This represents a recent change in the MARS baseline design and only limited analysis has been performed. The earlier MARS design called for 200-keV neutral beam injection into the axicell. This feature necessitated an axial gap in the superconducting axicell magnet, which lowered the magnetic efficiency (on-axis field/conductor field) and introduced difficult neutron shielding and magnet structural support problems. These problems are avoided with ICRH power feed-throughs that enter the axicell region from one end of the superconducting

Table 2.10-3. MARS end plug parameters.

Axicell

Midplane plasma radius	0.21 m
Plasma length	2.0 m
β	0.4
Hot ion density	$9.5 \times 10^{14} \text{ cm}^{-3}$
Total ion density	$1.16 \times 10^{15} \text{ cm}^{-3}$
Average confined-ion energy	300 keV
$n\tau$	$8.2 \times 10^{13} \text{ s/cm}^3$

Transition region

Length	12 m
β	0.15
Passing ion density at B minimum	$5.2 \times 10^{12} \text{ cm}^{-3}$
Passing ion density at pump point	$3.5 \times 10^{13} \text{ cm}^{-3}$
DT particle trapping current	295 A at each end

Anchor

Length	7 m
β	0.65
Passing ion density at barrier	$3.9 \times 10^{12} \text{ cm}^{-3}$
Warm electron temperature at potential peak	89 keV
Hot electron energy at barrier	460 keV

Table 2.10-4. MARS neutral beams and rf.^a

	Species	Energy (keV)	Injected current/end (A)	Injected power/end (MW)	F _{trap}	Frequency
<u>Neutral</u>						
<u>Beams</u>						
Pump	D	97	740	72	0.993	-
Anchor	D	475	20	9.3	0.446	-
<u>ICRH</u>						
Axicell	-	-	-	23	0.85	180 MHz
<u>ECRH</u>						
Barrier	-	-	-	27	1.0	64 GHz
Potential						
peak	-	-	-	1	1.0	96 GHz

^a Total trapped injected power = 246 MW

Q = 3500/246 = 14.2

magnet. Our preliminary choice for the ICRH frequency, 180 MHz, is the second harmonic for tritium at the axicell minimum field. The injected power, 23 MW per end, is assumed to be 85% absorbed in the mirror-confined tritium species.

ECRH is required to sustain the hot mirror-confined electrons at the barrier location and the warm electron population at the location of the potential peak (point A). The frequencies listed in Table 2.10-4 are the fundamentals at the vacuum field values of 2.3 and 3.5 T, respectively. Finite β and relativistic effects will tend to lower the required frequencies. We estimate 100% absorption of this power.

Fueling of the central-cell plasma requires the pellet injection of 1.9 kA of mostly tritium (deuterium fueling is provided by the charge-exchange pump beam). Injection is at the 9.4-T point at one end of the central cell, where the reduced plasma radius improves pellet penetration. For injection of 10% of the central-cell plasma ion population per pellet, the fuel pellets are about 4 mm in radius. We used Park and Turnbull's³ pellet ablation model (with density profile corrections) to calculate required pellet velocities. For penetration to the plasma axis we would require a pellet velocity of about 43 km/s. If radial diffusion of injected fuel will permit penetration to only 1/5 the plasma radius, as suggested by some Tokamak studies⁴, then the pellet velocity need be only 1.2 km/s. The required pellet velocity for MARS has not yet been determined, but it appears that it will be in the range from 1 to 10 km/s.

2.10.3 Overall Reactor Power Balance

Figure 2.10-1 gives an overall MARS power balance consistent with the plasma parameters discussed in this section and the baseline systems described in other sections. Two numbers are presented for terms affected by the blanket choice: the first refers to the LiPb blanket, the second refers to the high-temperature blanket. The injected power and trapping fractions for the neutral beam and rf systems are from Table 2.10-4; the electrical efficiencies of these systems have been separately estimated. The thermal conversion efficiencies have been adjusted to include coolant pumping power. The copper coil power is for the baseline-design axicell insert coils with internal conductor cooling. The auxiliary system power is primarily for cryogenic refrigeration. The end-loss charged-particle power is reduced by 80 MW of power to the halo plasma and 57 MW of charge-exchange power to the wall in the transition region.

Fig. 2.10-1. Overall power balance.

The net electrical output of MARS is 1357 or 1273 MW, depending on blanket choice, and the 590 MW of recirculated electrical power represents 30 to 32% of the gross electrical power. Note that the baseline design does not include thermal conversion of the halo and direct converter heat. Future work will investigate the recovery of this power. If 35% conversion of the halo and direct converter heat can be realized, the net electrical output will increase by 165 MW and the recirculating power fraction will decrease to about 0.28 to 0.29. The other high-leverage area for future improvement of the MARS power balance is in the design of the trapped-particle pump system. Note that the charge-exchange pump beams consume 50% of the total neutral beam and rf input power, which in turn makes up 70% of the total recirculated electrical power. Our future work in this area will investigate radial drift pumping, as discussed in Section 2.3.3.

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3. REACTOR SYSTEMS

3.1 MAGNETS

The MARS magnet system includes four principal magnet types. The central-cell magnets are located in the central region of the reactor and generate the axial magnetic field to confine the plasma. An axicell is located at each end of the central cell and is followed by transition coils that shape the plasma and anchor coils to provide plasma stability. Figure 3.1-1 shows the magnet configuration and summarizes major characteristics of each magnet system. For simplicity, only a portion of the central cell with the end cell of the machine is shown.

The central cell incorporates 48 superconducting solenoids to produce an axial field of 4.7 T. Magnet design is based on a reasonable extrapolation of state-of-the-art superconductor technology; the coil can be manufactured by using fabrication methods similar to those used to construct MFTF-B solenoids.

Each axicell magnet group consists of a superconducting solenoid that provides a background magnetic field of approximately 15 T and two resistive insert coils that increase the magnetic field to 21 and 24 T, respectively. The design of this system is based on advanced magnet concepts requiring further technological development.

The transition and anchor coils are similar to the transition and anchor coil of MFTF-B. The overall dimensions, magnetic field, and location of these coils may present a helium cooling problem. The yin-yang for MFTF-B was designed for a 45-deg-from-horizontal orientation to accommodate bubble ventilation. Additional scoping and design work is scheduled in FY 1983 to complete the MARS design.

The design work performed in FY 1982 concentrated on resolving key design issues of the central cell and axicell magnets and scoping alternatives to identify the best design approaches. We have proposed a number of innovative solutions to some challenging design problems, including:

- An integrated central-cell coil-blanket design using coil-blanket modules to facilitate machine assembly and maintenance.

Coil	Quantity	Mean diameter (m)	Central field	Peak field	Weight (t)
Central cell	48	5.8	4.7	7.4	180
Axicell	2	3.8	21, 17.3, 24	16 ^b	616
Transition	4	TBD	1.6	6.8	TBD
Anchor	2YY ^a	8	6, 2.3, 6	8.5	315

^aYin-yang pair

^bSuperconductor

Fig. 3.1-1. MARS magnet configuration.

- High-performance, internally cooled resistive-insert magnets to provide more than 1 full power year (FPY) magnet lifetime in the axicells.
- High-field superconducting coils using graded regions of NbTi, Nb₃Sn, and titanium-doped Nb₃Sn superconductors cooled with LHe II at 1.8 K to achieve higher fields with smaller coils.
- Reinforced conductors for application at the higher fields and stress levels, to reduce coil volume and cost.
- Radiation-resistant polyimide and spinel insulators to increase radiation tolerance of superconducting and resistive magnets.

3.1.1 Central Cell Coils

3.1.1.1 Design Considerations

The central-cell magnet requirements are shown in Table 3.1-1. Major requirements are an axial magnetic field of 4.7 T, an inner radius of the solenoid cryostat of 2.24 m, and an intercoil axial spacing of 3.16 m.

Table 3.1-1. Requirements for the central cell solenoids.

Central field	4.7 T
Peak conductor field	< 8.0 T
Operational life	24 FPY, 30 yr total
Cryostat clear bore radius	<u>< 2.24 m</u>
Axial spacing	3.16 m
LHe and vacuum vessel design	ASME pressure vessel code

Based on a design alternative study, a NbTi/Cu conductor cooled with LHe I at 4.5 K was selected as the baseline design. Existing superconductor technology with demonstrated performance in large coils is based on this combination.

Magnetic Field and Forces. The basic magnetic field was calculated with LLNL's EFFI computer code. Peak fields are within NbTi/Cu, pool boiling LHe at 4.5 K technology. The hoop stresses in the coil were evaluated with the STANSOL computer code. Large hoop forces led to incorporation of steel-reinforced conductors.

Radiation Effects. Radiation affects the magnets in several ways. It increases the resistivity of the stabilizer, decreases the mechanical and dielectric strengths of the insulators, and reduces the critical parameters of the superconductor. In addition, the neutron heat input in the magnets increases the load of the refrigeration system. These effects reduce cryo-stability, and decrease the peak magnetic field and lifetime of the magnets. The central-cell magnets, therefore, are provided with adequate shielding to keep radiation damage within acceptable limits for full performance of the magnets over their design life of 24 FPY.

Radiation damage to the stabilizer increases electrical resistivity, approaches a saturation limit, and is mostly recoverable by room-temperature annealing. Similarly, damage to the superconductor decreases the critical current and is recoverable. On the other hand, the radiation damage to the insulation is cumulative and non-recoverable. Detailed radiation limits for the central cell magnets are discussed later.

3.1.1.2 Engineering Trace-off Studies

Choice of Conductor and Insulation. Several types of conductors have been considered for application to the central cell magnets. A steel-reinforced monolithic conductor was chosen to resist the high hoop stresses. Detailed conductor manufacturing techniques and costs were investigated and compared to an alternate design conductor supported with separate stainless steel strips. The alternate was rejected based on cost.

Polyimide glass and epoxy glass have been considered for the insulating material. Polyimide was chosen for the insulation in high radiation areas since it tolerates high radiation doses (5×10^9 rad). Epoxy glass will be considered if a detailed shielding study shows that adequate margin is maintained for the 24-FPY lifetime.

Choice of Cooling. A cost comparison was made between cooling a central-cell coil with LHe I or with LHe II. The comparison included the cost of the magnet, the cost of the refrigeration system, and the present value of the operating costs. A magnet cooled with LHe II uses less conductor, but requires greater refrigeration capacity and operating costs. Table 3.1-2 shows that the two costs are almost identical. The use of LHe II would require considerable technological development of coil and cryogenic equipment. Also, the refrigeration system would need more space and the service stack and cryogenic plumbing would be more complex with LHe II. Because the design requirements of the central solenoids are met with proven LHe I technology, pool-boiling helium at 4.5 K is the preferred coolant. A cost comparison was also made for a central solenoid with a 2.06-m inner radius; the same conclusion was reached.

3.1.1.3 Magnet Design

The central-cell solenoid magnets provide the central magnetic field necessary to contain the plasma. There are 48 coils, spaced 3.16 m along the z-axis, that produce a central field of 4.7 T with a ripple of 3.8%. The magnet design is illustrated in Fig. 3.1-2; key features are given in Table 3.1-3. Each solenoid is a complete magnet including case, radiation shield, vacuum vessel, and service stack.

The NbTi conductor is graded to take advantage of the reduction in field and allow higher current densities. Three graded sections are used in the radial direction, with the same operating current of 7535 A flowing in series through each section of graded conductor.

The solenoid case is made of heavy sections of 304LN stainless steel. The insulation is polyimide fiberglass in high radiation areas. The conductors are layer-wound to take advantage of grading. A single service stack at the top provides for the vapor-cooled power leads, LHe, LN₂, gaseous exits,

Dimensions in centimeters
Not drawn to scale

Fig. 3.1-2. Central-cell solenoid cross section.

Table 3.1-2. Magnet cooling alternatives.

Costs	LHe I (pool boiling)	LHe II (superfluid)
Cost of coil including case at \$25/lb avg cost for first reactor	\$11.58 M	\$ 9.06 M
Cost of LHe thermal radiation shield at \$140/ft ²	N/A	0.19 M
Cost of cryogenic plant		
LHe I at \$1750/W	1.13 M	0.44 M
LHe II at \$4550/W	<u>N/A</u>	<u>1.44 M</u>
Comparative capital cost	<u>\$12.71 M</u>	<u>\$11.13 M</u>
Annual cost to operated based on 80% usage, \$0.10/kW-hr	\$0.304 M	\$0.505 M
Present value of 30-yr operation at 12% interest	2.46 M	4.06 M
Total lifecycle cost ^a (capital plus present value of operating cost)	\$15.17 M	\$15.19 M

^aCost estimates do not include stack, Li₂ shield, vacuum vessel, cryogenic plumbing, power and control system, or maintenance.

Table 3.1-3. Key features of the central cell solenoids.

LHe I-cooled (pool boiling, 4.5 K)

Coil spacing 3.16 m

Graded current density (three sections)

Peak field 7.4 T

6-cm side insulation for splices and leads

Conductors react own hoop stresses (40 ksi max. allowable)

Heat loads

- LHe < 757 W
- LN₂ < 9753 W
- Vapor-cooled leads--28 liters LHe/hr

Neutronic shielding requirements

- 1.1×10^{-4} dpa in copper stabilizer before annealing
- 0.06 mW/cm³ peak neutron heat deposition in winding
- 5×10^9 rad dose max in insulation

Approximate weight (including vacuum vessel) 200 tons

and instrumentation. An LN_2 -cooled radiation shield is used between the magnet and vacuum vessel. The inner radius of the vacuum vessel is 2.24 m, the width of the vessel is 1.37 m, and the outer radius is 3.94 m.

Conductor and Insulation. Figure 3.1-3 shows the design of the layer-wound conductor. The superconductor may be either cable or monolithic. The inner two regions of the coil have an OFHC copper-stabilized NbTi superconductor reinforced with a stainless steel core to sustain the larger hoop forces occurring in the high field regions. The outer, low-field region of the coil uses a NbTi superconductor stabilized with OFHC copper without the steel core. The peak stress is 40 ksi in regions I and II and 28 ksi in region III. Reinforcing the superconductor with a high-strength core material is a new concept presently being studied.

The insulation is made of polyimide fiberglass, a relatively radiation-resistant insulator. Turn-to-turn insulation uses the "button-on-a-string" concept; snow-fence perforated sheets are used for the layer-to-layer insulation. Conductor-to-ground insulation consists of multi-thicknesses of polyimide fiberglass sized according to location.

Conductor Cooling. The conductor is cooled with pool boiling LHe I at 4.5 K and 1 atm. The design heat flux is 0.25 W/cm^2 . This value is based on test data obtained for pool boiling heat transfer in liquid helium within simulated conductor packs. Proper interconductor spacing and insulation geometries yield minimum film boiling fluxes of 0.25 W/cm^2 . Since the design flux is the minimum film boiling flux, the central-cell conductors are designed to be unconditionally cryostable and do not rely on the collapse of a normal zone due to end conduction for recovery. The conductor recovery current analysis is presented later.

3.1.1.4 Engineering Analysis

Magnetics. One of the primary goals of the design was to minimize magnet size consistent with the central field, ripple, and the inner radius requirements. We optimized the design using graded current densities to take advantage of the reduction in field with increasing radius. Peak fields for configurations did not exceed the 8 T allowed for a NbTi superconductor, indicating that pool-boiling LHe I was a satisfactory coolant.

Fig. 3.1-3. Central-cell magnet conductor design.

Stress calculations showed that for reasonable current densities, the stress level of the inner layers exceeded the capability of a copper-stabilized conductor. Therefore, we considered a steel-reinforced LHe I-cooled conductor. We designed cryostable, steel-reinforced conductors that withstand a 276-MPa (40-ksi) hoop stress as compared with a 193-MPa (28 ksi) allowable stress for a copper-stabilized conductor. The use of high-stress conductors that react their own hoop stresses allowed us to minimize the size of the magnets.

We graded the current density by using cryostability curves giving allowable current density vs field strength for the conductors under consideration (discussed later). Because field strength decreases with larger radius, the allowable current density rises. The current density was increased until the maximum allowable stress level was reached.

Subsequent EFFI runs using optimized coil geometry verified that we met the central fields without exceeding the peak field of 8 T. The latest configuration is shown in Fig. 3.1-2. The key features of the winding are the three sections of graded current density, the use of LHe I for the coolant, the self-supporting conductors, and the use of a steel-reinforced conductor in the first two grades.

Force and Stress Analysis. Initial analyses of conductor stresses were made assuming self-supporting conductors. After a coil had been sized on this basis, several STANSOL-II computer code¹ runs were made using varying amounts of substructure in the winding. The STANSOL program takes into account the amount of insulation and gaps; it showed that the conductor stresses matched closely those calculated for free-standing conductors. We concluded that for initial sizing of these magnets, the use of a self-supporting conductor is justifiable and no substructure is required.

In addition to radial forces acting on a conductor, during normal operation there are axial forces that act on the magnets at the ends of the central cell. Under certain fault conditions, some large additional axial forces are also generated. The magnitude of these forces depends on the fault condition; the number of possible fault conditions is large. The EFFI and MAGF codes can quickly calculate these forces for any given fault condition. A worst-case condition is one where all end cell magnets are de-energized, which causes a force of 11.5×10^7 N (25.9×10^6 lb). Fault analysis is also

linked to the powering of the 48 solenoids, but this aspect of the study has not yet been addressed. In the meantime, the support for the magnets is based on the worst-case load given above.

Conductor Cryostability. To support the central-cell conductor design, cryostability analyses were performed to determine the maximum cryostable current density as a function of field strength for several candidate conductors. To facilitate the field calculations, each stability analysis was done in terms of overall current density, packing factor, and conductor unit cell size. The conductor design determines the unit cell area and packing factor. The turn-to-turn and layer-to-layer insulation covers 50% of the conductor surface; this then determines the wetted parameter. The copper-to-noncopper ratio is approximately 10 to 15 for the unreinforced conductors, and approximately one for the steel-reinforced conductors.

The magnetoresistivity of the copper stabilizer is computed from

$$\frac{\rho_D(B) - \rho_D(0)}{\rho_D(0)} = 4.37 \left[\frac{B}{\rho_D(0) \times 10^9} \right]^{1.115}, \quad (3.1-1)$$

where $\rho_D(B)$ = degraded resistivity at field B (ohm-cm),
 $\rho_D(0)$ = degraded resistivity at zero field (ohm-cm), and
 B = field strength (T).

Equation (3.1-1) is similar to an equation presented by Kohler² and gives the magnetoresistance of copper damaged by neutron radiation.³ For half-hard copper--the material selected for the central-cell solenoid stabilizer--the resistivity at 4.5 K is 1.9×10^{-8} ohm-cm.

The design heat flux q is limited by the conductor's ability to transfer heat to LHe I by pool boiling. For an unconditional stability criteria, the design flux is the minimum film boiling, or recovery flux. We conducted a series of tests to measure heat transfer to LHe I from conductors arranged in bundles (to simulate actual winding packs). Through these tests we established correlations between the minimum film boiling flux and the average gap dimension between conductors.⁴ Using these data, we chose a design flux of 0.25 W/cm^2 on the conductor-wetted surface.

The magnetoresistivity of radiation-damaged copper was determined for several typical conductors, including both conventional copper and steel-reinforced stabilizer designs, so that we could obtain plots of current density vs field. These plots were used for the first sizing of the coils; an iteration was then performed. Figure 3.1-4 shows the final data for the baseline central-cell conductors. Also indicated on the curves are the actual operating points for the three grades of conductor. The fact that the points fall on the allowable curves illustrates that the current densities are limited by cryostability.

Radiation Limits. The magnet components most affected by radiation are the stabilizer, insulation, and superconductor. We set the following radiation limits and guidelines to ensure adequate service performance over a 24-FPY lifetime:

1. 5×10^9 rad for polyimide insulation over 24 FPY.
2. < 10% decrease in critical current density of the NbTi superconductor over 24 FPY. This corresponds to approximately 4×10^{18} n/cm², $E_n > 0.1$ MeV, neutron fluence for 24 FPY.
3. 1.1×10^{-4} dpa in the copper stabilizer before annealing.
4. 0.06 mW/cm³ peak nuclear heat deposition in winding.

Our rationale for setting these limits was because the polyimide-glass insulation retains more than 80% of its pre-irradiation compression strength after a dose of 5×10^9 rad. This dose is about an order of magnitude higher than that for epoxy glass insulation for equivalent strength loss. The polyimide glass insulation costs about four to six times more than epoxy glass. Epoxy glass insulators are used in low-dose areas of the magnet.

The radiation limit for a NbTi superconductor is within a factor of two of that for polyimide-glass insulation. Neutronic calculations for determining the absorbed dose in the insulation took into account contributions from both gammas and neutrons (which are roughly equal in magnitude). The gammas have little effect on the critical properties of a NbTi superconductor.

For our magnet cryostability analysis we allowed a 20% increase, due to irradiation, in electrical resistivity of the copper stabilizer. The increase for half-hard OFHC copper at 8 T amounts to approximately 13×10^{-9} ohm-cm at

Fig. 3.1-4. Maximum cryostable current density for central cell solenoids.

4.5 K. The increase in electrical resistivity $\Delta\rho_{irr}$ of OFHC copper at 4 K for a fast-fission neutron spectrum has been experimentally determined⁵ as

$$\Delta\rho_{irr} = 400 (1 - e^{-162D}) \times 10^{-9} \text{ ohm-cm},$$

where D = radiation damage in dpa. For D = 1.1×10^{-4} dpa, this yields $\Delta\rho_{irr} = 7.06 \times 10^{-9}$ ohm-cm. In a subsequent room temperature anneal, about 80 to 85% of this radiation-induced resistivity anneals out, leaving about 1×10^{-9} ohm-cm as residual resistivity in the stabilizer. Thus, the allowed increase in electrical resistivity of 13×10^{-9} ohm-cm can be met by having the first anneal after more than 5 FPY and always having more than 1 FPY between anneals.

The rationale for arriving at 0.06 mW/cm³ peak nuclear heat deposition in the winding is analogous to that discussed in Section 3.1.2.2. This heat deposition is low enough to satisfy all radiation limits of the materials and also minimizes the total cost of magnet refrigeration equipment.

3.1.2 Axicell Superconducting Magnet

3.1.2.1 Design Considerations

Table 3.1-4 lists the basic requirements for the superconducting background coil of the axicell. The 16-T peak field has dictated the use of Nb₃Sn(Ti) as a superconductor cooled with LHe II as well as the use of steel-reinforced conductors.

The magnetic field produced by the superconducting coil should be as high as possible to limit the power dissipation from the resistive insert coils. This has led to the design of a superconducting coil employing advanced concepts. We have chosen a peak field limit of 16 T in the superconducting coil because this appears to be the limit attainable using Nb₃Sn(Ti) and LHe II. The baseline design with the above assumptions gives a central field slightly above 15 T. This is sufficient to reduce the power dissipation of the four resistive coils to 125 MW.

The large magnetic field in the windings interacting with the conductor current creates large hoop loads so that reinforced conductors are necessary. Additional large magnetic loads are the axial compressive load and the

Table 3.1-4. Requirements for axicell superconducting background magnet.

Peak field	< 16.0 T
Operating life	24 FPY (30 yr total)
Cryostat clear bore radius	< 1.12 m
LHe II case and vacuum vessel	ASME pressure vessel code

axial net force on the coil. The effect of these loads on the detailed stress distribution in the conductor pack and on the casing sizing will be addressed in the future.

3.1.2.2 Engineering Trade-Off Studies

Coil Alternatives. Several coil configurations were considered for the axicell. Originally, the magnets were grouped to form a single barrier consisting of nested superconducting coils and a resistive insert. Trade-off studies of 20-, 24-, and 28-T coils with various amounts of shielding showed that a peak field of 24 T is an optimum design point based on plasma Q , resistive coil power, and coil cost.

As the plasma physics requirements were updated, the magnet configuration also changed, with the barrier coil evolving into an axicell with two sets hybrid of coils rather than a single one. The most recent change in the axicell resulted from combining the two superconducting coils while still retaining the two resistive inserts as shown in Fig. 3.1-1. This change was made because forces between the two superconducting coils required a large inter-coil support and shield structures that did not leave enough space to accommodate neutral beam injection between the coils.

Choice of Shield Thickness. We conducted an optimization study to compare shield and magnet costs with cryogenic system costs for the axicell. Our results are presented in Fig. 3.1-5. The cost of the shield is small, but

Fig. 3.1-5. Trade analysis of the axicell superconductor shield.

the magnet cost increases with increasing shield thickness because of the larger size and higher stresses necessary to achieve the required central field. The cryogenic system cost decreases with increasing shield thickness because the heat load decreases. The minimum cost is based on an iron shield approximately 90 cm thick. As shown in the figure, the 90-cm shield keeps the magnet within all radiation limits. Similar conclusions are reached for a higher-cost tungsten shield.

Choice of Cooling. Initially, we considered both LHe I and LHe II as the coolant for the superconducting axicell background coil. LHe I was considered because of proven cryogenic technology and LHe II because it allows operation with higher current densities to produce higher fields. Preliminary estimates of maximum design heat fluxes were 0.25 W/cm^2 for LHe I and 2.0 W/cm^2 for LHe II. Because the maximum cryostable current density is proportional to the square root of heat flux, LHe II allows a current density 2.8 times higher than that of LHe I. However, in later estimates, the LHe II heat flux was reduced to 1.0 W/cm^2 . Therefore, LHe II current density is about twice that of LHe I. LHe II requires a much smaller and less expensive magnet than LHe I. This benefit outweighs the cryogenic plant capital and operating cost penalties of LHe II over LHe I. For these reasons, we chose LHe II as the coolant for the superconducting axicell.

3.1.2.3 Magnet Design

Each axicell provides two high-field points along the z-axis. The field strengths are 21 T at $z = \pm 80.2 \text{ m}$ and 24 T at $z = \pm 82.2 \text{ m}$. These field strengths are obtained by using a superconducting magnet that encircles two smaller resistive magnets. Figure 3.1-6 shows the layout of the system. To minimize the ohmic power loss in the resistive coils, the encircling superconducting magnet is designed for the highest achievable peak fields. As we have already stated, we have chosen a 16-T limit because it is the practical upper bound for Nb_3Sn superconductor doped with Ti. Because of the high peak field, 16 T, and the high conductor hoop stress of 276 MPa (40 ksi), we are using a steel-reinforced conductor and LHe II for cooling. A LHe I-cooled coil using a copper-stabilized conductor would require a much larger build.

As in the central cells, the same current will flow in the layer-wound conductors; they are graded to provide varying current densities. The first

Fig. 3.1-6. Axicell magnet layout.

section uses Nb_3Sn doped with Ti for the superconductor. The stabilizer is copper used in conjunction with a 304LN stainless steel reinforcing substrate. The second section uses the same stabilizer and reinforcement, but the superconductor is Nb_3Sn without the Ti doping. This section starts where the field is 12 T. The third section starts where the field is 9 T. The superconductor is NbTi and the steel-reinforced conductor is reduced in size to increase the allowable stress level to 276 MPa. The fourth section again uses NbTi as the superconductor; the field is just under 6 T and the stabilizer is again graded to give a stress approaching 276 MPa.

The coil is encased in a LHe II vessel fabricated from 304LN stainless steel which is, in turn, surrounded by a LHe I-cooled radiation shield and an outer LN_2 -cooled radiation shield. Each superconducting magnet has a vacuum vessel. Polyimide insulation is used because of its resistance to radiation degradation. Each superconducting coil has a service stack for all cryogenics, current leads, instrumentation, and exiting gases.

Conductor and Insulation. Figure 3.1-7 shows the basic conductor concept for the axicell superconducting magnet. The conductor carries 7848 A and has an OFHC copper stabilizer. Transverse and longitudinal grooves increase the cooling area of the stabilizer so that critical heat flux does not exceed 1 W/cm^2 . The conductor is supported by a stainless steel backing strip with a rib fitting in the stabilizer. The effective cross-sectional area of the conductor varies from one graded winding zone to the next to accommodate the current densities shown in Fig. 3.1-6. Figure 3.1-8 shows an alternate conductor concept that incorporates a high-strength core for reacting the electromagnetic hoop forces. This conductor can achieve higher current densities than the baseline, but requires more development.

The insulation is polyimide fiberglass similar to that used in the central-cell solenoids. Turn-to-turn insulation consists of "buttons on a string" placed at regular intervals along the conductor. Snow-fence perforated polyimide sheets are used for the layer-to-layer insulation. Conductor-to-ground insulation is also polyimide fiberglass that is sized according to location.

Conductor Cooling. LHe II is unique in that heat is transferred via a complex heat conduction mechanism, and not by the normal convection mode of typical fluids. Numerous experimental studies show that the maximum heat flux

Fig. 3.1-7. Baseline design of the axicell magnet conductor.

Fig. 3.1-8. Alternate design for the axicell magnet conductor.

in a channel of LHe II is inversely proportional to the cube root of the channel length. Hoard⁶ has shown that eight channels are available to cool a normal conductor within a winding. Each channel has an equivalent length equal to 1.5 times the conductor unit cell dimension in the parallel direction. This theory gives an expression for maximum heat flux on the conductor as a function of conductor size, insulation thickness, and insulation coverage. We can equate the maximum heat flux to the joule heat flux generated by a normal conductor, such that

$$J^2 \rho A \left(\frac{1 + \phi}{\lambda \phi} \right) = 29.6 \left[\left(\frac{B}{L_N^{1/3}} \right) + \left(\frac{C}{L_V^{1/3}} \right) \right], \quad (3.1-2)$$

where

- J = current density over unit cell (A/cm²),
- ρ = stabilizer electrical resistivity (ohm-cm),
- A = area of unit cell (cm²),
- ϕ = copper/non-copper ratio in conductor,
- λ = packing factor (conductor area/unit cell area),
- C = cross sectional area of radial channels between conductors,
- B = cross sectional area of axial channels between layers,
- L_N = 1.5 times unit cell axial dimension (cm), and
- L_V = 1.5 times unit cell radial dimension (cm).

Equation (3.1-2) is used to perform cryostability analyses and conductor sizing iterations to optimize the conductors.

We initially chose 2.0 W/cm² as the maximum heat flux at the surface of the conductor. Subsequent conductor and pack sizing iterations showed that 1.0 W/cm² was a more reasonable and practical level; this value ensured cryostability at the design current densities of each grade of conductor.

3.1.2.4 Engineering Analysis

The engineering analysis of the axicell superconducting coil includes magnetics, forces, stresses, cryostability, and radiation environment considerations. The methods of analysis are essentially the same as those used for the central-cell magnets described in Section 3.1.1.4.

The cryostability analyses of the conductors, which allow us to derive curves of J vs B , differ from the analyses of the central-cell conductors because Eq. (3.1-2) is used to calculate the maximum heat flux. The new curves are presented in Fig. 3.1-9. The actual operating points fall below the curve because the conductors are stress-limited.

The radiation limits for the axicell differ from those of the central cell in two respects. The radiation limit, before annealing at a 20% increase in resistivity in the stabilizer at 16 T, is increased to 1.9×10^{-4} dpa because copper has a higher magnetoresistance at this field. The radiation limit for the superconductor is decreased to 2×10^{18} n/cm² because Nb₃Sn is more radiation-sensitive than NbTi.

3.1.2.5 Axicell Shield

The radiation source in the axicell region of the reactor is high enough so that shielding of the superconducting coil must be provided to meet the design radiation limits. We concluded a one-dimensional analysis of three paths: (1) through the inboard side of the superconductor where there is no resistive insert; (2) through the 21-T (M1) insert; and (3) through the 24-T (M2) insert.

We conducted a study of alternate materials and configurations and selected an option for path 1 with a layer of tungsten 59 cm thick covered by an iron shield (90% Fe-1422, 10% H₂O) 14 cm thick, and a layer of boron carbide 2 cm thick. For paths 2 and 3, the thicknesses of the regions were reduced proportionately with the shield thickness. The material compositions were unchanged. The results of the calculations for these three sections are given in Table 3.1-5. The design limit for the spinel insulation in the resistive insert is presently 3% volumetric swelling. This corresponds to about 1.5 FPY life to replacement. If 5% volume swelling can be tolerated, the life of the insert coils would be 2 FPY from this effect. However, the increase in resistivity from transmutations dominates the lifetime and is discussed in Section 3.1.3. The design limits for the superconducting coil are all met except for peak heating. When the heating rates are averaged over the volume of the coil, the cost of cooling is acceptable.

Fig. 3.1-9. Axicell cryostability limit.

Table 3.1-5. Axicell radiation shielding.

Coil path	Wall loading (MW/m ²)	Wall radius/shield thickness (cm)	Spinel peak fluence $E_n > 0.1$ MeV n/cm^2 FPY	Polyimide peak dose (rad/24 FPY)	Peak nuclear heating (mW/cm ³)	Peak Cu damage (dpa/FPY)	S/C peak fluence $E > 0.2$ MeV n/cm^2
1	3.6	34.5/75 shield	--	2.56+9	0.0746	3.76-5	9.70+17
2	4.8	27.5/23 Cu 51 shield	3.28+22 ^a	1.76+9	0.0635	2.18-5	7.14+17
3	4.8	25.8/49.3 Cu	3.49+22	2.40+9	0.0387	2.46-5	8.00+17
Design limits			4.13+22 for 3 v/o swelling	5.0+9	0.06	1.90-4 after 5 FPY	3.0+18

^a Read as 3.28×10^{22} .

3.1.2.6 Conductor Development for Axicell Superconducting Magnets

The design of the axicell superconducting magnet is based on advanced superconductor concepts. Major developments still needed include:

- Fabrication and verification testing of conductors using titanium-doped Nb₃Sn for application at the higher magnetic field levels.
- Verification of cryostability of the LHe II-cooled advanced conductors.
- Development of detail designs and fabrication techniques for steel-reinforced conductors to evaluate conductor performance and cost.
- Detailed characterization of polyimide insulation properties with regard to radiation tolerance.
- Use of LHe II in large system applications.

3.1.3 Axicell Resistive Magnets

3.1.3.1 Design Considerations

Low power dissipation, small coil build, and long operational life in the radiation environment are the most desirable coil features. Design simplicity and easy coil replacement are equally important from construction and maintenance points of view. Minimizing the power requirement is possible by selecting proper coil geometry and current distribution in the windings. However, coils with constant current distribution and appropriate geometry can approximate the ideal coil, and we selected them for the MARS design because they minimize coil size and are easier to manufacture. The basic design limitations of these coils are stresses and cooling. Electrical integrity is also a limitation, but its causes are difficult to identify because they depend on individual manufacturing details as well as on design parameters.

Encircled by a 16-T peak-field superconducting coil, the resistive inserts must generate additional central fields to meet the 21- and 24-T requirements and provide the clear bore necessary for the plasma. In addition, the coils should minimize radial build and ohmic power loss. We have met these requirements using a uniform current density coil. Future design changes may further reduce the size and power of the coils over the baseline design.

Because the stress level within the resistive coils is quite high, we selected a magnesium-zirconium-chromium (MZC) copper alloy. Cooling is a limitation for internally cooled MZC magnets because of their relatively long and small water paths, but we chose this option because it offers high confidence in long expected lifetimes.

Of all the magnets in the MARS magnet set, the axicell resistive magnets sustain the most severe radiation exposure. The peak radiation dose rate is approximately 10^6 rad/s. Space limitations on these magnets do not leave room for radiation shielding. Any radiation shielding placed between the required clear bore and the magnets increases costs because of greater power dissipation in the winding and the increased cost of the surrounding superconducting coil. The radiation environment degrades the mechanical and electrical properties of the components and reduces their service life.

The requirements for the axicell resistive magnets are summarized in Table 3.1-6, and include central fields of 21 and 24 T, clear bores of 54 and 58 cm, and an operational life of more than 1 yr, which is about an order of magnitude longer than that of existing high-field coils.

3.1.3.2 Engineering Trade-Off Studies

Coil Alternatives. Several types of coils have been considered: (1) Bitter coils; (2) modified Kelvin distribution coils as exemplified by the TASKA design⁷; (3) internally cooled uniform current density coils; and (4) externally cooled uniform current density coils. Our initial choice was the externally cooled, uniform-current density coil shown in Fig. 3.1-10 because of its high achievable current density which results in a much smaller encircling superconducting coil. We did not choose Bitter coils because they must use organic insulation, which does not meet our radiation requirements. The modified Kelvin design, while using less power, requires much larger diameters and axial space.

Most laboratory high-field coils are externally cooled with water. We evaluated several of these, including the 31-T coil at the Francis Bitter National Magnet Laboratory, the 30-T magnet at the Australian National University, a 16-T coil at Clarendon, and the Duplex I and II magnets at Grenoble. These magnets have an average service life (without radiation) of about 200 hr, which is unacceptable for MARS. We evaluated the causes of

Fig. 3.1-10. Externally cooled resistive coil.

failures in externally cooled coils.^{8,9} Some of the failure mechanisms that we observed for Bitter magnets, their applicability to MARS, and potential solutions are given in Table 3.1-7.

As our investigation continued, it became apparent that separating the coolant and the insulation is necessary to achieve reliable operation for more than . FPY using current or foreseeable technology. Figure 3.1-11 shows our latest concept of an internally cooled coil, our current baseline. The main disadvantages of the internally cooled conductor coil are its lower allowable current density (larger build required) and separate powering for each layer (needed to keep water passages short).

Choice of Conductor and Insulation. The conductor for the resistive magnet has to satisfy two requirements: high yield strength to withstand hoop stresses and high electrical conductivity for low power dissipation. Silver is the best electrical conductor, copper is next, and gold and aluminum follow. Precipitation-strengthened alloys of copper have high electrical conductivity, high yield strength, and a high recrystallization temperature. We considered and compared several alloys of copper and one alloy of silver. Figure 3.1-12 shows the values for mechanical yield strength and electrical conductivity of these alloys.

The composition of the alloys is:

1. Be - Cu (Be 0.4 to 0.75%, Co 2.4 to 2.7%, balance Cu).
2. Cu - Ni - Ti (5% Ni, 2.5% Ti, balance Cu).
3. Glidcop Al-20 (0.4% Al_2O_3 , balance Cu).
4. Narloy Z (2.75 to 3.25% Ag, 0.3 to 0.5% Zr, balance Cu).
5. Consil 900 (Cd 10%, Ag 90%).
6. ZAC-1 (0.1% Zr, 0.3% Cr, balance Cu).
7. MZC (0.03 to 0.06% Mg, 0.08 to 0.015% Zr, 0.4 to 0.8 Cr, balance Cu).
8. AMZIRC (0.13 to 0.2% Zr, balance Cu).

Consil 900 is desirable because of its lower induced radioactivity, but it does not have sufficient yield strength. We chose MZC for the conductor material because it has sufficient conductivity and yield strength and is therefore appropriate for the design.

Fig. 3.1-11. Internally cooled resistive coil.

Fig. 3.1-12. Alloy properties for resistive coils.

Table 3.1-6. Requirements for axicell high-field resistive insert coils.

Central field	21.0 T (M1) ^a 24.0 T (M2) ^a
Operational life	More than 1 yr
Clear bore radius	29 cm (M1) 27 cm (M2)
Structural case design	ASME pressure vessel code

^a Combined with superconducting background coil.

Table 3.1-7. Failure mechanisms for Bitter magnets.

Failure mechanism	Applicability to MARS	Potential solution
Water absorption in compressed insulation	No	Copper-clad polyimide ^a
Water attack on copper/insulation board	No	Bond epoxy to phenol formaldehyde coating
Radiolytic corrosion of copper	Yes	Suppression by coating/plating
Water oxidation of mechanical joints	Yes	Eliminate or gold-plate joints
Growth of CuO whiskers in water	Yes	Suppression by coating/plating

^a Ceramics resist water penetration.

Spinel (Al_2MgO_4) has low swelling and no microcracking, and it strengthens in a neutron radiation environment. A stress analysis shows that 3 vol % swelling in spinel can be accommodated in the magnet structure without causing stress problems. This amount of swelling is expected in spinel after 2 FPY of magnet operation. Under the same conditions, alumina (Al_2O_3) would swell 16% by volume, develop microcracks, and deteriorate in mechanical strength. Based on these observations, we selected spinel as the insulator material for the resistive magnets.

Choice of Cooling. We considered water, liquid nitrogen, and gaseous helium to cool the coil. The three coolants were evaluated in an externally cooled coil having a current density of 3700 A/cm^2 (the baseline at that time). The figure of merit was the total power requirement of the coil (resistive losses plus pumping or refrigeration power). We considered water because of its excellent thermodynamic properties; we considered the two other coolants because they are chemically inert and could lead to longer lifetimes for externally cooled coils in a radiation environment.

Water-Cooled Option. The water-cooled option has a negligible pressure drop and pumping power requirement. The maximum resistive loss in the 24-T coil at end of the magnet lifetime is only 25.9 MW (based on an increase in resistivity of 20% because of radiation). The conductor temperature is lower than 100°C . These characteristics are favorable and we tentatively selected water for the baseline coolant. The externally water-cooled case was also used as a point of comparison to evaluate the two other coolants.

Liquid-Nitrogen-Cooled Option. If we assume that the magnet configuration remains the same as the baseline, the resistive power consumption is reduced with LN_2 coolant by the fraction of copper resistivity that remains at 80 K. For pure OFHC copper, resistivity decreases linearly with temperature, from room temperature to LN_2 temperature at a rate of about $6.9 \times 10^{-9} \text{ ohm-cm/K}$, so that the resistivity ratio is approximately 8. In a neutron environment, this ratio is smaller because the increase in resistivity due to transmutations is independent of temperature. After 1 FPY this resistivity increase is 20% at room temperature, or 160% at 80 K. For damaged copper, the ratio of resistivity from 300 to 80 K is about 3.7 after 1 FPY. Thus, for an LN_2 -cooled coil, the resistive power is initially reduced by a factor of 8, but only by a factor of 3.7 at the end of 1 FPY.

Conversely, the refrigeration power is high. Theoretically about 3.8 W of compressor work is required to produce 1 W of cooling at LN_2 temperature. Most real compressors have efficiencies of less than 0.5, so at best an LN_2 -cooled refrigerator requires 7.6 W input per watt of cooling. Thus, the total power consumed by an LN_2 -cooled magnet is 8.6 times the joule heat dissipation. For radiation-damaged copper, the trade between water cooling and LN_2 cooling shows that the total power requirements for an LN_2 -cooled magnet is more than twice that of a water-cooled magnet. Moreover, the addition of a cryostat will increase the coil size. For these reasons we rejected the LN_2 option.

Gaseous Helium-Cooled Option. The main advantage of GHe as a coolant is that it is inert. Our hope was that the GHe could be pumped through the coil at high enough velocities (and thus a high heat transfer coefficient) to keep the conductor cool without using excessive pump power. However, a heat transfer analysis showed that, for a maximum conductor temperature of 125°C , the total power consumption is more than double that for water cooling. There is a 10-MW increase in resistive heating and a 20-MW increase in pump power. Because the GHe cooling concept is not as efficient as water cooling, we rejected it.

We chose water as the baseline coolant because it minimizes the power requirements of the resistive coils. However, because externally cooled coils have reliability problems,^{8,9} we selected an internally cooled conductor coil for the baseline design. Future developments may show that spinel-insulated, externally cooled coils can operate reliably enough, and if this is the case, we will reexamine our design decision.

3.1.3.3. Magnet Design

The two resistive magnet inserts produce axial magnetic fields of approximately 6.0 and 9.6 T. These fields add to the background field of the axicell superconducting coil to obtain total fields of 21 and 24 T.

The coils have a compact build and an efficient shape for reducing ohmic power loss. They are layer-wound with a relatively wide conductor of high-strength low-resistance MZC with insulators made of sintered spinel (Al_2MgO_4). Electrical leads are anchored to the coil flanges and are distributed to

accommodate the ICRH coaxial leads. The coils have inner radii of 0.30 and 0.32 m to provide sufficient clearance for the plasma and are enclosed in a casing with a support structure to react the axial magnet forces.

Conductor and Insulation. For reliability of operation, we chose an internally cooled conductor for the baseline. For high current densities, this type of conductor requires relatively short water paths to prevent overheating, which requires cooling and powering each layer separately. The current density of an internally cooled conductor must be less than that of an externally cooled conductor. These factors combine to give a coil that consumes 10% more power and has a volume 23.5% greater than a coil using external cooling. However, these disadvantages are offset by increased coil reliability resulting from the use of dry insulation.

The conductor hoop stress is 300 MPa (43.5 ksi), which is too high to be able to use OFHC copper. The peak conductor temperature is 200°C, so to be conservative we selected MZC copper for the conductor material, which not only has a high allowable stress, but it also has a low resistivity of 80% of the International Annealed Copper Standard (IACS). The shape of the conductor is a flat bar with four cooling holes. Figure 3.1-13 shows a cross section of the conductor unit cell.

We have chosen spinel for the conductor insulation. Water passages are not necessary between conductors, so we chose the insulation thickness for good electrical isolation of the conductors. Layer-to-layer insulation consists of short rectangular bars butted against each other and the turn-to-turn insulation consists of bars curved to match the radius of the coil.

Conductor Cooling. The results of the thermal analyses for the internally and externally cooled resistive coil designs are listed in Table 3.1-8. The flow rate is the major difference between the two cooling schemes. The externally cooled design has a greater flow area, allowing four times the flow rate of an internally cooled coil with negligible pressure drop, and much lower water velocities. Consequently, an externally cooled coil has a cooler conductor at a higher current density as compared with an internally cooled coil. The total power consumption is 124.6 MW for the internally cooled coils vs 114.4 MW for the externally cooled coils. The greater power consumption is justifiable based on the added electrical reliability of internal cooling.

Fig. 3.1-13. Internally cooled conductor design.

Table 3.1-8. Comparison of operating parameters for internally and externally cooled 24-T resistive coils.

Operating parameters for 24-T coil	<u>Internally cooled coil</u>			<u>Externally cooled coil</u>
	Inner layer	Outer layer	Coil (average)	(average)
Overall current density (A/cm^2)	2500	2500	2500	2800
Winding volume (cm^3)	38,700	88,200	1.42×10^6	1.15×10^6
Resistivity ($ohm\text{-}cm \times 10^{-6}$)	3.6	2.8	3.4	2.9
Cooling load including nuclear heating (MW)	2.2	2.3	51.7	42.9
Water velocity (cm/s)	1000	1000	1000	157
Heat transfer coefficient ($W/cm^2 \text{ - } ^\circ C$)	5.1	5.1	5.1	0.657
Flow rate (liter/min)	200	200	4600	20,000
Water temperature rise ($^\circ C$)	147.5	162.5	16.1	27.5
Peak conductor temperature ($^\circ C$)	200	200	N/A	117
Pressure drop (atm)	21	35	N/A	~0
Total power consumption (MW)	1.4	2.2	42.0	38.2

3.1.3.4 Engineering Analysis

We performed the following analyses in support of the design of the axicell resistive coils.

Magnetics. The resistive coils have gone through several changes. The central field strength, coil inner radius, number of coils, method of conductor cooling, and coil current density have all changed. As we described in Section 3.1.2.4, there are currently two coils at each end of the reactor, surrounded by a large superconducting coil: M-1 has an inner radius of 0.32 m and a central field of 21 T, and M-2 has an inner radius of 0.30 m and a central field of 24 T. Both coils have a current density of 2500 A/cm^2 and internally cooled conductors (Fig. 3.1-11).

Unlike a superconductor, the current-carrying capacity of a resistive conductor is not limited by field strength, which simplifies the design approach. The power dissipation of a resistive coil in a radiation environment varies with the radius and the type of radiation. We evaluated the resistive losses by determining the radiation damage for each layer of conductor and the resultant increase in resistivity for that layer and then calculating the power.

One disadvantage of the internally cooled conductor coil is that most of the layers must be individually cooled; this calls for separate power supplies. On the other hand, this approach may offer advantages of its own by allowing us to grade the current density to reduce coil size.

Coil shape has a definite effect on the power consumption. For a given field, inner radius, and uniform current density (radiation effects neglected), only one shape will give minimum power consumption. This is the shape that also gives minimum coil volume for the above conditions. We used this concept in sizing the coils.¹⁰ With more study it may be possible to optimize the coil sizes, taking into account the varying resistivity due to radiation.

Another approach to the resistive coil design minimizes ohmic power at the expense of greater coil volume. This is typified by the modified Kelvin distribution described in the TASKA Design Study.^{7,11} This type of coil uses a series of concentric layers, with each succeeding layer being wider and having a lower current density than its inner neighbor. This type of coil

substantially reduces the ohmic power required, but at the cost of a much larger magnet. In the case of a large encircling superconducting magnet, this approach is uneconomical because the larger resistive coil causes a large increase in the inner radius of the superconducting coil and an even larger increase in the coil volume.

Force and Stress Analysis. For our stress analysis of the baseline resistive coils we evaluated the conductor hoop stresses and the anchoring of the conductor leads, and analyzed the effect of the large axial forces on the coils. The coil cases are of secondary concern because they carry no internal pressure. The hoop stress in the conductors has a maximum value of 300 MPa (43.5 ksi), which is well within the allowable yield for MZC copper. However, developmental work will be required before a fabricator can make the four-hole hollow conductor. Because the individual layers are cooled and powered separately, the conductors must be anchored at each end. Thus, a coil with 24 layers has 48 individual conductor anchorings. The anchoring force is approximately equal to the conductor hoop force. We have taken a preliminary look at the anchoring and determined it is feasible.

Large axial forces also act on the coils. We have not yet evaluated the effect of these forces on the winding, the coil case, or the anchoring of the conductors. The side force of 2.48×10^7 N gives an average pressure of 15.23 MPa (2200 psi) on the end of the case. This does not appear to be a problem, but we will have to consider it in the detailed design.

Thermohydraulics. We analyzed the thermohydraulics of the resistive choke coils to determine the water flow rates necessary to keep the coil cooled to below the desired operating temperature and to verify that the capability exists to transfer the heat into the water. The basic principles of heat transfer are identical for either the externally or internally cooled coils, although the details of computation differ.

First, the total heat load is computed by adding the joule heating in the conductor to the heat deposited by neutron radiation. For the internally cooled coil, the heat load is computed for each hydraulic path. For the MARS coils, this corresponds to one layer of conductor. The winding volume, then, is the unit cell areas times the length of conductor required to wind one

layer. Alternatively, for the externally cooled coil, there is only one equivalent hydraulic path through the magnet and the heat load is computed for the magnet as a whole.

Once the coolant heat load is calculated, the required water flow rate is determined. The heat balances in the conductor and coolant are solved interactively to determine coil operating temperatures, allowing all temperature-dependent properties to vary. The heat transfer coefficient is from the Colburn equation

$$h = \frac{0.23}{D^{0.2}} \frac{kV^{0.8}}{v^{0.8}},$$

where

- h = heat transfer coefficient (W/cm² K),
- D = flow passage hydraulic diameter (cm),
- k = thermal conductivity of water (W/cm K),
- v = kinematic viscosity of water (cm²/s), and
- V = water velocity (cm/s).

The inlet water temperature is taken to be 32.5°C, a value expected to be typical of the capability of a cooling tower. For the externally cooled coil, the flow rate is determined by keeping the conductor temperature less than 100°C. This eliminates the possibility of boiling in a nonpressurized system. For the internally cooled coil, the flow rate is chosen to minimize the total power input to the coil with an additional constraint of a maximum conductor temperature of 200°C to avoid recrystallation of the copper and loss of strength of the conductor. The internally cooled coil requires a pressurized system to avoid water vapor formation.

The neutron effects are the most severe at the inner layer of the coil, and they drop off exponentially with radial distance. A volume-averaged value of resistivity was found to be 2.9×10^{-6} ohm-cm (this represents a 20% increase in initial resistivity) for the externally cooled coil. The radial distributions of increased resistivity and neutron heat deposition were used to compute heat transfer for the inner and outer layers of the internally cooled coil.

Radiation Environment. In the resistive magnets, the insulation and conductors near the inner surfaces of the coil see an almost unattenuated

stream of neutrons and dose rates as high as 10^6 rad/s. In this radiation environment, organic insulators with a radiation dose limit of 5×10^9 rad are unsuitable and ceramic insulators have to be used. For the reasons given in Section 3.1.3.2, we selected spinel Al_2MgO_4 as the material for the insulator.

The neutrons affect both the mechanical and electrical properties of the conductor. The major changes in mechanical properties are caused by radiation-induced void swelling, radiation creep, and radiation embrittlement. Major changes in electrical properties are caused by defects introduced into the conductor lattice and by transmutations of atoms. The electrical resistivity changes due to defects (sometimes referred to as dpa damage) saturates at any temperature when the recombination rate of defects equals the generation rate of defects. In OFHC copper, the saturation value of induced resistivity is approximately 80×10^{-9} ohm-cm at 30°C . The increase in resistivity due to transmutations does not saturate but increases linearly with neutron fluence.

The peak change in conductor resistivity at the inner radius as a result of both transmutations and dpa damage is estimated to be approximately 59%, with an average increase of resistivity throughout the coil of 20% after 2 FPY.

The corrosion rate of water-cooled copper varies with the resistivity of the water and is minimized for 1 Mohm-cm water at 0.5 mil/yr. For purer water, 8 to 10 Mohm-cm, it is 7.7 mil/yr, and for 0.004 Mohm-cm it is 2.8 mil/yr. Irradiation of the coolant (deionized water) affects the rate of corrosion of copper and introduces new mechanisms of corrosion. Radiolysis of the water occurs when radiation is present. Radiation breaks H_2O molecules down to H^+ and OH^- radicals that interact to produce other radicals and H_2 , O_2 , and H_2O_2 . It is estimated that the oxygen concentration in the water will be saturated at about 8000 ppb. At this concentration and at temperatures of 60°C , water resistivity cannot be as high as 1 Mohm-cm. Radiolytic corrosion, which is oxidation of copper by H_2O_2 , may erode 0.65 mm in 2 FPY. Radiolytic corrosion can be lowered by adding reducing agents such as H_2 or hydrazine or by using an ion exchanger.

3.1.4 Transition and Anchor Coils

These coils are similar enough for us to give a common description. Their basic function is to provide the nonsymmetrical fields necessary to help

obtain MHD stability. Each coil is a quadrupole. The anchor coils have total sweep angles of 160 deg, while the transition coils have maximum sweep angles of 210 and 240 deg. They all have a uniform current density and, because they are similar to comparable coils of MFTF-B, they use a copper-stabilized NbTi conductor. The coolant is LHe I.

The coil cases are 304LN stainless steel; insulation is polyimide fiberglass. Each coil has an LN_2 -cooled radiation shield and vacuum vessel. The forces acting on these coils are not known at this time, but are expected to be large. We assume that each coil will have a structural yoke to contain the spreading forces but we have not determined the location and number of coil supports. Each coil will have a service stack for all current leads, fluids, and instrumentation leads. Table 3.1-9 compares the major design parameters of the transition and anchor coils of MARS and MFTF-B.

The transition and anchor coils may need some radiation shielding to protect them from neutrons streaming from the central cell and to prevent D-D neutrons born in the anchor from sloshing-ion interactions.

The major areas that we will be investigating in FY 1983 for the transition and anchor coil regions are:

1. Radiation analysis, shielding and neutral beam access.
2. Magnet support structure design.
3. Vacuum vessel design.
4. Potential interface problems between transition and anchor magnet cryostats.
5. Design approaches for preventing stagnation of helium bubbles.

Table 3.1-9. Comparison of MARS and MFTF-B transition and anchor coils.

	Transition coils		Anchor coils	
	MARS	MFTF-B	MARS	MFTF-B
Mean major radius (m)	1.25/2.0	0.875/1.2	3.0	2.5
Sweep angle (deg)	210/240	200/220	160	150
Mean minor radius (m)	1.25	0.7/1.35	0.80	0.75
Sweep angle (deg)	180	180	180	180
Winding pack height (m)	0.46/0.53	0.45/0.34	1.85	0.82
Winding pack width (m)	0.46/0.53	0.2/0.34	0.32	0.33
Current density (A/cm ²)	3000/2900	2844/3075	2750	1977/2271
Peak field (T)	6.8	4.5	8.5	7.0

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3.2 ECRH SYSTEMS

Electron cyclotron resonance heating is critical to the operation of the endplugs in a tandem mirror reactor (TMR). As discussed in Section 2.6, ECRH injected at the minimum of the endplug magnetic mirror (Point B) creates a collisionless, mirror-trapped electron population that helps maintain the thermal barrier. This barrier decouples the central-cell electrons from those in the region of the potential peak. The magnitude of the potential peak is proportional to the electron temperature at that point; the decoupling makes it possible to maintain a high electron temperature with reasonable ECRH power (at point A). Hence, two ECRH systems are included in the MARS baseline. The requirements defined by the TMR physics code are shown in Table 3.2-1.

3.2.1 ECRH Systems Analysis

ECRH systems on past and current devices deliver powers in the range of 80 to 200 kW and have frequencies of 28, 35, or 50 GHz.¹⁻³ The basic systems include a pulsed, capacitor-charged power supply, a 200-kW gyrotron, and a 2.5-in. overmoded waveguide transmission system. These systems have been inefficient and expensive, and when scaled to reactor powers they tend to be cumbersome. Hence, we have considered several options that would need development.

The source in an ECRH system has a major effect on both system cost and efficiency. In addition, it affects the power supply and transmission system characteristics. Because experience with other microwave tubes indicates that the use of high power tubes provides a cost benefit, we have considered using 1-MW gyrotrons. In principle, gyrotron efficiencies (30 to 50%) can be improved by direct recovery of the electron beam. The baseline MARS ECRH source is a 1-MW gyrotron with a depressed collector.

Although this gyrotron is our baseline source, it does not possess some very advantageous features: large bandwidth, frequency variability, and a gaussian wave mode. Free electron lasers (FEL's) contain these features, but they are at an early stage of development and we have no long-term development plan for them. Thus, although the FEL appears to be an ultimately more suitable ECRH source than the gyrotron, we have not selected it as the baseline source.

Table 3.2-1. ECRH system requirements.

	<u>At point A</u>	<u>At point B</u>
Frequency vacuum (GHz)	95	64
β -corrected	74	49
Relativistic	44	30
Trapped power (MW)	2	54
Trapping fraction	1	1
Delivered power (MW)	2	54

Because our experiments have used pulsed systems, and because the actual reactors will need continuous systems, we will have to continuously convert the power rather than store it in capacitors for intermittent use. A conventional steady-state power supply includes a fused disconnect, step/start contactor, step controller, transformer/rectifier, capacitor comp crowbar, modulator/regulator, and vacuum disconnect/ground, as well as gyrotron mounts and controls. An induction voltage regulator and cathode crowbar could replace the modulator/regulator tube if the circuit elements are designed to withstand the 10X fault currents for a satisfactory number of crowbars.

To reduce the cost of the ECRH system, we have considered a less costly power supply. The system is based on a Graetz bridge ac/dc converter similar to the type used in a power grid for dc transmission.⁴ This option is discussed in Section 3.2.2 as the baseline supply. Note that several key issues are yet to be resolved and the conventional power supply is our backup option.

The transmission system offers two distinct options: the overmoded waveguide system and the quasi-optical transmission system. The efficiency of the overmoded waveguide transmission line is very sensitive to the mode purity requirements of the injected wave, and is usually in the 50 to 80% range. Even if megawatt gyrotrons are considered, single entry of the guides would be both unwieldy and impractical for high power systems. As a result of the high magnetic fields and high powers, efficient power combination techniques are

not available. Power density limitations for metallic guides would limit the number of gyrotrons that could be combined in a composite launcher. For high powers, a pressurized guide with a ceramic vacuum window is probably required, but windows capable of handling the high powers in the radiation environment do not currently exist.

In contrast, quasi-optical transmission does not have some of the limitations of waveguide systems, offering low loss power combinations with more relaxed constraints on allowable power density. Because the system is in vacuum, ceramic windows are not required. For these reasons, we chose the quasi-optical transmission system described in Section 3.2.4. It is an evolution of designs previously employed in the WITAMIR⁵ and TASKA⁶ tandem mirror fusion devices.

Our baseline ECRH system, which comprises the Graetz bridge power supply, megawatt gyrotrons with depressed collectors, and the quasi-optical launcher, evolved after an extensive trade study. Our analysis compared system capital costs, efficiency, and recirculating power for several ECRH configurations.

3.2.2 ECRH Power Supply

The power supply requirement of the ECRH systems is 83 MW. One or a number of supplies must provide 150-kV 578-A continuous dc power to the gyrotron cathodes. The choice of the number of supplies necessary to convert to dc power depends on the reliability of the supply, how long the ECRH system will be down if a supply malfunctions, and what impact loss of ECRH power would have on plant operation. The Graetz bridge power supply for dc transmission has proven to be very dependable, and the technology is capable of converting the required power.⁷

The Graetz bridge supply includes transformers, thyristor valves, circuit breakers at a number of locations, dc smoothing reactors, dc harmonic filters, lightning arrestors, and overcurrent diverters. The basic power supply configuration is shown in Fig. 3.2-1.⁷

The bridge is an ac/dc converter consisting of six main thyristor valves connected as a two-way, three-phase circuit with a seventh valve as a bypass valve connected across the dc bus and ground. The group must be rated for 150 kV and ~600 A dc, and draws from a 230-kV ac line. The MARS requirements

Fig. 3.2-1. Simplified diagram of bridge circuit with valves: (1) external damping circuits (2), lighting arresters (3), and overcurrent diverters (4).

are well below state of the art; the typical bridge used in the electric power grid is rated at 285 MW, 133 kV, and 2160 A.⁸ A thyristor valve can be designed with a minimum number of thyristor devices in series to reduce costs and power losses and increase reliability. A typical valve is shown in Fig. 3.2-2.

3.2.3 ECRH Source

The gyrotron is the microwave source for the frequency range of interest. The development program to achieve the desired operating characteristics for MARS is nearly in place with only the depressed collector to be added.⁹ As described previously, the MARS baseline is a 1-MW gyrotron with a depressed collector. Each tube operates at 150 kV and 8.5 A. The collector voltage is chosen to be -40 kV; the net efficiency is estimated to be 80%.

Current gyrotrons are of the microwave cavity design shown in Fig. 3.2-3. In this configuration, the electron beam propagates in the resonant cavity and also shares the collector cavity with the wave. As the gyrotron power increases, the collector cavity must be enlarged to dissipate the beam power, making it more difficult to couple to the desired cavity mode efficiently. One of the 1-MW gyrotron concepts, the quasi-optical gyrotron, offers a solution to this problem. As shown in Fig. 3.2-4, it is a double-cavity device with the electron beam traveling through two optical cavities perpendicular to their axis.¹⁰

3.2.4 Quasi-Optical Launching System

The wave mode in a conventional metallic waveguide is characterized by a propagation constant and field pattern that is the same at all points along the guide. By contrast, the beam mode in the quasi-optical system consists of a bundle of waves characterized by a spectrum of propagation constants. The field distribution varies along the beam guide but is reset at the conducting mirrors (see Fig. 3.2-5), which act as periodically spaced phase transformers. This permits beam transport with minimal diffraction losses.

The lowest loss-beam mode, designated TEM_{00} , is characterized by a Gaussian radial amplitude distribution and linear polarization. Beam modes in general are characterized by the product of a Gaussian and the generalized Laguerre polynomials.¹¹ The lowest order Laguerre polynomial, L_0^0 , is simply

Fig. 3.2-2. A water-cooled thyristor valve with a rating of 133 kV and 2160 A, installed at the Sylmar Converter Station of the Pacific Intertie.

1. Main magnet coils	7. Collector magnet coils
2. Gun magnet coil	8. Output guide up-taper
3. Electron gun	9. Output guide down-taper
4. Cavity	10. Lower collector gap
5. Output waveguide and window	11. Transverse field magnet
6. Beam collector area	

Fig. 3.2-3 The conventional gyrotron of a microwave cavity design.

Fig. 3.2-4. Quasi-optical gyrotron.

Fig. 3.2-5. Schematic of the MARS quasi-optical launching system.

equal to one, however, and thus the TEM_{00} mode transverse electric fields are given by

$$E_r = E_{00} e^{-a^2 r^2 / R^2}, \quad (3.2-1)$$

where

- R = guide radius (i.e., mirror radius),
- $2z_0$ = phase transformer spacing or "iteration length",
- a = $\sqrt{k/2z_0}$, and
- k = $2\pi/\lambda_0$.

This lowest-loss TEM_{00} mode in Eq. (3.2-1) is shown in Fig. 3.2-6(a) and is linearly polarized. Note the Gaussian radial dependence of the electric field. The next lowest-loss mode, TEM_{01} , is circular electric (i.e., ϕ -independent, not circularly polarized) and is shown in Fig. 3.2-6(b). Because the electromagnetic energy of these beam modes is concentrated near the axis, wall losses and wall breakdown problems associated with metallic waveguides are eliminated.

The parameter "a" in Eq. (3.2-1) is analogous to the Fresnel number of geometrical optics. The diffraction (or iteration) loss, i.e., loss per mirror spacing, is shown in Fig. 3.2-7 as a function of the parameter "a." It is evident that for a given frequency (i.e., given k) it is possible to design the mirror spacing and radius for any desired diffraction (iteration) loss.

The function of the mirror configuration in Fig. 3.2-5 is interpreted by Fig. 3.2-8. In the absence of the hyperbolic mirror, excitation at focal point A' would result in parallel rays, i.e., a uniform phase front at B. This is the conventional Cassegrain system of geometrical optics. In the offset Cassegrain system shown, adding the hyperbolic section merely switches the excitation point to A and aperture blocking is prevented by the offset.

To launch the fundamental beam mode into the mirror system, a conventional dual-mode horn-reflector can be employed. Such an antenna is also an offset paraboloid. This system is shown schematically in Fig. 3.2-9. The apex of the cylindrical horn is placed at the focal point of the paraboloid. The horn has a mode converter in its throat which, if fed with a cylindrical TE_{11} waveguide mode, produces both E_{11} and TM_{11} modes in the horn. These

Fig. 3.2-6. Quasi-optical beam modes illustrating the gaussian radial dependence.

Figure 3.2-7. Diffraction losses for beam mode.

Fig. 3.2-8. Offset Cassegrain mirror system.

Fig. 3.2-9. The dual mode horn and associated mode conversion.

modes together in the horn produce, by interference, a near-Gaussian radial amplitude distribution at the horn mouth.¹² Reflection from the paraboloid creates a uniform phase front and the resulting aperture field is exactly the field required to launch a beam mode. The dimensions shown in Fig. 3.2-9 are for one line of the 64-GHz launching system.

However, gyrotrons operate in the TE_{0n} mode and not in TE_{11} . Therefore, an intervening mode converter is required between the gyrotrons and horns. A stepped twist-reflector can be employed here to provide the necessary conversion to the TE_{11} mode. A quarter wave grating covers one-half of the reflector and converts the circular symmetric mode electric field into an anti-symmetric field. At this point the quarter wave grating of the twist-reflector reverses the polarization of only one of the rectangular components of the electric field, with the result that the fields now conform to the TE_{11} fields. The desired polarization direction can be obtained by rotating the reflector.

The power losses on the system are assumed to be 5% in each of the mode converters and the dual mode horn and a 0.2% per mirror. The total transmission loss is then ~11%. The assumed efficiencies are those obtained in low power devices (e.g., several watts) that have been and are being tested.^{13,14}

3.2.5 ECRH Systems Layout

Figure 3.2-10 is a cross-section of the 64-GHz ECRH launcher system. Note that a launcher is defined as one complete circular array of gyrotrons with associated mirrors. The design power of this launcher is 13.5 MW. Thus, two launchers will be required at each end of the machine, giving a total ECRH power injection of 54 MW at 64 GHz. The dimensions A through G in Fig. 3.2-10, together with other relevant parameters, are given in Table 3.2-2.

The geometrical design of the launcher in Fig. 3.2-10 is taken from the specification of four base requirements, namely, the ECRH frequency (64 GHz), the design power of the launcher (13.5 MW), the diffraction loss per iteration path (0.01 dB or ~0.2%) and the angular spread (2α) around the injection angle (± 5 deg). The diffraction loss per iteration path of 0.01 dB was chosen as a reasonable compromise between the diffraction (scattering) power loss and mirror dimensions. Given these base system requirements as well as the efficiencies of gyrotrons, mode converters, and mirror reflectivities, the

Fig. 3.2-10. Cross section of the quasi-optical launching system.

minimum dimensions of the launcher in Fig. 3.2-10 can be reduced to a set of coupled algebraic equations. It should be noted that the system in Fig. 3.2-10 and Table 3.2-2 includes two redundant gyrotrons.

Table 3.2-2 also gives a breakdown of the launching system losses. The design diffraction loss per iteration (0.01 dB) and the mirror reflectivity losses ($\sim 0.1\%$) will be much smaller than the expected power losses in the mode converters.

Table 3.2-2 lists similar parameters for the 96-GHz ECRH systems. The power requirements here are low compared with those for the 64-GHz system (i.e., 1 MW at each end compared with 27.6 MW). Therefore, because a maximum of only three gyrotrons is required for each launcher (two for the required power output after losses plus one for redundancy), there is considerably more flexibility in launcher dimensions. Accordingly, with the constraints on diffraction losses per iteration similar to those above, a launcher length of 20 m was chosen to facilitate ease of location. This resulted in a lower angular spread (2α) about the injection angle of ± 0.7 deg.

The consequence of any decrease in frequency is that system dimensions can become very large in order to keep the diffraction losses low. For example, if we use the vacuum field frequency and allow the resonance surface to shift, then a 40% Doppler shift from the design value of 64 GHz to ~ 38 GHz would require paraboloidal mirror diameters (D in Fig. 3.2-10) of ~ 1 m and launcher lengths (length $A + B$) of ~ 35 m. This would result in a rather unwieldy launcher configuration. Therefore, we are studying the use of second harmonic heating to keep the transmission frequency high.

As the ECRH beam is transported down the launcher to the machine, it encounters a value of the fringing B-field from the magnets that corresponds exactly to the resonance value at the injection point on the z-axis. At this outer resonance point, any residual neutral gas will be efficiently ionized by electron cyclotron resonance, with attendant problems of power absorption and breakdown. It is thus necessary that we specify the maximum design gas pressure in this region, such that breakdown effects will be minimized. Preliminary results from MFTF-B suggest that the maximum gas pressures of $\sim 10^{-5}$ Torr will be required.¹⁵

Table 3.2-2. Parameters for the ECRH launcher systems.

	<u>Thermal barrier</u>	<u>Potential peak</u>
ECRH power requirement/endplug (MW)	27.5	1
ECRH frequency (GHz)	64	96
No. of launchers (each end)	2	1
Design power per launcher (MW)	13.5	1
Maximum power per launcher (MW)	15.18	2.68 ^a
No. of gyrotrons/launcher (MW)	17 ^a	3 ^b
Gyrotron power/tube (MW)	1	1
Angular spread at plasma (deg)	± 5	± 0.7
Diffraction loss/iteration (dB)	0.01	0.01
Reflection loss/mirror (%)	~ 0.1	$\sim 0.07\%$
Mode converter loss (%)	~ 5	~ 5
Horn loss (%)	~ 5	~ 5
Total loss/launcher (%)	~ 10.7	~ 10.7
Launcher length, A+B (m)	18.27	20.0
Dia. of second parabolic mirror, 2C, (m)	3.20	0.48
Dia. of second parabolic mirror, D (m)	0.54	0.46
Dia. of hyperbolic mirror, E (m)	0.38	0.33
Horn length, F (m)	0.57	0.43
Dia. of horn mouth (m)	0.33	0.26
Gyrotron length, G (m)	~ 2	~ 2

^a Two redundant gyrotrons per launcher.

^b One redundant gyrotron per launcher.

Two remaining issues are tritium contamination and radiation damage. Because no vacuum window is required in the quasi-optical system, no tritium barrier is necessary between the machine vacuum and the launcher. Tritium contamination issues for both the launcher and gyrotrons need to be addressed, especially during gyrotron replacement. The folded-path arrangement of the offset Cassegrain optics in the quasi-optical system offers some unique advantages with respect to radiation damage protection. Figure 3.2-6, with the exception of the second parabolic mirrors, shows that all components can be shielded from direct line-of-sight of the fusion plasma. The key performance parameter for the parabolic mirrors is reflectivity, which is a function of their surface condition and bulk conductivity. However, it is difficult to envisage any radiation effects likely to degrade this reflectivity. Surface damage would have to be comparable to the wavelength (~ 0.5 cm) to be appreciable, while conductivity decreases due to neutron-induced transmutations would be negligible given the relatively low neutron flux at the mirrors. It can be concluded, therefore, that radiation effects on the quasi-optical system will be minimal. If the neutron flux is low enough to cause minimal damage to the gyrotrons, the folded path could be eliminated, thus simplifying and reducing the size of the ECRH vacuum vessel.

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3.3 ICRH SYSTEM

In the high-field magnetic well in the axicell region, ions are heated so that the trapping rate in the well balances the loss rate due to collisions into the loss cone, and a potential bump is created. The magnitude of this bump controls the passing ion density into the anchor; and the density is minimized within stability constraints. Until recently, the potential bump was created and maintained by neutral beam injection; the high energy beam was injected through a split hybrid coil set at 90 deg to the plasma axis. Because of access limitations between the axicell coil set, we have proposed ICRH.

ICRH has proved effective in heating Tokamak and mirror plasmas. Systems with powers up to ~ 4 MW have operated at frequencies from 25 to 46 MHz.¹⁻⁵ Although the axicell plasma, geometry, and field are distinctly different than what we have encountered up to now, a near proof-of-principle experiment in similar conditions is planned next spring on Alcator-C.⁶

3.3.1 ICRH Systems Analysis

Power balance calculations indicate that ~ 40 MW of ICRH power must be absorbed by the axicell ions to maintain the mirror-trapped distribution. An 85% ion absorption efficiency has been estimated; therefore, 47 MW of ICRH power must be delivered.

The vacuum field in the magnetic well is ~ 18 T. For a fixed frequency corresponding to this field, as beta increases, the resonance surface will move off the midplane. As discussed in Section 2.7, tritium ions need to be heated. The fundamental tritium ion cyclotron resonance frequency is $f_{CT} = 91.2$ MHz. Coupling to the plasma is improved by the use of the second harmonic because the evanescent region at the plasma edge, including the halo, decreases.⁷ In addition, second harmonic heating experiments have proven to be quite successful in Tokamaks.^{2,8} Hence, we use a frequency of $f_{2CT} = 182.4$ MHz.

A schematic of the axicell region is shown in Fig. 3.3-1. When we consider magnets, shields, structure, coolant feeds, etc., the ICRH launcher must fit into a very restricted space. Present ICRH systems have basically consisted of the power supply, coaxial transmission system, and one-half loop antenna launcher. However, because of radiation damage, lifetime,

Fig. 3.3-1. MARS axicell geometry.

and maintenance, several reactor studies have chosen waveguide launchers⁹⁻¹¹ over antennas, even though the physics of waveguide-plasma coupling is poorly known. For these reasons, we feel that the launcher is the most critical component of the MARS ICRH system.

We initially considered both waveguides and antennas for the MARS axicell. We eliminated waveguides for two important reasons. To match the load, waveguides are generally some multiple of one fourth of a guide wavelength, i.e., $\lambda_g/4$. For $f_{CT}/f_C = 1.2$, where f_C is the waveguide cutoff frequency, the guide length must be greater than 75 cm with an additional length for matching. This length would reduce the shielding available for the superconducting coil or would force an increase in the bore, neither of which are acceptable. The second reason is that, for a reasonable peak electric field, too many guides would be required for the available surface area (for a 60-cm bore), and the number of feeds to the guides would be too unwieldy.

The MARS ICRH system will thus have an antenna launcher. The issues of radiation damage, lifetime, and maintenance are critical, but since the resistive magnet inserts are faced with the same issues, the antennas will be designed within the same constraints.

3.3.2 ICRH Amplifier and Power Supply

The ICRH system must deliver 23.5 MW at 182.4 MHz to each axicell. The power will be launched from an as yet undetermined number of antennas and transmission lines. Because of the restricted space in the axicell region, the number of antennas and coax feeds should be minimized. Although we have not yet determined the power of an individual power supply we can calculate the basic amplifier configuration.

Each amplifier will probably consist of IPA/driver units (including local control and monitoring, medium voltage power supplies, and ac distribution), low-power 90-deg hybrid splitters, final power amplifier (FPA) cavities, FPA filament power supplies, and high-power 90-deg hybrid combiners.

The most practical amplifier approach, using current technology, is a cavity-type amplifier with triode or tetrode vacuum tubes. High power tubes rated above 100 MHz do not exist. The RCA 6950 shielded-grid double-anode triode has been tested to 750-kW output, just below the desired frequency. The

Eimac 4CX50000E tetrode can be operated in a derated mode at 180 to 190 MHz but for only ~100 kW. Because multimegawatt powers per line and antenna are desired, high-power tube development is needed. Otherwise, the power from several tubes will have to be combined. An Eimac 4CX15000A used as a cavity amplifier is capable of driving several FPAs; the 600 W required to drive the tetrode will be provided by a solid state amplifier.

The quadrature hybrid combiner will be a coaxial type using a 9-3/16-in. or 12-in. pressurized coax. The power supply and cooling requirements should be met with good design practices and presently available components. Figure 3.3-2 shows a schematic of a similar system at 154 MHz to deliver 1.3 MW.¹²

3.3.3 ICRH Power Feed

The transmission system will transport the power from the amplifier to the antenna, whose input impedance is not accurately known and may change with time. A schematic of the line and matching network is shown in Fig. 3.3-3; it is similar in concept to the TEXTOR system.¹³

The size of the coax for a majority of the system will be 9-3/16 in. o.d. with 50-ohm impedance, but because of the shield between the superconductor and the resistive insert, the final coax can be no larger than 6-1/8 in. o.d. Active, pressurized cooling inside the inner conductor will be required in L_4 and the third stub because of the expected high power (more than several megawatts), and may be needed in the entire line. The volume between the coaxial conductors in L_4 , st_3 , and part of L_3 will be evacuated, while the remainder of the system will contain a controlled atmosphere. The pressure and type of gas used will be determined by the desired safety factor.

Several voltage probes throughout the system will continuously monitor the match; the stubs are controlled by an on-line computer. The two-stub system will hold the voltage standing-wave ratio on L_1 at unity.

We will also have to include dc breaks to insulate the amplifiers and power supplies from the tandem mirror and coaxial vacuum windows.

3.3.4 ICRH Antenna

The Princeton Large Torus (PLT) experiments use end-fed half loop antennas that have an average power density on the antenna surface of

Fig. 3.3-2. A power amplifier chain of an ICRH system similar to that required for MARS.

Fig. 3.3-3. Schematic of an ICRH transmission and matching system.

$\sim 0.5 \text{ kW/cm}^2$. The power has been limited by breakdown at the feedthrough, and not by antenna surface effects. A center-fed antenna with an improved feedthrough is being built for which calculations predict a four-fold increase in power capability.⁸ Power densities in this range have been achieved on JFT-2, i.e., 1.4 kW/cm^2 on the antenna surface.¹⁴

The basic antenna configuration is a short-circuited section of stripline electrostatically shielded by an array of metal strips. The shield prevents the low density plasma from short-circuiting the ICRH voltage and suppresses the excitation of waves with the wrong polarization. The task during the next few months is to determine the antenna parameters necessary to optimize the power handling capability of the total system without exceeding the voltage standoff capabilities of the antenna feedthrough or transmission line components.

An antenna based on a previously developed model should have the following characteristics:¹³

- The antenna should be as close as possible to the plasma.
- Large antenna widths are advantageous, although the aspect ratio should not be too small.
- The capacitance should be as large as possible.
- The length should be $\lambda/4$. At large frequencies finite antenna effects might favor $3 \lambda/4$ lengths.

The vacuum feedthroughs are the weakest element in experiments. A large line reduces the field, but the space restriction between the conductor shield and resistive insert limits the conductor size to 6-1/8 in.

3.3.5 System Layout

In summary, the ICRH system will consist of a two-stage amplifier system with associated power supplies, protective gear, and controls, as well as a coaxial transmission and multi-stub system and a shielded strip-line antenna.

Because the neutron flux drops off several orders of magnitude on the outboard edge of the axicell, there is an advantage to bringing the coax and feedthrough to the antenna from this side, as shown in Fig. 3.3-4.

Fig. 3.3-4. ICRH system layout.

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3.4 PUMP BEAM

The MARS reactor baseline requires that 1480 A of neutral beam current (740 A per end) at 94 kV be injected from 30 deg off the axis at the 9.3-T magnetic field location. The beam profile spans the range from 7.8 to 10 T. This injection point is 85 m from the device midplane, between the axicell and transition coils. The beam line location is shown in Fig. 3.4-1. The energy, angle, and magnetic field choices are determined by the physics requirement that the charge-exchanged ions from the beam are in the local loss cone and can pass into the central cell. Because of the high power (140 MW) injected by this system, the pump beam lines must be as efficient as possible and yet they must represent reasonable technology advances in the MARS time frame. We have not yet analyzed either the beam dumps or the heat flux on the walls and other structures from charge-exchange neutrals.

3.4.1 Pump Beam System Analysis

We have investigated several options investigated for this very-high-power injection system. Positive ion source beam lines based on the existing technology can be used for the pump beam. These would require an energy recovery system for the unneutralized ions and some advances in positive ion source technology. Negative ion sources can also be used with laser photo-detachment neutralization to produce a highly efficient system. These options will be discussed in further detail in the following section.

Positive ion sources produce some fraction of the accelerated current as molecular ions. These particles dissociate during neutralization and form half- and third-energy products that can charge exchange in the plasma. The fractional energy products are then either trapped in the transition region and must be removed by additional pumping current or separated and collected before injection into the reactor. If the partial energy products are allowed to enter the plasma, then roughly 50% more current must be supplied by 90% D^+ sources to pump the transition region. This represents a large increase in the required injected power to 206 MW, and necessitates a much larger neutral beam injection system. System electrical efficiencies of up to 50% are possible if energy recovery is used.

The molecular species can be removed by magnetic mass filtering before neutralization. A large sweep magnet located behind the accelerator system

Fig. 3.4-1. MARS pump beam location.

deflects the atomic ions toward the target and allows the molecular species to be collected in the beam line. The neutralizer would have to be decoupled from the accelerator by a distance of about 0.5 to 1.0 m and additional gas provided in the duct. The beam losses in the magnet region as a result of charge exchange with the gas from the source are excessively large (20 to 30%) and cause low system efficiencies of less than 40%. The additional neutralizer gas increases the pumping requirements and enlarges the beam lines.

A new mass filtering technique recently reported by Ken Ehlers of Lawrence Berkeley Laboratory (LBL) would decrease the losses in the magnet region and has revived interest in a positive ion pump beam. The LBL mass filter basically consists of very thin rows of magnets aligned in the deceleration region of the grids to produce a transverse magnetic field. Each magnet row is shielded from the beam by the accelerator electrodes and produces a relatively short magnetic field region (4 to 6 cm) between the source and neutralizer. The use of closely spaced SmCo_5 magnets produces adequate mass separation if the beam lines are long enough. The mass filter effectively removes the partial energy beam components before injection. Ions that charge exchange with the gas from the source in the magnet region are lost. Calculated values for this beam loss range from only 2.5 to 5%, depending on the extent of the fringing fields assumed. The deflected atomic ions from the source are then neutralized in a standard gas cell of near equilibrium density. Unneutralized ions from this gas cell can be subjected to energy recovery at high efficiency because the partial energy ions have been removed and a fairly monoenergetic beam results. A schematic representation of this system is shown in Fig. 3.4-2, where the beam has been rotated 90 deg with respect to the accelerator to illustrate the mass separation. A typical positive ion beam line based on these sources is illustrated in Fig. 3.4-3.

A negative ion system with photodetachment is illustrated in Fig. 3.4-4. Electrical efficiencies of 55 to almost 70% are possible with this system because photodetachment provides 95% stripping efficiency. Electrical efficiency vs beam energy is illustrated in Fig. 3.4-5. Since laser efficiencies of more than 10% are projected, beam line electrical efficiencies of 60% are achieved at the cost of the laser hardware.

Table 3.4-1 illustrates the comparison between these four options. The negative ion systems are divided into a one-source-per-laser neutralizer and a

Fig. 3.4-2. Schematic of LBL positive ion mass filter.

Fig. 3.4-3. MARS pump beam line.

Fig. 3.4-4. Folded laser resonator with multiple mirrors.

Fig. 3.4-5. Sensitivity of the negative ion beam system efficiency to photodetachment laser efficiency.

Table 3.4-1. Comparison of pump beam options.

Ion type	System electrical efficiency (%)	Total injected power (MW)	Wall power (MW)	Minimum number of sources	Total gas flow (Torr-liter/s)	Requirements
Positive	50.5	206	404	42	945	Energy recovery
Positive sweep magnet	38	144	380	40	900	Energy recovery Good beam quality
Positive LBL mass filter	47	144	310	35	765	Energy recovery Good beam quality
Negative 1 source/laser	55 to 60	144	240	35	546	High current sources
Negative 4 sources/laser	65 to 70	144	215	35	546	Multiple sources per laser

hopeful four-sources-per-laser neutralizer. A multiple-sources-per-laser neutralizer would be difficult for the pump beam because of the injection angle geometry constraints on stacking of the sources. We are examining this problem in further detail. The negative ion and LBL mass-filtered positive-ion systems use similar numbers of sources and comparable gas flows. The positive ion system requires an additional 70 to 95 MW, the development of energy recovery, and perhaps some beam focusing by the magnetic field.

3.4.2 Ion Sources

For positive ion source systems, four beam lines (illustrated in Fig. 3.4-3) are necessary. Each source has an extraction area of 15 x 70 cm and produces an average current density of 0.12 A/cm² of deuterium ions over that area. The source configuration for continuous plasma production and confinement has not been developed, but will probably contain magnetic multipole confinement and either hollow cathode or rf plasma generation. The gas flow required for each 50% utilization efficiency source is 25.2 Torr-liter/s. The 126 A of accelerated ions consist of 90% atomic ions, of which 5% are lost because of charge exchange in the LBL magnetic mass filter region.

The negative ion sources are difficult to project at this time because this is a new technology. A system designed by Fink and Neef at LLNL would use 12 LBL self-extraction negative ion sources per beam line operating at a 50-mA/cm² current density in a slot 2.5 cm x 3 m. The long dimension would be broken into six equal segments. A system based on Brookhaven National Laboratory's (BNL) sources would consist of four source units per beam line. Each unit would use multiple slots with an accelerator area of 10 x 150 cm and an average current density of 50 mA/cm².

3.4.3 Accelerator

The positive ion source accelerator uses multiple cooled rails with an effective transparency of 40%. The accelerator produces a beam with 1.0- x 0.5-deg optics and operates continuously. The LBL filter system would incorporate magnets in the decel regions and has yet to be developed. The negative ion system accelerators are specified to produce the same optics as the positive ion sources, but we have not yet determined the configuration.

3.4.4 Neutralizer

The positive ion source systems use a conventional close-coupled gas neutralizer. The negative ion source system uses a laser resonator photo-detachment cell. Competing schemes consist of either multiple lasers spaced along the beam line axis, or a single large laser with multiple mirrors to fold the light flux down along the beam axis. Because the total flux and interaction length is specified by the 95% stripping requirements, the total power required by both systems is approximately the same. Estimates of the laser wall power produce 2 to 5 MW of input power per beam line, depending on the laser efficiency and resonator configuration assumed.

3.4.5 System Layout

The pump beam layout is illustrated in Fig. 3.4-3 and consists of four beam lines per end of the machine. The choice of positive vs negative ion sources will be made in the next few months after we complete our analyses of the geometry, efficiency, and approximate cost of each system. The higher electrical efficiency of the negative ion beam system (60%) compared to the 45 to 48% calculated for the positive ion source system is balanced by the expected higher capital cost of negative ion systems. It appears that the size of both systems is about the same because the lower negative-ion current density offsets the lower positive-ion neutralization efficiency. Table 3.4-1 compares system characteristics for various pump beam options; technology advances are required in both proposed systems and are briefly described in Table 3.4-2.

Table 3.4-2. Required technology advances.

LBL positive-ion mass filter system

1. High-current (110-A) continuously operating 100-kV positive-ion sources.
2. Very thin, cooled SmCo_5 magnets incorporated into the decel stages of the ion source.
3. Magnetic focusing of the ion beam to maintain optical quality of the beam (perhaps).
4. Energy recovery of the unneutralized atomic ions at high efficiency (70 to 80%).

Negative ion system with laser photodetachment

1. High current (40- to 50-A) continuously operating, 100-kV negative-ion sources.
 2. High power (30- to 80-kW) continuously operating COIL laser resonator.
 3. Laser photodetachment system.
-

3.5 SLOSHING ION BEAM

The sloshing ion beam lines must deliver 39.2 A (19.6 A per end) at 475 kV. The injection angle is 90 deg from the MARS axis and the injection point is 100 m from the device midplane. Because the neutralization efficiency is less than 5% for positive ions at this energy, negative ions must be used for this beam. Laser photodetachment is then the preferable method of achieving 95% stripping of the ion beam. The accelerator must produce this beam with a high electrical efficiency and must also contain a system for electron rejection.

3.5.1 Ion Source

The ion source uses a cesiated surface to convert into negative ions those positive ions formed by an rf plasma generator in a magnetic multipole bucket containment geometry. Because the accelerator (discussed in Section 3.5.2) uses circular beamlets, the cesiated surface is machined to focus the ions into two rows of 34 circular apertures located 5 cm apart. This two-dimensional focusing of the ions produces 50 mA/cm² of negative ions at each 1.5-cm-radius aperture on the first grid. This geometry produces a total current of 0.35 A per beamlet. The area of the beam is roughly 10 x 170 cm, with a majority of the space occupied by the accelerating and focusing electrodes.

The source configuration is based on the LBL self-extraction negative-ion source with some anticipated improvements in current density and gas efficiency. The cesiated surface is located roughly 15 cm from the accelerator and the electron current from the plasma is suppressed by a transverse magnetic field produced by permanent SmCo₅ magnets. The plasma generator operates continuously with an electrical efficiency of 5 kW/A of negative ion beams and a 30% gas utilization efficiency.

3.5.2 Accelerator

Several accelerator options are possibilities for the sloshing ion beam. Systems that use rf acceleration, such as the LANL-RFQ and the BNL-MEQALAC, provide high energy acceleration without using very high voltages. These systems are conceptually safer and easier to protect and maintain than d.c. systems. However, the time-averaged current density is low in the rf

system, and the electrical efficiency is lower in the rf system than in a d.c. system because of inefficiencies in joule heating and power supply modulators. The standard d.c. multiple grid accelerators used today in positive ion systems could possibly be reconfigured to produce a high-voltage negative ion beam. These very high voltages, however, can contribute to stray electron- and radiation-induced breakdowns in the accelerator column that cause unreliable operation. The stored energy in each grid must also be minimized to eliminate meltdown problems associated with the arc breakdowns. A d.c. electro-static quadrupole (ESQ) system, in which the beam is focused by electrostatic quadrupoles between accelerator stages, reduces the probability that a breakdown will occur over the entire accelerator column. Accelerator length is also adjustable by the quadrupole parameters so that reasonable voltage gradients can be achieved.

The d.c.-ESQ accelerator concept is basically a d.c.-MEQA in which the acceleration occurs between focusing stages over an extended accelerator length. If a conservative voltage gradient of 10 kV/cm is selected, the accelerator column length will range from 65 to 80 cm, depending on the space required by the quad support structures. The preliminary accelerator design consists of two rows of 34 beamlets each. Each beamlet is 1.5 cm in radius and carries 350 mA of negative ion current. The ions from the source are pre-accelerated by a standard d.c. multiple grid accelerator to 50 to 100 kV and then injected into the quad system. We estimate that losses of the negative ions as a result of charge exchange in the accelerator column are below 10%. We assume the accelerator electrical efficiency to be 85%.

3.5.3 Neutralizer

We have chosen a laser photodetachment neutralizer for this beamline because of this neutralizer's very high stripping fractions. A folded resonator with the ion beam passing through the peak laser flux produces reasonable requirements for the laser power to achieve 95% stripping. The initial design uses two resonators (one for each row of beamlets), with 4 diameter mirrors. Each system requires laser power of 70 kW and six mirrors to fold the laser resonator along the path of the ions. Assuming a 10% laser electrical efficiency (5 to 16% projected efficiency), the neutralizers

require a total of 1.4 MW of wall plug power. Focusing of the beam to a smaller size (< 3 cm) by the d.c.-ESQ accelerator will decrease the requirements on mirror size and laser power.

3.5.4 System

Figure 3.5-1 shows a schematic representation of the sloshing ion beam line. We are currently working on a detailed design of each component. Some of the parameters chosen for each beamline are indicated in Table 3.5-1. Based on these values, the total system electrical efficiency, defined as beam power on-target divided by the required electrical power, is 69.5%.

Fig. 3.5-1. Schematic of the sloshing ion beamline.

Table 3.5-1. Sloshing ion beamline parameters.

Ion source	
<u>Modified LBL self-extraction source</u>	
Electrical efficiency	5 kW/A
Gas efficiency	30%
Total current	24 A
Current density	50 mA/cm ²
Beam area	10 cm x 170 cm
Number of apertures	68 (1.5-cm radius)
Gas flow	6.75 Torr-liter/s
Neutralizer	
<u>Laser photodetachment</u>	
Stripping fraction	95%
Number of lasers	2
Laser efficiency	10%
Mirror reflectivity	0.999
Scatter/detraction losses	0.2%
Window transmission	0.998
Number of folds	5
Mirror diameter	4 cm
Laser power	70 kW
Total wall power	1.4 MW
<u>System</u>	
Accelerator efficiency	85%
Ion loss in accelerator	6%
Total collimator scrape off	5%
Duct reionization loss	5%

3.6 END-PLASMA TECHNOLOGY

The end-plasma technology area consists of a group of related but separate systems. They are grouped together because they all reside in the end vacuum tanks and are involved in handling charged-particle leakage from the central cell. The end plasma technology consists of the following systems:

- Ion converter end;
- Electron converter end;
- Halo scraper;
- Continuous cryopumps;
- Vacuum tanks and support structure.

In addition, we discuss the tritium containment and processing problems that occur in these systems.

3.6.1 Direct Converter

The direct converter consists of the ion end collector, the electron end collector, and their associated grids. The principal issue is to determine whether it is cost effective to directly convert the approximately 800 MW of charged particle energy that escapes from the central cell. It is obvious that recovering some or all of the charged particle energy is desirable. It is not as obvious, however, that direct conversion is a cost effective method of recovering the additional energy. Physics considerations (see Section 2.2-4) dictate that a small central-cell plasma potential is necessary for stability. One way to create this condition is to depress the end cell potentials several hundred keV below ground. As a result, it is obvious that we can achieve little or no cost savings by eliminating direct conversion, and so our design objectives have been to maximize the energy recovered.

The halo scrapers, although used as plasma limiters, are in fact a part of the direct conversion system. The charged particles that are removed from the plasma edge, flowing against the bias potential of the electron collector, contribute a significant portion of the total recovered energy.

The present configuration has the electron collector and entrance grid at -170 kV, the suppressor grid at -270 kV, and the ion collector at 156 kV. This configuration is expected to recover 426 MW of electrical energy and dump 408 MW of thermal energy. The baseline is designed around water-cooled AMAX-MZC copper grids and plates. The maximum water temperature obtainable with this design is about 215°C. This is not high enough quality heat to make thermal recovery efficient. We recognize that recovering a portion of this energy would be useful, and we are conducting studies of the use of higher temperature water or gas cooling. Figure 3.6-1 is a conception of the ion collector end of the reactor.

The entrance grid (1), suppressor grid (2), and ion collector plate are the major components of the ion converter which, by selective leakage, receives most of the ion flow. The overall layout of these components is shown in Fig. 3.6-1. Electrostatic shields to prevent high voltage breakdowns are not shown. Each of the components must be segmented to limit the capacitively stored energy in any one segment.

The location of these components is determined by setting the average heat load on grid 2 at 2.5 MW/m^2 . Each grid has 5% opacity and receives the heat and electrical power loads given in Section 2.9. Grid 2 is the most highly loaded, with 30.8 MW giving an area of 246.4 m^2 . From conservation of magnetic flux, the field strength in the plasma fan when it has expanded to 246.4 m^2 is 0.0079 T. EFFI calculations provide coordinates, where the field is 79 G, which define the location and shape of grid 2.

The relative geometry of grids 1 and 2 and the ion collector plate are shown in Fig. 3.6-2. Intergrid spacing is 50 cm and the distance from grid 2 to the collector plate is 100 cm. The tube-to-tube spacing of 20 cm is fixed by the 1-cm outer diameter of the tubes and the requirement for 5% opacity in the grid array. We chose the tube size (1 cm) as the largest practical size before the tube-to-tube spacing becomes too large to create a uniform field. Large-diameter tubes are more desirable because the heated length of the longest tubes is more than 4 m.

Fig. 3.6-1. MARS ion-end configuration.

Fig. 3.6-2. Grid geometry.

3.6.1.1 Grid System

Requirements. The grid tube electrical requirements were presented in Section 2.9.7. In addition, they must perform satisfactorily in the specified thermal, mechanical, and particle environments and maintain their thermal-hydraulic and mechanical integrity under all operating conditions.

The thermal load for the grid tubes is defined by the power density distribution contours shown in Fig. 3.6-3. The actual plasma footprint is not exactly an ellipse. The range of power density contours shown is between 0.625 and 7.6 MW/m². We obtained the direct converter power density contours by assuming that powerflow along a flux line is proportional to the square of the density in the central cell, and that the density falls off radially in accordance with the relationship

$$n(r) = n_c(1 - r^3),$$

where n_c is the density at the center of the cell and r is the fraction of radial distance in the central cell. By assuming flux conservation and by mapping circular cross sections in the central cell into elliptical cross sections at the direct converter, we can show that the power density ϕ is

$$\phi = \frac{20}{9} \phi_{av} f(\beta_c, 1) \frac{(1-r^3)^2}{\sqrt{1-\beta_c(1-r^3)}},$$

where

$$f(\beta_c, t) \equiv 2 \int_0^t \sqrt{1-\beta_c(1-x^3)} \, xdx,$$

ϕ_{av} is the average direct converter power density, and β_c is the central cell beta. Figure 3.6-3 shows the power density distribution for a β_c of 0.7 and a ϕ_{av} of 2.5 MW/m².

Design. The present grid design evolved from a study of grid cooling methods, configuration optimization, and material suitability considerations. We had to resolve the question of passive (radiation) vs active cooling at an early stage because of the considerable differences in system complexities for

Fig. 3.6-3. Power density distribution.

the two modes of cooling. Our study was conducted to determine the feasibility of a radiatively cooled grid system. We selected tungsten as the grid tube material because of its high temperature capability.

Figure 3.6-4 shows grid temperature as a function of power density. The two solid curves bracket the grid temperatures obtained when only half of the cylindrical tube is assumed to be radiating and when the full cylinder is radiating. Superimposed on the figure are a few limiting temperature lines, ranging between the temperature at which the material strength goes below 10% and the melting temperature. Also shown are two thermionic emission lines of 3 A/cm^2 and 80 A/cm^2 . We concluded that at a peak power density of 7.6 MW/m^2 , a radiatively cooled grid will be unacceptable.

We then performed a thermostructural analysis for active cooling. The assumptions, conditions, and design requirements are listed in Table 3.6-1. Figures 3.6-5 and 3.6-6 show grid performance as a function of grid tube outside diameter. The optimum tube diameter is about 1 cm.

With the tube outside diameter fixed at 1 cm, we investigated the following three water-cooled MZC copper grid-tube point designs:

- Design 1--Maximum grid tube temperature should not exceed 400°C .
Operation at higher than 400°C is considered marginal for the expected stress levels.
- Design 2--Coolant should not operate in boiling regime at maximum pressure of 3206 psi.
- Design 3--Coolant should have same exit temperature as collector plate coolant (215°C).

The objective of the analysis was to discover the maximum coolant outlet temperature and thermal recovery efficiency.

The analysis showed that, within the stated constraints, no significant increase in coolant outlet temperature above 215°C can be realized using a water-cooled grid system. We will have to investigate either materials other than copper alloys or gas cooling.

Fig. 3.6-4. Radiation-cooled grid performance.

Fig. 3.6-5. Grid thermostructural analysis.¹

Fig. 3.6-6. Grid thermostroctural analysis.²

Table 3.6-1. Grid structural analysis.

Assumptions and conditions

Peak power density = 7.6 MW/m^2 (mid plane)

$d = 20 \text{ cm}$

$D = 65 \text{ m}$

Coolant velocity = 30 fps

Coolant inlet: temp = 30°C

Tube material: copper alloy

Tube length = 5 m

Number of tubes = 325

Cooling medium: water

Design requirements

$d/L \approx 1/2$ ($L = 50 \text{ cm}$)

$T/d < 0.05$

The baseline grid design consists of two arrays, each with 325 tubes spaced at 20-cm intervals. The suppressor grid array is located along the intersection of the 79-G surface over a length of 64.94 m. The longitudinal axes of the individual tubes are located parallel to the MARS X-axis. The entrance grid array is located 50 cm inboard of the suppressor grid array. Each grid tube is AMAX-MZC copper 5 m long, 1 cm outside diameter, and watercooled.

Performance. We performed a thermal analysis on the baseline design to determine the grid temperature distribution and the thermal gradients through the tube walls. Using the power density distribution given in Fig. 3.6-3 and a maximum water outlet temperature of 215°C, we calculated the maximum inner and outer tube temperatures at the grid midplane. The results are shown in Fig. 3.6-7.

We conducted a similar analysis to obtain the distribution of the through-the-tube thickness thermal gradients (ΔT) shown in Fig. 3.6-8.

To determine the erosion of the grids by energetic plasma particles, we conducted a physical sputtering analysis of the entrance and suppressor grids, focusing on copper and beryllium as the possible materials. We obtained physical sputtering yields as a function of the atomic and mass numbers of the incidence and target atoms, the surface binding energy of the target materials, and the incidence angle and energy of the particles.

The peak particle fluxes and energy levels are given in Table 3.6-2. The erosion rates are given in Fig. 3.6-9 for the entrance grid, suppressor grid, and collector plate. For the baseline tube thickness of 0.159 cm, the lifetime for total erosion of a grid tube is approximately 9 yr. Further analysis will be necessary to predict erosion from other sources, such as coolant flow, and to determine minimum wall thickness before replacement is necessary.

Deformation. Because the purpose of the grids is to create potential planes, it is important that they remain in the correct place during operation. A non-planar potential field tends to turn the charged particles, which decreases their axial velocity component and reduces the collection efficiency.

Table 3.6-2. Peak particle fluxes and energies.

Flux ^a	Entrance grid energy (keV)	Repeller grid energy (keV)	Collector plate energy (keV)
$3.74 \times 10^{15} \text{ D}^+ \text{ @}$	319	429	43.5
$3.74 \times 10^{15} \text{ T}^+ \text{ @}$	319	429	43.5
$9.48 \times 10^{13} \text{ D}^+ \text{ @}$	584	694	308.5
$7.99 \times 10^{13} \text{ D}^+ \text{ @}$	236	346	---
$1.28 \times 10^{14} \text{ } \alpha^{++} \text{ @}$	739	849	463.5

^aFluxes in ions/cm²/s.

Fig. 3.6-7. Grid tube temperature distribution.

Fig. 3.6-8. Grid tube-wall temperature differences.

Fig. 3.6-9. Physical sputtering rates.

Two phenomena can lead to deformation. The first is column buckling due to thermal expansion; each tube is a long column capable of carrying only 76 N before buckling. The second is bending due to a temperature difference between the front (heated) and back sides of the tube. Any difference in temperature induces a moment in the tube, which then results in deformation of the tube and adds to the column buckling load.

Figure 3.6-10 illustrates tube-end supports that can be added to deal with buckling and bending. Linear expansion of the tube can be handled by allowing the end to slide in a fixture, by spring loading the end, or by pretensioning the tube. Solving the problem of deformation due to differential heating is more difficult. Pretensioning can reduce but never eliminate bending in the tube. For a tube with a uniform temperature difference along its length, a simple end moment equal to the induced moment will prevent bending. However, the actual temperature difference varies along the tube length as a function of flux density and coolant temperature. For such a temperature difference variation, a simple end moment cannot eliminate the bending.

We performed a detailed analysis of this problem using a 300-segment model for the tube and a realistic heating profile for our baseline water-cooled copper design. Our baseline design maintains very high cooling rates which, when coupled with the high thermal conductivity of copper, keep the maximum ΔT to 50°C. This results in the small deformations shown in Fig. 3.6-11 (0.5 cm maximum). With no pretensioning of the tube and an applied end moment, the deflection is maintained at less than 1 cm.

Gas Cooled Grids. The temperature of the grids is limited to 215°C in the baseline design. To increase the temperature to a more useful level, other materials or coolants are required. We selected helium as an alternate coolant to be studied because it has several advantages over water. As with most gaseous coolants, helium can sustain higher temperatures than water and grid tube leaks in a helium cooled system will be less catastrophic than leaks in a water cooled system.

An important consideration in the grid system design is the power required to pump the coolant. A parameter that assesses the effectiveness of a given system is the pumping power ratio (PPR), defined as the ratio of the pumping power to the power absorbed by the coolant. For the baseline grid

Fig. 3.6-10. Grid tube supports.

Fig. 3.6-11. Grid tube deformation.

tube array, the PPR has been calculated as a function of temperature and pressure. The results are shown in Fig. 3.6-12 and indicate the possibility of high frictional losses.

Alternate Grid Materials. As previously stated, the water-cooled copper grid system is temperature-limited. To remove the temperature constraint, several alternate materials were investigated for helium-cooled grids. The selection of a material must include satisfying design constraints imposed by:

- Temperature,
- Erosion (physical and chemical),
- Stress,
- Tritium permeability,
- Vaporization,
- Thermionic emission,
- Grid thermal deflection, and
- Material activation.

We must distinguish between erosion and vaporization for the following reason: The erosion constraint is associated with a limit dictated by the material, e.g. a certain minimum life expectancy of the grid. The vaporization constraint, on the other hand, is a limit dictated by the plasma.

The materials that we investigated are: copper alloy (AMAX-MZC), molybdenum alloy (TZM), niobium alloy (FS-85), and ferritic steel (HT-9). The maximum predicted temperatures for these materials are shown in Fig. 3.6-13. We concluded, on the basis of temperature alone, that both AMAX-MZC and HT-9 can be eliminated. Temperatures exceeding 400°C are considered marginal for AMAX-MZC. Similarly, the strength of HT-9 at 1165°C is rapidly approaching zero.

The next step was a stress analysis of the materials. We computed the combined (thermal plus pressure) stress levels as a function of position along the grid midplane ($z=0$). Our results are shown in Fig. 3.6-14, including the tensile yield strength (FTY) at the appropriate maximum temperature, except for HT-9. For HT-9, the 10,000-hr creep-rupture strength is plotted in

Fig. 3.6-12. Grid pumping power ratio.

Fig. 3.6-13. Effect of material on maximum grid tube temperature.

Fig. 3.6-14. Grid tube stress distribution.

addition to the applied stress. We observed that the applied stresses for HT-9 are higher than the allowable creep rupture except at the very end of the footprint, and that the strength of AMAX-MZC is very low. At the grid mid-point ($y=0, z=0$) FS-85 is also marginal. However, we would have to do a more detailed analysis to definitely rule out FS-85 as a grid material candidate. The molybdenum alloy, TZM, appears to meet the stress and thermal requirements.

Additional Issues. Several important design issues have not yet been addressed. One of the more important is chemical sputtering. For some materials, chemical sputtering may be a bigger problem than physical sputtering. We did not address the problems of sputtered material transport and redeposition and material activation. Because the basic mode of grid operation is high temperature and long duration, the issue of thermal creep needs further study.

3.6.1.2 Collector Plate

Requirements. The total thermal power to the direct converter collector plate is about 170 MW, as specified in Section 2.9.2. The plate is located according to heat load on the grids, which in this case is at an axial z position of 124 m. At this location, the area is 240 m^2 for an average power density of 0.7 MW/m^2 .

Design. We considered two types of cooled surfaces: (1) a fully cooled back and (2) a back with spaced coolant channels. We used a one-dimensional analysis to scope the maximum temperatures at the front surface of the plate.

The choice of the location for the direct converter collector plate is a compromise between a requirement for a low thermal power density to simplify the thermal design, the need for useful coolant temperature, and the physical size of the converter. The choice for the reference design was to place the converter plate at the 124-m station where the thermal power density is 69.6 W/cm^2 and the impingement area is 240 m^2 . The magnetic field is 79 G at the 124-m station on the direct converter axis. For this value of constant magnetic field, we have determined an intersection within the horizontal center plane of the direct converter. This line, the semi-major axis of the elliptical impingement area, is an arc 30 m long. For convenient installation and maintenance, we designed the collector plates as individual sector panels,

each 1.5 m wide and 6 m high. There are 40 collector sectors, 20 on each side of the center line (Fig. 3.6-15). The sections are numbered 1 through 20, starting with the center line.

We considered five coolant channel configurations, shown in Fig. 3.6-16, for the ion collector plate. Configuration A has a partially cooled back where the vertically oriented coolant channels are separated from one another. All 40 sectors have the same configuration. However, the thermal analysis showed that the maximum material temperature on sector 1, the sector closest to the axis, is greater than the melting temperature of the MZC copper. Therefore, we rejected this configuration. In configuration B the back is completely cooled by horizontal channels in a switchback arrangement. Again, all 40 sectors have the same configuration. The maximum material temperature is 245°C, which is below the maximum allowable of 400°C. However, in the single-channel horizontal switchback design, the fluid velocity causes a high fluid pressure drop and sufficiently high pumping power (>800 kW) to reject the concept.

We have considered two other horizontal switchback arrangements. Both have maximum material temperatures below 300°C and fully cooled backs. In configuration C, sectors 1 through 10 are double-channel horizontal switchbacks and sectors 11 through 20 are single-channel horizontal switchbacks. The pumping power is 267 kW. In configuration D, sectors 1 through 12 are triple channel horizontal switchbacks and sectors 13 through 20 are single channel horizontal switchbacks. The pumping power is 118 kW. The pumping power for configuration D is acceptable but the power for configuration C is not. However, we rejected both configurations because the sectors in each configuration, with a mixture of multiple and single channels, lack a common design. Configuration E has a fully cooled back with three channels in series to form a channel path and 13 channel paths per sector. This vertical switchback arrangement has a maximum material temperature of 376°C and a pumping power of 31 kW. We have selected it as the baseline.

Certain design objectives have evolved from the coolant configuration study. We selected a modular design consisting of reasonably small individual sectors because it simplifies the installation and maintenance procedure. Only one type of sector need be kept in inventory for replacement after a failure or during normal maintenance. The sector coolant configuration is

Fig. 3.6-15. Collector plate design.

Fig. 3.6-16. Coolant channel configuration.

designed with straight vertical channels and straight channel walls for ease of milling and brazing sector parts. Each sector has a single inlet and outlet, and all sectors are connected in parallel to a single inlet and outlet header. This simplifies maintenance because only two cuts and welds have to be made for sector replacement. The sector is designed with a fully cooled back to maintain the lowest material temperature. The coolant outlet temperature of all sectors is the same.

The reference design for the total collector is summarized in Table 3.6-3. There is a total of 39 vertical channels per sector. The channel cross section of 3.84 x 0.72 cm maintains a relatively high coolant velocity, ensuring that the temperature across the coolant boundary layer is low enough to keep the maximum material temperature below the allowable 400°C. Because of material degradation by high-energy particle impingement, the front wall thickness is 1 cm to allow for a reasonable material life. The coolant scheme is turbulent nonboiling flow for all sectors except 19 and 20 which have laminar flow. The sectors are in parallel between the 16-in. inlet and outlet headers, and pressure balance valving is required on each sector inlet to maintain a constant coolant outlet temperature. The sector material is AMAX MZC copper.

Performance. The overall collector plate performance can be characterized by a few key parameters. The maximum inside surface temperature at the wall occurs in section 14 and is 312°C. The pressure required to prevent boiling at this temperature is 106 atm. We calculated the pressure drop of 1.4 atm using the Fanning relation.¹ The channel length in the sector for calculating pressure drop is based on the straight length of channel path and the equivalent length of two "U" bends per channel path. The total coolant flow rate is based on a coolant temperature rise of 185°C from an inlet temperature of 30°C. The coolant pumping power of 31 kW is based on the coolant flow rate and a system pressure drop that is assumed to be double that of the sector to account for the external piping system.

The thermal performance for each of the 20 sectors is given in Table 3.6-4. The maximum material temperatures and coolant velocities are those required to maintain the outlet temperatures of all sectors at 215°C. Based on the Reynolds number, turbulent flow exists in sectors 1 through 18 and laminar flow in sectors 19 and 20.

Table 3.6-3. Collector plate design parameters.

- Power input: 167 MW
 - Surface area: 240 m²
 - Number of sectors: 40
 - Sector height: 6 m
 - Sector width: 1.5 m
 - Coolant channel arrangement
 - Back fully cooled
 - 39 vertical channels/sector
 - 3 channels in series/channel path
 - 13 channel paths/sector
 - Channel width: 3.84 cm
 - Channel depth: 0.72 cm
 - Sector front-surface plate thickness: 1 cm
 - Material: AMAX-MZC copper
 - Coolant: water
 - Coolant scheme: turbulent flow/nonboiling
 - Sectors in parallel between inlet/outlet headers
 - Inlet header: 16 in. across top
 - Outlet header: 16 in. across bottom
 - Pressure balance valving on sector inlet piping
-

Table 3.6-4. Collector plate sector thermal parameters.

Sector number	Center heat flux (W/cm ²)	Heat input (MW)	Maximum material temperature (°C)	Coolant velocity (cm/s)	Reynolds number
1	211	8.18	374	294	1.3 x 10 ⁵
2	211	8.10	375	290	
3	211	7.97	376	286	
4	207	7.77	376	276	
5	203	7.43	376	267	
6	194	7.10	373	250	
7	184	6.55	370	235	
8	170	6.00	364	216	1.0 x 10 ⁵
9	156	5.42	359	195	
10	139	4.68	354	168	
11	122	3.91	349	141	
12	101	3.14	341	113	
13	80	2.40	333	86	
14	65	1.75	333	63	4.9 x 10 ⁴
15	49	1.25	326	45	
16	35	0.86	316	31	
17	20	0.48	302	17	
18	12	0.33	280	12	
19	4.7	0.11	228	4	
20	2.4	0.03	215	1.1	

^aLaminar flow.

A parametric study of collector plate operating conditions is shown in Fig. 3.6-17 for a water inlet temperature of 30°C. The four performance variables of surface temperature, system pressure, flow rate, and pumping power are plotted against the coolant outlet temperature. By increasing coolant outlet temperature, the flow rate and pumping power decrease. The maximum material surface temperature is influenced by the combined rise in the coolant outlet temperature and the increase in the temperature drop across the film caused by decreasing the fluid velocity. The required system pressure is the sum of the pressure required to prevent boiling at the maximum fluid temperature and the pressure drop due to flowing coolant. The combined effects result in a minimum system pressure at a coolant outlet temperature of 105°C.

Design Alternatives. Preliminary work has begun on an investigation of an alternate reference design for the collector plate. A helium-cooled plate with an outlet temperature of 500°C is the objective of the alternate design. There are still 40 sectors placed on the 79-G arc at the 124-m axial station. In the alternate design, the coolant is center-fed, splits into two paths, and then flows to the coolant outlets at the top and bottom of the sector. There are 117 parallel channels per sector.

The materials that we considered for the alternate design were AMAX MZC copper, HT-9, V15Cr5Ti, FS-85 niobium, and TZM. Thermal and stress analyses showed that MZC copper and HT-9 are not suitable materials for the same reasons that caused us to reject them for use in grid cooling (discussed in Section 3.6.1.1). Figures 3.6-18, -19, and -20 present thermal stress results for V15Cr5Ti, FS-85, and TZM ion collector plates, respectively. The data are for the first channel of sector one, which is at the center of the direct converter. For these three materials, the combined stress is lower than the yield strength over the entire length of the channel, and the maximum outside metal temperature is below the allowable temperature. Therefore, any of these materials could be used, based on thermal and stress data.

3.6.1.3 Electron Collector

The electron collector plate is heated by 112 MW of charged-particle power in a plasma fan oriented vertically in the long dimension. To minimize the size of the vacuum tank that encloses this tall plasma fan, the heat flux should be made as large as possible, with a long life design that allows for

Fig. 3.6-17. Collector plate system study.

Fig. 3.6-18. Center coolant channel performance--vanadium alloy.

Fig. 3.6-19. Center coolant channel performance--niobium alloy.

Fig. 3.6-20. Center coolant channel performance--molybdenum alloy.

thermal recovery. We determined the plate location by assuming 2.5 MW/m^2 average over an area of 43.8 m^2 . The flux bundle has the profile shown in Fig. 3.6-21 with the collector at a field of 445 G. The variation of flux density across the plate is assumed to be the same as that for the ion collector. We have not yet performed a detailed analysis of the cooling for this plate.

A single-grid array is located in front of the electron collector plate to suppress secondary electron flow from the surface. This will be maintained at about 20 V negative in relation to the plate.

The halo scraper for the electron end has a different design than the scraper at the ion end even though the heat and particle loads are the same. To reduce the size of the electron end tank, the plate has been placed closer to the yin-yang, which required a modified design. The configuration shown in Fig. 3.6-21 is significantly smaller than the ion-end halo scraper; its leading edge is exposed to a heat flux of 2.5 MW/m^2 . Our preliminary estimates indicate the design is feasible; the actual analysis will be performed in FY 1983.

3.6.2 Halo Scraper

3.6.2.1 Requirements

The halo scraper removes low energy DT and alpha-particles from the unconfined region of the plasma. The halo contains about 80 MW of power that must be dissipated by the scrapers in the ion and electron ends. We have assumed the flux density in the halo plasma to be uniform so that the load at any location is the total power in the halo divided by the area of the flux bundle. We have designed the baseline halo scraper located at the ion end with the leading edge at a heat flux 1.0 MW/m^2 and at a field of 633 G. The length of the leading edge is 52.9 m while the thickness of the halo plasma varies from 0.37 to 2.39 m. As the halo thickness varies, the length of the scraper inlet duct varies, as discussed in Section 3.6.3.

3.6.2.2 Design

We analyzed the halo scraper for a water-cooled plate with both a fully cooled back and a partially cooled back with spaced coolant channels. The

Fig. 3.6-21. Electron collector end tank.

halo scraper, like the collector plate, is designed for recovery of thermal power. To facilitate this, the water-coolant outlet temperature of all channel paths is the same and is set at the same temperature as the ion collector plate coolant outlet. From the scraper z-plane projection (Fig. 3.6-22) we can see that the coolant channel length will vary with position on the scraper. Therefore, the coolant velocity, coolant film temperature drop, and outside material surface temperature will vary with position. For the partially cooled back at the minimum channel length, the outside material surface temperature is greater than the allowable material temperature of 400°C for MZC copper. Therefore, we rejected the partially cooled back design.

The cross section of the halo scraper reference design in Fig. 3.6-23 shows the copper channel wall, channel, and center beam. The halo scraper is placed at 45 deg to facilitate the impinging thermal flux to facilitate vacuum pumping of the impinging particles. The channel dimensions are 4 x 0.3 cm. An internal web provides increased structural strength. The web splits the 4-cm channel width in half. Other dimensions are given in Fig. 3.6-22. There are 1320 channels in the scraper connected in series in groups of four; therefore, there are 330 channel paths between the inlet and outlet headers. We used this design scheme to raise the channel velocity so that the film drop is minimized, keeping the material temperature below the allowable 400°C. The coolant will flow down the back face in the first and third channels in series and down the front face in the second and fourth. Both the inlet and outlet coolant headers will be adjacent to the back face. The coolant header is 20 cm.

3.6.2.3 Performance

The halo scraper performance parameters are summarized in Fig. 3.6-22. The power density at the tip, which is normal to the impinging flux, is 100 W/cm². Over the majority of the scraper area, which is at 45 deg to the flux, the power density is 71 W/cm². The coolant pumping power is 1730 W and the flow rate is 820 GPM. The coolant temperature rise is 185°C from the inlet temperature of 30°C, with a pressure of 167 atm. The coolant velocity varies between 56.6 and 161.3 cm/s with an average of 130 cm/s. Figure 3.6-22 shows the temperature of the root and tip for both the maximum and minimum

Fig. 3.6-22. Halo scraper performance.

Fig. 3.6-23. Halo scraper design.

channel lengths. Both plasma side and coolant side temperatures are given for the downstream channel in a channel path of four channels. This channel would have the highest root and tip temperatures of all the four channels.

Figure 3.6-24 contains a halo scaper parametric analysis for an inlet temperature of 30°C. Five performance parameters--maximum surface temperature, coolant pressure, flow rate, pumping power, and combined stress--are plotted against the coolant outlet temperature. The flow rate and pumping power decrease with increasing coolant outlet temperature. As the coolant outlet temperature rises, the temperature drop across the coolant film also rises. Both effects cause the maximum material surface temperature to rise. The required coolant pressure is the sum of the pressure required to prevent the coolant from boiling at the maximum fluid temperature and the pressure drop of the flowing coolant. The pressure drop falls with increasing coolant outlet temperature because the flow rate decreases. The result is that the coolant pressure is flat out to about 120°C and then rises steeply. The shape of the combined stress curve follows the shape of the pressure.

3.6.3 Continuous Cryopumping Systems

The vacuum pumping system is designed to continuously remove the gas load that passes from the reactor central cell to the end tanks. The gases are helium, deuterium, and tritium. Additional vacuum systems that are not discussed are those needed to maintain vacuum beam lines, the pumps used to bring the vessel to a pressure where the cryopumps take over, and the system used to de-gas the continuous cryopumping system.

3.6.3.1 Requirements

The continuous cryopumping system must maintain a total pressure in the reactor end tanks of 2×10^{-5} Torr during plasma burn by pumping the particle loads specified in Table 3.6-5. The charged particles pass from the central cell into the ion-end tank and the electron-end tank either into the main tank chambers or into the halo region at each tank perimeter.

3.6.3.2 Performance

We have calculated the performance of a single-stage continuous cryopumping system for the ion-end tank, electron-end tank, and halo scrapers; the

Fig. 3.6-24. Halo scraper system study.

Table 3.6-5. MARS pumping requirements.

		Throughput (Torr-liter/s at 300 K)		
	Total current (A)	Ion end	Electron end	Total both ends
Core plasma				
Deuterium fuel	432.4	42.2	--	42.2
Tritium fuel	432.4	42.2	--	42.2
Deuterium sloshing	12.6	0.62	0.62	1.24
Alphas	119.3	5.8	5.8	11.6
Total	--	90.8	6.4	--
Halo plasma				
Deuterium fuel	696.0	33.7	33.7	67.4
Tritium fuel	696.0	33.7	33.7	67.4
Alphas	278.4	13.5	13.5	27.0
Total	--	80.9	80.9	--
<u>Overall total</u>	--	171.7	87.3	259.0

results are presented in Table 3.6-6. The pump speeds are 4.5×10^6 liter/s in the ion end tank, 3.2×10^5 liter/s in the electron end tank, and 1.62×10^6 liter/s in each of the halo scrapers.

The end tank pumps are located within the tanks to maximize conductance. Therefore, the total speed is assumed to equal the speed of the pumps. The halo pumping system is placed in a plenum downstream of the halo scraper. To account for the intervening ducting, the conductance of the duct and plenum is presumed to equal the pump conductance, so that the required pump speed is double the effective speed required at the halo scraper. The halo effective speed is based on a total pressure of 10^{-4} Torr maintained in the scraper. We selected this pressure for the given plasma conditions as a compromise between the required effective speed, which varies inversely with the pressure, and the size of the scraper, which increases with the pressure (see Section 3.6.3.4).

3.6.3.3 System Description

The continuous cryopumping system is located in the ion end tank, in the electron end tank, and in plenums downstream of the halo scrapers in each of these end tanks. The system removes the helium and fuel constituents continuously while maintaining a total pressure in the end tanks of 2×10^{-5} Torr. The pumping units are sized as shown in Table 3.6-6 to meet the requirements specified for each cavity.

We chose cryopumps because of their ability to be configured for the specified high pumping speeds. Because cryopumps are by nature batch-type devices, it is necessary to include the capability for periodic regeneration of each pump while other pumps in the system are on-line. This excess capacity is included in each pumping module.

The pumping system is made up of individual modules (shown in Fig. 3.6-25) that can be removed for maintenance. The number of pumps (cryopumping units) in each module is derived in Table 3.6-7, with a 5-m^2 pumping panel (two sides at 2.0×1.25 m each) selected for commonality. The system has a 20% excess pump capacity, which, when combined with a short regeneration time, results in a maximum of only 67 g of tritium being held up in the pumps. The tritium throughput is 3.04 kg/day, based on 15% burnup; deuterium throughput is 2.05 kg/day. In addition, 712 g/day of helium must be pumped.

Table 3.6-6. Cryopump sizing.

		Throughput (Torr-liter/s)	Total pressure (Torr)	Partial pressure (Torr)	Total speed (liter/s)	Specific pumping speed (liter/s-cm ²)	Pumping surface area (10 ⁴ cm ²)	Pump speed (liter/s)
Direct converter	DT	85.0	2×10^{-5}	1.87×10^{-5}	4.5×10^6	7.9	57	
Tank pumps	Helium	5.8	2×10^{-5}	1.28×10^{-6}	4.5×10^6	6.5	69	
Electron end	Deuterium	0.6	2×10^{-5}	1.88×10^{-6}	3.2×10^5	7.9	4.1	
Tank pumps	Helium	5.8	2×10^{-5}	1.81×10^{-5}	3.2×10^5	6.5	4.9	
Halo pumps (each end)	DT	67.4	10^{-4}	8.33×10^{-5}	8.1×10^5	7.9	21	1.62×10^6
	Helium	13.5	10^{-4}	1.66×10^{-5}	8.1×10^5	6.5	25	1.62×10^6

^aAssume pump speed equals conductance of pump duct system.

Table 3.6-7. Cryopump installation.

Location	Required pump area (m ²)	Area per unit (m ²) ^a	Actual unit area (m ²)	Number of pumping units	Units per module ^b	Number of modules	Excess capacity (%)	Total pump area (m ²)	Tritium through- put (g/day)	Tritium holdup (g)
D.C. tank	69	-	75	15	6	3	20	90	1170	25.8
Electron end tank	5	5	5	1	2	1	100	10	0	0
Halo (D.C. end)	25	-	25	5	6	1	20	30	934	20.6
Halo (electron end)	25	-	25	5	6	1	20	30	934	20.6

^aEach unit is double-sided, 2 m x 1.25 m per side.

^bIncludes one unit off-line for regeneration.

Fig. 3.6-25. Continuous cryopump module.

The system consists of a total of 32 cryopumps; 26 are pumping during plasma burn while six (one per module) are being regenerated. Helium gas is cryotrapped on a double-sided, liquid-helium-cooled panel using an argon gas spray; the hydrogen isotopes are condensed on the same surfaces. The panel sides are shielded thermally by liquid-nitrogen-cooled chevrons, which allow passage of the pumped gases from the reactor to the pumping panels. The cryopumps are arranged in an in-line housing so that one pair of baffles can be deployed to de-gas each pump sequentially as is shown in Fig. 3.6-25. The entire assembly is mounted on a vacuum tank cover for removal during maintenance. Valves for cryogen supply and vent, as well as for purge gas control during regeneration, are mounted in an evacuated, shielded valve box on the tank cover.

An individual pump is regenerated by covering it with the regeneration baffles and purging the liquid helium from the pumping panel. The degassed particles are captured by a collector pump for subsequent recycling.

3.6.3.4 Design Options

We evaluated alternative cryopump and halo scraper configurations before selecting the baseline cryopumping system.

Specific Pumping Speed of Alternative Cryopump Concepts. Sizing of MARS cryopumps for the continuous flow of helium, deuterium, and tritium requires that the specific pumping speed S_p be determined for each gas and each configuration of interest. The S_p for an ideal pump is

$$S_{p(\text{ideal})} = 3.638 \left(\frac{T}{M} \right)^{1/2} (\text{Torr-liter/s-cm}^2),$$

where T is the temperature (K) of the pumped particles with molecular weight M . For a real installation, $S_p = k S_{p(\text{ideal})}$, where k is a complex function of the pump geometry.

A helium and hydrogen cryopump basically consists of a pumping surface or surfaces separated from the source gases by intervening impedances such as chevrons and louvers, that intercept radiated energy and condensable gases. The expression for a single-impedence pump² is

$$S_{p1} = 3.638 \left(\frac{T}{M} \right)^{1/2} \left(\frac{1}{\tau} + \frac{1}{c} - 1 \right)^{-1},$$

where t is the transmissivity of the impedance and c is the capture coefficient of the pumping surface. Expanding the expression for multiple impedences and that assuming the gas temperature becomes that of the impedance as it passes through, the expression for specific pumping speed of a two-impedence unit is

$$S_{p2} = 3.638 \left(\frac{T}{M}\right)^{1/2} \left[\frac{1}{t_1} + \left(\frac{T}{T_1}\right)^{1/2} \left(\frac{1}{t_2} - 1\right) + \left(\frac{T}{T_2}\right)^{1/2} \left(\frac{1}{c} - 1\right) \right]^{-1}, \quad (3.6-1)$$

where the subscripts 1 and 2 refer to the first and second impedance, respectively.

Alternative Cryopump Concepts. We evaluated three pump concepts (Table 3.6-8) to determine the best design for MARS. Two of these are compound pumps, each with two impedences; the third is a single-stage single-impedence pump. Compound pumps have helium and hydrogen isotopes pumped on separate surfaces. The hydrogen is condensed on the second impedance and the helium passes through to be pumped on the final surface. In the single-stage pump, helium and hydrogen are pumped on one surface. In both the compound- and single-stage pumps, the first impedance is an opaque thermal shield.

The compound pump concept has the advantage of separating the helium and hydrogen isotopes, thus avoiding the need for a helium separator in the fuel cleanup system. However, this is gained at the expense of helium pumping speed, which is diminished by the extra surface needed for pumping hydrogen, and at the expense of a more complex regeneration and gas system. The reduced pumping speed can be recovered somewhat by making the first impedance less restrictive, for example by using a louver instead of a chevron, but the pumping speed gain is offset by increased heat load to the helium refrigeration system as is indicated in Table 3.6-9.

The single-stage pump concept features improved helium pumping speed and consequently smaller pumps. However, helium must be separated in the fuel cleanup system; Los Alamos is currently developing this capability as an alternative for TSTA (Tritium System Test Assembly).

We evaluated the effect of the three alternative pump configurations on pumping speed using Eq. (3.6-1). For hydrogen and helium pumping speeds in the single-stage concept, $t_2 = 1$ because there is no second impedance. Trans-

Table 3.6-8. Pump configuration.

Type	Configuration									Specific pumping speed (liter/s ⁻¹ cm ⁻²)			Remarks	
	First panel			Second panel			Last panel			Helium	Hydrogen isotopes	K ^a		
	Function	Style	Temp (K)	Function	Style	Temp (K)	Function	Style	Temp (K)					
Compound	A	Thermal shield	Chevron	77	Hydrogen isotope pumping	Chevron	4.2	Helium pumping	Trapping surface	4.2	3.5	7.9	0.11	Separation of hydrogen isotopes and helium
	B	Thermal shield	Louver	77	Hydrogen isotope pumping	Chevron	4.2	Helium pumping	Trapping surface	4.2	4.3	12.7	0.14	Complex regeneration scheme No helium in fuel cleanup system
Single stage (selected baseline)	C	Thermal shield	Chevron	77		None		Helium and hydrogen isotope pumping	Trapping surface	4.2	6.5	7.9	0.21	Improved helium pumping speed Simplified pump design Helium separator in fuel cleanup system Trapping-gas separator in fuel cleanup system

$$K = \frac{\text{Helium pumping speed}}{\text{Helium ideal pumping speed}}$$

Table 3.6-9. Electric power requirements for pump options.

Configuration	Heat to 4.2-k surface (W/cm ²)	Pumping system	Total pump speed (liter/s ⁻¹)	Specific He pumping speed (liter/s ⁻¹ cm ⁻²)	Pumping surface area (10 ⁴ cm ²)	Heat load (W at 4.2 K)	Thermal load (W at 4.2 K)	Gas enthalpy (W at 4.2 K)	Total load (W at 4.2 K)	Electric power at 500:1 (kW)
A. Compound (chevron-chevron)	2.0 x 10 ⁻⁵	Direct converter	4.5 x 10 ⁶	3.5	129	25.8	46	3	49	25
	2.0 x 10 ⁻⁵	Electron end	3.2 x 10 ⁵	3.5	9.1	1.8	46	3	49	25
	2.0 x 10 ⁻⁵	Halo (each)	1.6 x 10 ⁶	3.5	46	9.2	46	3	49	25
B. Compound (lower-chevron)	1.8 x 10 ⁻³	Direct converter	4.5 x 10 ⁶	4.3	105	1890	3355	3	3358	1679
	1.8 x 10 ⁻³	Electron end	3.2 x 10 ⁵	4.3	7.4	133	3355	3	3358	1679
	1.8 x 10 ⁻³	Halo (each)	1.6 x 10 ⁶	4.3	37	666	3355	3	3358	1679
C. Single stage	2.0 x 10 ⁻⁵	Direct converter	4.5 x 10 ⁶	6.5	69	13.8	24	34	58	29
	2.0 x 10 ⁻⁵	Electron end	3.2 x 10 ⁵	6.5	4.9	1.0	24	34	58	29
	2.0 x 10 ⁻⁵	Halo (each)	1.6 x 10 ⁶	6.5	25	5.0	24	34	58	29

missivities are 0.28 for a chevron and 0.45 for a louver. To compare alternatives, the capture coefficient c for helium in the compound pump and for hydrogen is taken as unity. However, a more conservative value of $c = 0.6$ is assumed for helium pumping in the single-stage pump where the gas mixture is pumped on a single surface.

We selected the single-stage cryopump design because of its simplified design, higher pumping speed, and, thus, smaller-sized units.

Halo Scraper Configurations. The halo scraper system, shown schematically in Fig. 3.6-26, neutralizes particles in the halo plasma so they can be removed by the continuous cryopumping system. Ionized particles entering the scraper are neutralized at the elbow formed by the scraper and the vacuum pumping duct. Their path depends on the conductance of these two passages. Neutral particles reentering the scraper can be reionized or can return to the plasma. Reionization probabilities have been based on STARFIRE data. The fraction escaping from the scraper, because of its symmetry, is based on an assumption that 50% of the neutrals reentering the scraper escape if the scraper length is zero. This percentage rapidly decreases as the scraper length-to-height ratio is increased.

The performance and design parameters for the halo scraper in the ion-end tank are tabulated in Table 3.6-10. The inlet area and plasma flux are based on the halo plasma profile defined in Section 3.6.2. The results show that the halo scraper has to be 8.8 m long for the required throughput.

We evaluated the practicality of reducing pumping speed to a range where turbomolecular (TM) pumps could be used. For this study, we held the inlet width constant and allowed the height to vary as a function of the plasma flux density. Pump speed reduces inversely with halo pressure. However, for a fixed-plasma particle and energy flux, the halo inlet conductance must be reduced (increased length) to maintain the particle balance within the halo scraper as the halo pressure is increased. To reduce pump speed while maintaining a scraper size comparable to the baseline, the plasma density and heat flux must increase. We selected two plasma conditions for comparing TM and cryopumps. The results are shown in Fig. 3.6-27 and Table 3.6-11, which present the changes required in plasma conditions to reduce halo pump speed while maintaining the scraper length at 8.8 m. We will evaluate the consequences of the low pump speed option in FY 1983.

Table 3.6-10. Ion-end halo scraper parameters.

Helium-pumped (Torr-liter/s)	13.5
DT-pumped (Torr-liter/s)	67.4
Halo pressure (Torr)	10^{-4}
Halo plasma inlet molecular flux ($\text{cm}^{-2}\text{-s}^{-1}$)	1.2×10^{16}
Halo plasma power (MW)	40.15
Heat flux (MW/m^2)	1
Halo inlet area (m^2)	40.2
Halo inlet height, averaged (cm)	100
Halo inlet length for 1-m height (m)	8.9
Pump speed (liter/s)	1.62×10^6

Table 3.6-11. Alternative pump parameters.

	<u>Baseline</u>	<u>Low pump speed option</u>
Scraper length (m)	8.8	8.8
Inlet height (cm)	100	27
Molecular flux ($\text{cm}^{-2}\text{-s}^{-1}$)	1.2×10^{16}	4.5×10^{16}
Plasma power (W/cm^2)	100	375
Halo pressure (Torr)	10^{-4}	3.3×10^{-3}
Pump speed (liter/s)	1.6×10^6	4.9×10^4

Fig. 3.6-26. Halo scraper model.

Fig. 3.6-27. Halo pump parameters.

The particle flow to the electron-end halo is identical to the ion-end halo flow; however, the profile is not yet defined. Therefore, its parameters are to be determined.

3.6.3.5 Helium Refrigeration Load

We have completed a preliminary estimate of the helium refrigeration load for the continuous cryopumping system. The load is principally that required to support the regeneration function and to offset heat leak from the distribution system.

Each of the 32 cryopumps is regenerated every 48 min. During regeneration the LHe-cooled pumping surface is allowed to warm to 18 K by purging the coolant with warm helium gas. The helium refrigeration recools the panel to 4.2 K for operation. The refrigeration load for regeneration depends mainly on the control of purge temperatures. Use of ambient helium purge gas requires 24.4 W of cooling per pump (778 W total). If a lower purge gas temperature is used--125 K assumed--this requirement drops to 9.2 W per pump (295 W total), which is the value used in the baseline. The additional load for cooling the metal panel after regeneration is small (49 W total) because of the panel's low specific heat at cryogenic temperatures. The heat loss from the helium distribution system is given in Table 3.6-12.

The total helium refrigeration load, which includes energy extracted from the pumped gas and energy radiated to the pumping surfaces, is summarized in Table 3.6-13.

3.6.4 Vacuum Tanks and Support Structure

The end tanks for MARS will be different from each other in many ways. The two biggest differences are in size and orientation. The end tank for the ion end of the machine is significantly larger than the end tank for the electron end because the high heat load on grid 2 dictates its large area. The plasma fans are rotated 90 deg with respect to one another for plasma stability regions. Thus, while the large ion-end tank lies flat, the electron-end tank fans out vertically. In both cases, the tanks must be designed considering remote maintenance of the plasma direct converter components.

Table 3.6-12. Helium distribution system heat loss.

Item	Quantity	Heat loss (W)
Supply valve	38	67
Return valves	38	76
Fill bayonets	6	12
Return bayonets	6	12
Field joints	12	28
Fill lines	250	53
Return lines	250	<u>75</u>
Total		323

Table 3.6-13. Total helium heat load (W).

Pumped gas energy	34
Radiation to pumping surfaces	32
Distribution system	323
Regeneration	344
Contingency	<u>73</u>
Total	805 W

3.6.4.1 Ion-End Tank

The halo scraper, cryopump modules, and ion converter components require a vacuum tank with overall dimensions of 32.5 x 60 x 10 m, as shown in Fig. 3.6-1. Several possible schemes were considered for supporting the vacuum loads introduced on such a tank before deciding upon the configuration shown in Figs. 3.6-28 and 3.6-29.

The concepts considered but rejected were: a stiffened flat plate design (similar to the TFTR neutral beam boxes); a flat plate stiffened by large reinforced concrete beams; and a suspension-type construction. The idea of bridging through the plasma fan with compression members to most directly react the vacuum loads was not considered because of the plasma-wall interaction problems and the resulting loss in efficiency from blocking particle flow.

A reinforced concrete barrel shell enclosure is the design we chose to support the vacuum vessel. As Figs. 3.6-28 and 3.6-29 show, the end tank vacuum boundary is determined by flat plates that approximate the shape of the direct converter components. The flat plates are stiffened and supported by beams on 3-m centers that are in turn supported from the barrel shell. On the upper surface, this support is via cables, while on the lower side tie rods are used since both gravity and vacuum loads must be reacted.

The total estimated cost for this structure is \$55 million, which includes \$50 million for the stainless steel vacuum tank and \$5 million for the concrete barrel shell. A less expensive option may be to use a magnetic material for the vacuum vessel, but this will require additional study of the effect on the plasma fan.

The components in the end tank are remotely maintained through access ports in the top of the vacuum vessel. These ports are located between the stiffening beams, which are spaced at 3-m intervals. Each cryopump module is the size of one port, and the grid structures and ion collector plate can be reduced to 1.5-m segments for replacement. Service cranes run in the direction of the axis of the barrel shell between the service ports and the service bay area adjacent to the end tank.

Fig. 3.6-28 Barrel shell vessel support.

Fig. 3.6-29 Barrel shell support structure.

3.6.4.2 Electron-End Tank

The electron-end tank and supporting structure are shown in Fig. 3.6-30. The vacuum tank is supported by a concrete barrel shell structure similar to the structure on the ion end, except that the plasma fan expands vertically. Access to the tank for remote maintenance must be through the top because the clearance between the barrel shell and tank wall is not sufficient. The tank itself has overall dimensions of 15 x 31 x 6 m, approximately half the size of the ion-end tank. It is made from 1.9-cm-thick stainless steel plates stiffened by back-to-back channels at 3-m spacing. The vacuum loads are transferred to the barrel shell structure through tie-rods connected to the channels.

3.6.5 Tritium Considerations

3.6.5.1 Tritium Permeation into Cooling Water

In the ion converter, high-energy tritium ions (40 to 400 keV) strike the collector plate and grid array assemblies to enable the plasma energy to be recovered in the form of electrical and thermal energy. In addition, the halo plasma containing tritium impinges on the halo scraper. The energetic impingement of tritium ions on these surfaces will result in a steady-state tritium loss to the water coolant as well as in a tritium inventory in the walls. This steady-state tritium loss to the coolant poses a potential environmental hazard because of subsequent tritiated water leakage from the cooling system. In addition, the tritium inventory must be kept low enough to prevent material failure due to hydrogen embrittlement. To estimate the extent of these problems, we applied a simple model of tritium diffusion and holdup, which takes into account the recombinative properties of the surface and the depth of the plasma implant, to the surfaces of interest. The model is based on the recommended approach of Baskes,³ which has been successfully used to investigate reemission of implanted deuterium from stainless steel walls, thermal absorption, and nuclear reactor profiling of deuterium near surface concentrations. The analysis, however, has not been verified experimentally for permeation implantation problems. Furthermore, in our analysis, we had to rely solely upon analytic estimates of the recombination rate coefficients and implant depth profiles for energetic tritium/copper interaction because we could not find any experimental data.

Fig. 3.6-30 Electron-end tank and support structure.

3.6.5.2 Tritium Diffusion Model

The analytic model for tritium diffusion is briefly described, sample calculated results are presented, and methods for reducing the permeation loss are suggested for further investigation. More details of the analysis, along with a sensitivity analysis of predicted loss and holdup to uncertainties in the recombination rate coefficient and implant depth parameters, may be found in Ref. 4.

We estimated the permeation and inventory in the ion collector plate, entrance and suppressor grid arrays, and halo scraper using solutions of the steady-state one-dimensional Fick's law, with the following assumptions:

1. A monoenergetic DT plasma interacting with a clean, semi-infinite one-dimensional, homogenous Cu slab with a linear temperature profile across the slab.
2. Source term due to implanted tritium only.
3. The release of tritium from the external surfaces requires recombination of the tritium atoms; i.e., second-order desorption is the dominant mechanism.
4. No trapping, i.e., no hydrides are formed.

Figure 3.6-31 illustrates the salient features of the analytic model that can be expressed with the following four equations:

$$D(T) \frac{\partial c}{\partial x} = \text{CONST} = J, \quad (3.6-2)$$

$$\phi = D(T_1)[c^* - c(-\delta)]/\delta \text{ at } x = 0 \text{ for } v_B \ll v_\delta, \quad (3.6-3)$$

$$K_r(T_1)c^2(-\delta) = D(T_1)[c^* - c(-\delta)]/\delta \text{ at } x = -\delta, \text{ and} \quad (3.6-4)$$

$$K_r(T_2)c^2(L) = D(T_2) \left. \frac{\partial c}{\partial x} \right|_{x=L}, \quad (3.6-5)$$

where the recombination rate coefficient $K_r(T)$ was estimated using the model developed in Ref. 5 assuming a sticking probability of one; the implant depth for tritium in copper was obtained from Ref. 6.

Fig. 3.6-31. Tritium diversion model.

From the solution of Eq. (3.6-2) we obtain the concentration profile in the wall

$$\begin{aligned}\tilde{c}(\xi) &= 1 + J' \int_0^\xi \frac{d\xi'}{(3.95 \times 10^{-2}) \exp\left(\frac{-10,250}{R[T_1 + (T_2 - T_1)\xi']}\right)}, \quad 0 < \xi < 1 \\ &= 1 + J'I(\xi),\end{aligned}\quad (3.6-6)$$

and the wall inventory

$$INV = A_s L c^* \int_0^1 c(\xi') d\xi',$$

where a linear temperature profile is assumed in the wall, $T = T_1 + (T_2 - T_1)\xi$, $\xi = x/L$, $c = c(x)/c^*$, and $J' = JL/c^*$.

From Eqs. (3.6-3) and (3.6-4), we obtain expressions for the implant concentration c^* and the upstream wall concentration $c(-\delta)$, such that

$$c^* = c(-\delta) + \phi\delta/D(T_1) = \sqrt{\phi/K(T_1)} + \phi\delta/D(T_1).$$

From Eq. (3.6-6) and the boundary condition at $x = L$ given by Eq. 3.6-5,

$$J' = -1/I(1).$$

The permeation to the coolant is then

$$P = -A_s D(T_2) \left. \frac{\partial c}{\partial x} \right|_{x=L} = -A_s J = A_s c^*/LI(1). \quad (3.6-7)$$

The integral $I(1)$ was solved numerically for the cases investigated with the assumed linear temperature gradient. For $T_1 = T_2 = \text{const} = T$, $I(1) = 1/D(T)$ and $P = A_s Dc^*/L$, the correct result for $c(L) = 0$. For all the cases we have investigated to date, $c(L) \sim 0$ because the ratio of the recombination rate flux to the diffusion rate flux is very large and the permeation is diffusion-rate limited; i.e., Eq. (3.6-7) is only correct for $K(T_2)C^*L/D(T_2) \gg 1$. If the rate of tritium removal at the coolant-wall interface is limiting (e.g., by an oxide coating formed by H_2O/Cu reaction), the wall loading will increase

and the permeation will decrease. For the cases we have considered, we obtained conservative estimates of the permeation and underestimates of the wall inventory.

3.6.5.3 Results

Table 3.6-14 summarizes our estimates of the tritium permeation to the coolant and the wall inventory for the ion collector plate, the entrance and suppressor grid arrays, and the halo scraper. The principal input parameters specified in the calculations are also shown in the table. The total tritium loss and wall inventory obtained represent roughly 0.013% of the incident flux. The peak implant concentration observed was 1.4 appm (centerline suppressor grid tube) and does not present material failure problems from hydrogen embrittlement.

In the analysis, we assumed that a constant tritium flux and surface temperature was appropriate for the current halo scraper design. For the ion collector plate, the tritium flux variation along the major axis of the elongated elliptical plasma footprint was approximated by average values over each of the 6-m² sectors (Table 3.6-15). Note that there are approximately seven grid tubes per sector for each grid array. We did not include detailed wall temperature gradients in the present analysis of these designs because the temperature gradients are too small to significantly reduce permeation and holdup. For example, two cases are shown in Table 3.6-14 for the grid arrays: (1) a constant temperature for all tubes in the array and (2) a case where each tube has a different constant temperature corresponding to its local heat load. These results differ by only 20% in the permeation and by 5 to 8% in the inventory. We obtained similar results for the collector plate with and without temperature gradients.

Temperature gradients can significantly reduce the permeation and holdup.⁴ However, for copper walls with the coolant wall temperature maintained at a minimum of 200°C and a maximum allowable surface temperature of 500°C, a factor of 10 reduction in permeation and inventory would be the most optimistic result. The effectiveness of the temperature gradient in reducing permeation and holdup is primarily caused by the decreased implant concentration level with increasing temperature in the implant region. On the other hand, the increased uncertainty of the predictions due to the uncertainty in the

Table 3.6-14. Tritium permeation (all surfaces constructed of AMAX-MZC material).

Surface	Thickness (cm)	Total area (m ²)	Tritium energy (keV)	Tritium flux (cm ² -s)	Implant depth (μ m)	Plasma-wall temperature (°C)	Coolant-wall temperature (°C)	Permeation (mg/day)	Inventory (mg)
Converter collector plate	1	240	45	Varied ^a	0.3	225	225	33	142
Entrance grid array	0.16	11.2	371	Varied ^a	2.04	{ 276 Varied ^a	{ 276 Varied ^a	{ 65 54	{ 2.0 1.9
Repeller grid array	0.16	11.2	471	Varied ^a	2.6	{ 276 Varied ^a	{ 276 Varied ^a	{ 83 68	{ 2.6 2.4
Halo scraper	0.6	103	52.5	1.06×10^{15}	0.24 ^b	250	250	19	18

^aSee Table 3.6-15.

^bSurface is inclined at 45 deg relative to the incoming tritium stream.

Table 3.6-15. Tritium flux and temperature profiles.

Sector number	Flux $\times 10^{15}$ (no./cm ² -s)	Plasma-wall temperature (°C)	Coolant-wall temperature (°C)
1	1.86	378	340
2	1.86	376	338
3	1.86	374	337
4	1.82	370	336
5	1.79	367	334
6	1.71	362	330
7	1.62	356	326
8	1.50	348	320
9	1.38	334	306
10	1.23	314	287
11	1.08	288	266
12	0.89	262	246
13	0.71	242	232
14	0.57	226	218
15	0.43	214	208
16	0.31	206	200
17	0.18	200	200
18	0.11	200	200
19	0.042	200	200
20	0.021	200	200

recombination rate coefficient (dirty surface effects) with increasing plasma-wall surface temperature could easily offset the anticipated reduction in permeation.⁴

3.6.5.4 Permeation Barriers

The permeation rate can be significantly lowered by using a barrier layer, cladding, or coating on the coolant wall surface. Oxide ceramics such as Al_2O_3 , BeO , and MgO would prove very effective in reducing tritium permeation by several orders of magnitude and can be formed for this application. For example, at 400°C , $D_{\text{Al}_2\text{O}_3}^{\text{H}} = 5 \times 10^{-16} \text{ cm}^2/\text{s}$ and $D_{\text{Cu}}^{\text{H}} = 5.2 \times 10^{-5} \text{ cm}^2/\text{s}$.⁶ Therefore, very thin coatings should produce substantial reductions in permeation. To apply this approach, a detailed analysis of the tritium build-up at the ceramic-metal interface would be needed in addition to a reevaluation of the thermal design. Ceramic liners have the advantage of reducing tritium contamination to the coolant to such a low level that tritium recovery may not be required. However, the increased costs of such a procedure would need to be balanced against the actual costs of tritium recovery without a barrier. A tradeoff study is recommended before any final engineering decisions are made on the need for tritium barriers.

The tritium permeation rate could also be reduced by employing low Z coatings such as C and TiC on the plasma-wall surface. Coatings of this type have been investigated in a number of implantation studies which have shown that the implanted hydrogen concentration saturates at a relatively low level.³

The next phase of our analysis will consider these possibilities for permeation in the current design. We will also investigate the tritium problems associated with higher-temperature helium-cooled direct converter designs.

3.6.5.5 Tritium Extraction From Cooling Water

We investigated tritium permeation into the reactor water coolants. The direct-converter ion-collector coolant water will receive the largest tritium contamination of $\sim 750 \text{ Ci/day}$. The water supply to this area will be kept separate and tritium will be recovered from the water when the tritium concentration reaches a specified level, at which time a slip stream will be removed

and the tritium recovered at the permeation rate.

The parameters anticipated for the MARS water recovery unit (WRU) are a tritium permeation rate and subsequent recovery rate of 1000 Ci/day, a concentration limit in water of 1 Ci/liter, a 1% slip stream, and a slip stream flow rate of 10^3 liter/day.

We anticipate that the concentration level of 1 Ci/day can be handled safely with respect to personnel exposure and environmental release. The CANDU reactor systems presently handle concentrations of 24 Ci/liter in moderator D_2O and 0.65 Ci/liter in the reactor coolant D_2O . The maximum recommended concentration level for the FED/INTOR water coolant is also 1 Ci/liter.

Several methods can be used to recover tritium from water. Many of these methods are in developmental stages, and choosing an optimum WRU for a future reactor is difficult. Following are several options considered for the MARS WRU.

LPCE. Liquid phase catalytic exchange is favored for its simplicity and operation at low temperature and pressure, and also because the tritium concentration as HTO is kept below the feed level. In addition, tritium can be removed at low concentrations and T_2 is produced in the exchange reaction. This process is presently in developmental stages, but it appears promising for future reactors.

VPCE. Vapor phase catalytic exchange is more complex than LPCE because it requires repeated evaporation and condensation cycles and higher operation temperatures. However, the technology for the process is available now. A \$58 million VPCE process is being installed for operation in 1985 at the Ontario Hydro Pickering site. Considerable industrial experience will exist for this process.

CECE. Combined electrolysis and catalytic exchange consists of a front end exchange column using a hydrophobic catalyst that enriches the HTO concentration. The enriched HTO is electrolyzed and hydrogen species are cryogenically distilled. This process is in development and should be available for future reactors.

Water Distillation Front End. We have investigated low pressure distillation of H_2O/HTO as a possible front end process. The distillation is

based on the relative volatilities of the H_2O/HTO system presented in Table 3.6-16.⁷ The design of the distillation system is summarized in Table 3.6-17. From the parameters listed, water distillation appears to be a simple and economical front end process for enrichment of HTO .

Table 3.6-16. Relative volatility for H_2O/HTO .

Temperature (°C)	Relative volatility (α)	System pressure (mmHg)
40	1.19	55
55	1.14	118
76	1.12	296

Table 3.6-17. Distillation column design for H_2 /HTO separation.

<u>Specified quantities</u>	
Feed HTO (Ci/liter)	1
Overhead HTO	0.1
Bottom HTO	100
Feed rate (liter/min)	0.77
Overhead rate (liter/min)	0.763
Bottom rate (liter/hr)	0.415
Relative volatility	1.14
Tower operating pressure (mbar)	150
Reflux ratio	2 x minimum
<u>Calculated results</u>	
Theoretical trays in rectifying section	27
Theoretical trays in stripping section	39
Reboiler power (MW)	0.4
Reflux ratio	12.3
Tower dimensions: ⁸	
Diameter (m)	0.5 m
Height (m)	17 m
Cost of distillation system ⁹	\$600,000

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3.7 CRYOGENICS

We have performed a preliminary analysis of MARS cryogenic requirements. The subsystems needing refrigeration are: the superconducting magnets, vacuum system cryopanel, beamlines, and tritium processing equipment. These requirements are summarized in Table 3.7-1. Boiloff gas from refrigeration is returned to the cryopant at a low temperature (~ 5 K). The liquid requirement, given in liter/hr, refers to helium that will be returned to the cryopant at approximately room temperature. Thus, the two requirements are very different. The MARS magnet cryogenic requirements were obtained through analysis of baseline design. Each of the systems that contribute to the cryopant load will be discussed separately.

Table 3.7-1. MARS cryopant heat load summary.

System	LHe I (kW @ 4.4 K)	LHe I (liter/hr)	LHe II (W @ 1.8 K)	LN ₂ (kW @ 80 K)
<u>Magnets</u>				
Central cell (48)	22.4	1,344		468
Axicells		108	3,700	48
Anchor cell pair (2)	0.48	64		61
Transition pair (2)	0.26	42		30
<u>Magnet totals</u>	23.2	1,558	3,700	608
Cryopanel	0.82			12
Miscellaneous	7.85			17
<u>MARS total cryogenic requirements</u>	30.8	1,600	3,700	638

3.7.1 Central Cell Magnets

The central-cell magnet requirements are comprised of six components listed in Table 3.7-2.

Table 3.7-2. Central cell magnet requirements with LHe cryogen.

Component	Load (kW @ 4.4 K)	Basis
Conduction	210	Analysis
Radiation	58	Scaled from MFTF-B
Instrumentation	25	Scaled from MFTF-B
Fluid lines	12	Scaled from MFTF-B
Splice heating	12	Scaled from MFTF-B
Neutronic	<u>150</u>	Scaled from axicell loads
Total	467	

This results in 22.4 kW of refrigeration at 4.4 K for the 48 central cell coils. The liquid helium (LHe) requirement for the power leads is estimated at 1344 liter/hr based on 0.35 liter/hr per 100 A. The liquid nitrogen heat load (LN₂) required by the thermal radiation shields is calculated to be 468 kW.

3.7.2 Axicell Magnets

The axicell magnet requirements were scaled from central cell loads (since no support/shield designs are complete), except for neutron heating loads, which were obtained from neutronics analyses presented in Section 3.1. The LHe II requirements for each end are given in Table 3.7-3.

Table 3.7-3. Requirements for end-cell magnets with LHe II cryogen.

Component	Load	
	(W @ 1.8 K)	Basis
Conduction	870	Scaled from central cells
Radiation	111	Scaled from MFTF-B
Instrumentation	69	Scaled from MFTF-B
Fluid lines	138	Scaled from MFTF-B
Splice heating	163	Scaled from MFTF-B
Neutronic	<u>500</u>	Analysis
Total	1850	

3.7.3 Yin-Yang and Transition Magnets

The yin-yang (anchor cell) and transition coil cryogenic requirements were obtained from MFTF-B requirements^{1,2} because the MARS anchor cells are similar. These requirements are given in Table 3.7-4.

Table 3.7-4. Yin-yang and transition coil requirements with LHe cryogen.

Anchor cell pair		Transition coils	
Component	Load	Component	Load
	(W @ 4.4 K)		(W @ 4.4 K)
Conduction	87	Conduction	143
Radiation	22	Instrumentation and radiation	43
Instrumentation	63	Fluid lines	2
Fluid lines	25	Splice heating	28
Splice heating	<u>42</u>	Neutronic	<u>46</u>
Totals	239		262

3.7.4 Vacuum System

Cryogenic requirements for the vacuum cryopanel system (without allowance for neutron heating) are given in Table 3.7-5.

Table 3.7-5. Cryogenic requirements for the vacuum cryopanel system.

LHe		LN ₂	
Component	Load (W @ 4.4 K)	Component	Load (W @ 4.4 K)
Sensible heat	34	Radiation on chevrons	5.6
Radiation/panels	32	Radiation on piping	1.4
Radiation/piping	5	Distribution	4.3
Distribution lines	323	10% margin	1.1
Regenerative operation	350		
10% margin	<u>74</u>		<u> </u>
Totals	818		12.4

3.7.5 Additional Systems

Other subsystems that will require cryogenic refrigeration include: tritium processing, main beamlines, and additional distribution. However, we have not assessed the requirements and loads for these subsystems to date. We have estimated these loads based on a use fraction rate obtained from the TDF cryoplant study. The miscellaneous loads were found to account for approximately 29 and 2.8% of major LHe and LN₂ heat loads, respectively. Thus, the estimated loads for LHe and LN₂ systems are 7850 W at 4.4 K and 17.5 kW at 80 K, respectively.

3.7.6 Cost Estimate

We estimated the capital and operating costs for a large cryoplant and have established incremental capital and oprating costs in \$/W or \$/kW-hr.

For an LHe system, the incremental capital cost is \$1330/W of heat removed at 4.4 K, and the incremental operating cost is \$37.50/kW-hr of heat removed per hour. For an LN₂ system, the incremental capital cost is \$15.90/W of heat removed at 80 K, and the incremental operating cost is \$0.75/kW-hr of heat removed per hour.

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4. POWER SYSTEMS

4.1 WASTE MANAGEMENT CONSIDERATIONS

To date we have devoted only a modest effort to the generic issue of waste management in fusion power systems. Now that we have developed several blanket concepts, we will be concentrating on more specific problems of waste management. The lithium-lead and high-temperature blankets use nonflammable coolants and have low tritium inventories, which greatly reduces the severity of possible accidents and mitigates handling of tritium in waste disposal. During FY 1982 we also assessed the problem of radioactive waste generated by neutron activation of structural materials. We found that by carefully selecting structural materials we will be able to make reactors with radioactive components that can be disposed of by near-surface burial. This would greatly simplify waste processing and handling, including disposal both during life of plant and after decommissioning. To realize near-surface disposal, it may be necessary to modify current structural steels to meet both the waste performance goal and other performance requirements.

In our assessment we assumed that fusion waste will be disposed of under the same institutional conditions required for fission waste disposal. Currently, no regulations are prepared specifically for fusion waste. However, future regulations can be expected to depend on waste form, isotopic composition, and levels of activity, without regard to the primary source of the waste (i.e., fission or fusion). Therefore, we examined the problem of assuming that it will be regulated by NRC Rule 10CFR61, "Licensing Requirements for Land Disposal of Radioactive Waste." This regulation assumes that the waste will be under administrative control for 100 yr. In this time period, the activities of isotopes with half lives less than 5 yr will have decayed to insignificant levels from any possible initial activity. Therefore, isotopes with half lives less than 5 yr pose no hazards for long-term waste disposal.

However, isotopes with half lives greater than 5 yr can be hazardous after 100 yr, depending on the waste form, quantity, and half life. The isotopes of direct concern to fusion waste treated specifically in NRC Rule 10CFR61 are tritium, C-14, Ni-59, Ni-63, Co-60, Nb-94 and Tc-99. Several other long-lived isotopes may be subjected to direct regulation. Table 4.1-1 contains a list of long-lived isotopes that may be produced in a fusion reactor.

Table 4.1-1. Long half-life isotopes (> 5 yr) potentially important for near-surface disposal of fusion activation wastes.

Element	Natural isotope mass number (% abundance)	Long half-life isotope	Half-life (yr)
Beryllium	9 (100)	Be-10	2.7×10^6
Carbon	12 (98.892) 13 (1.108)	C-14	5730
Aluminum	27 (100)	Al-26	7.4×10^5
Silicon	28 (92.18) 29 (4.71) 30 (3.12)	Si-32	~ 700
Calcium	40 (96.97) 42 (0.64) 43 (0.145)	Ca-41	8×10^4
Titanium	46 (7.99) 47 (7.32) 48 (73.99) 49 (5.46) 50 (5.25)	Ti-44	47
Manganese	55 (100)	Mn-53	2×10^6
Iron	54 (5.84) 56 (91.68) 57 (2.17) 58 (0.31)	Fe-60	3×10^5
Nickel	58 (67.76) 60 (26.16) 61 (1.25) 64 (1.16)	Ni-59 Ni-63	8×10^4 92
Strontium	84 (0.56) 86 (9.86) 87 (7.02) 88 (82.56)	Sr-90	28
Zirconium	90 (51.46) 91 (11.23) 92 (17.11) 94 (17.4) 96 (2.8)	Zr-93	1.5×10^6

Table 4.1-1. Continued.

Element	Natural isotope mass number (% abundance)	Long half-life isotope	Half-life (yr)
Niobium	93 (100)	Nb-92 Nb-93 m Nb-94	3.2×10^7 13.60 2×10^4
Molybdenum	92 (15.86) 94 (9.12) 95 (15.7) 96 (16.5) 97 (9.45) 98 (23.75) 100 (9.62)	Mo-93	3500
Technicium	None	Tc-97	2.6×10^6 1.5×10^6 2.12×10^5
Palladium	102 (0.96) 104 (10.97) 105 (22.2) 106 (27.3) 108 (26.7) 110 (11.8)	Pd-107	7×10^6
Silver	107 (51.35) 109 (48.65)	Ag-108	> 5 (127 yr)
Cadmium	106 (1.22) 108 (0.88) 110 (12.39) 111 (12.75) 112 (24.07) 113 (12.26) 114 (28.86) 116 (7.58)	Cd-113 m	13.6

Because reactor materials will be composed of a very wide range of elements, an index system, called the Waste Disposal Rating (WDR), serves as a guide to designers in evaluating activated alloys and other materials with respect to their classification for disposal as waste. The WDR assigns an index number to each material, which is given by

$$WDR = \sum_i \frac{A(i)}{L(i)},$$

where $A(i)$ is the activity in Ci/m^3 of isotope i and $L(i)$ is the maximum allowable activity of isotope i as specified by whatever regulation is in force. This definition was suggested by the rule in 10CFR61 that governs how allowable concentrations of isotopes in mixtures are determined. Presumably, if $WDR > 1$, the material cannot be disposed of by near-surface burial.

The WDR gives designers quantitative guides as to the actions needed to meet the goal of near-surface disposal. For example, if materials with $WDR > 1$ are used to enhance performance, then the procedures necessary to process this waste to put it in a form suitable for near-surface burial must be examined. The magnitude of the WDR directly gives the volume to which a unit volume of waste must be diluted with inert material to make $WDR < 1$. The individual terms in the WDR can guide the evaluation of possible chemical processing to partition the waste into batches so that most of the bulk volume could be disposed directly by near-surface burial and only a small volume would have to be disposed of by other methods (e.g., deep geologic burial) or by dilution for near-surface burial.

If new materials with $WDR < 1$ are selected to produce a reactor that inherently meets the performance goal of near-surface disposal for long-term waste, then the properties of the new materials that affect reliability, lifetime of components, etc., must be critically examined. These new materials might be developed to meet $WDR < 1$ by elemental substitutions in existing alloys to eliminate the elements causing long-lived isotopes (e.g. substituting vanadium for molybdenum). This would undoubtedly shift significant burdens to developers of materials and system designers to make trade-offs related to the mechanical, chemical, and magnetic properties of the new materials. In particular, the system designer should make certain that optimizing long-term waste disposal does not create severe short-term

activation problems in the case of accidents or cause a significant increase in maintenance difficulty.

Another approach to producing a reactor that inherently meets the performance goal for long-term waste would be to use existing materials modified by tailoring the isotopic composition but retaining the original elemental composition. Presumably this would not change the known mechanical, chemical, and magnetic properties of the alloys. The absence of natural isotopes that result in long-lived isotopes through activation/decay chains would ensure $WDR < 1$. For example, if the molybdenum content of an alloy was only Mo-96, the long-lived activation products (Mo-93, Nb-92, Nb-94, Tc-97, Tc-98, and Tc-99) would be generated in such quantities that the waste disposal rating for molybdenum alone would be small enough to allow near-surface disposal. This approach would require little change in present systems designs based on existing materials. However, research would be required to develop isotopic tailoring processes for new or modified materials.

From a systems design view, it must be remembered that during reactor operation and for a time after shutdown, the radiation hazards are mainly produced by Fe-55 ($t_{1/2} = 2.7$ yr), Mn-54 ($t_{1/2} = 303$ days), Mn-56 ($t_{1/2} = 2.8$ hrs), Co-60 ($t_{1/2} = 5.3$ yr), and a few other short half-lived isotopes in iron alloys. Therefore, the in-plant radiation hazards governing waste handling and processing procedures for a fusion reactor using iron alloys will be essentially the same regardless of the strategy for long-term waste performance of the iron alloys. However, the waste performance strategy and the resulting WDR will have significant effects on long-term disposal. For example, if $WDR < 1$, one could consider using designs with throw-away components that might be safely stored for the life of the plant and then permanently disposed of by decommissioning rules. These decommissioning rules may be considerably different from ordinary waste disposal rules if $WDR < 1$.

We have prepared two examples of k_{eff} applied to conceptual first-wall and first-shield region designs.¹ These designs are for WITAMIR² and the MARS high temperature blanket discussed in Section 4.3. The relatively more compact structure of the MARS central cell uses a thicker breeder section with less reflector and shield. Designs for WITAMIR and MARS also differ in the postulated fast neutron loading on the first wall. WITAMIR is designed to

have a neutron loading of 2.4 MW/m^2 with a planned first wall/blanket replacement time of 5 yr. The comparable numbers for MARS are 4.8 MW/m^2 and 3 yr. For this study, we assumed that in both cases the shield would last for 30 yr. Based on neutronic calculations, these wall loadings correspond to the neutron fluxes presented in Table 4.1-2. The limiting activities used in the examples are shown in Table 4.1-3.

Table 4.1-2. Neutron flux assumptions.

Design	Neutron Flux ^a ($10^{18} \text{ n/m}^2\text{-s}$)	
	First wall	Shield
WITAMIR		
0 to 1 MeV	26.6	1.9
1 to 15 MeV	3.5	0.005
MARS high temperature		
0 to 1 MeV	30.9	0.2
1 to 15 MeV	9.9	0.0023

^aNeutron fluxes are taken in the first calculational zone of the first wall and in the first calculational zone of the shield.

Table 4.1-3. Concentrations of radioactive wastes allowable for near-surface disposal.

Isotope	Maximum concentrations (Ci/m ³)	
	Fission ^a (10CFR61)	Fusion (MARS)
Any with half life < 5 yr	TMSA ^b	TMSA
H-3	TMSA	TMSA
C-14	0.8	8
Mn-53 ^c	--	22
Ni-59	2.2	22
Co-60	TMSA	TMSA
Ni-63	70	700
Zr-93 ^c	--	0.02
Mo-93 ^c	--	22
Nb-94	0.002	0.02
Sr-90	700	7000
Tc-99	0.3	3

^aBased on Table i, Federal Register, Vol. 46, No. 142, p. 38097. For isotopes contained in metals, metal alloys, or permanently fixed on metals as contamination, the values given in this column may be increased by a factor of 10.

^bTheoretical maximum specific activity (TMSA): the activity that would result if all the elements were converted to this radioactive daughter.

^cNot explicitly covered in the proposed 10CRF61. The limit given is an estimate based on the closest analogy in terms of isotope half life, type of radiation, and decay energy to an isotope explicitly covered in 10CRF61.

Table 4.1-4 contains the natural elements commonly found in steels that generate long-lived radioactive daughters. Although we have examined all of these radioactive daughters only a few of them appear to occur at levels which significantly affect long-term waste disposal. These are Ni-59, Ni-63, Nb-94, Mo-93, Tc-99, and Mn-53.

Table 4.1-5 gives the composition of several steels that may be candidates for structural materials in fusion reactors. The weight percentages of the five elements that affect WDR are specified. These few alloying elements produce radioactive daughters that exceed the limits proposed in 10CFR61 for class C near-surface disposal. The other elements in these alloys have daughters that contribute to high radioactivity immediately after shutdown of a fusion reactor; however, the half lives are short enough for the radioactivity to decay after 20 to 30 yr to levels not significant for long-term waste disposal.

Table 4.1-4. Long-lived radioactive daughters from natural elements in steel alloys.

Parent element	Daughter isotope	Daughter half-life (yr)
C	C-14	5,700
N	C-14	5,700
Al	Al-26	740,000
Cr	Mn-53	1,900,000
Mn	Mn-53	1,900,000
Fe	Mn-53	1,900,000
Co	Ni-59	80,000
Ni	Ni-59	80,000
	Ni-63	92
Cu	Ni-63	92
Zr	Sr-90	28
	Zr-93	1,500,000
Nb	Zr-93	1,500,000
	Nb-94	20,000
Mo	Nb-94	20,000
	Mo-93	3,600
	Tc-99	210,000

Table 4.1-5. Critical natural elements in various steels.

Alloy ^a	Weight Percent					
	Fe	Mn	Ni	Nb	Mo	Other ^b
SS316	64.44	1.43	14	--	2.8	17.33
PCA	64.68	1.8	16	0.03	2.0	15.49
HT-9	85.1	0.55	0.5	--	1.0	12.85
2.25-1	94.58	0.45	--	--	1.0	3.97
Tenelon	68.0	14.5	--	--	--	17.5
Nomagne	79.35	15.0	2.2	--	--	3.45
NM-1	76.92	20.0	--	--	--	3.08
25-5-1	66.22	26.0	1.2	0.07	--	6.51
32-7	60.24	31.6	0.23	--	--	7.93
JUS289-N	72.45	20.0	--	--	--	7.55
JUS289-V	72.15	20.0	--	--	--	7.85

^aNomagne is made by Kobe Steel, NM-1 by Nippon Korean, 25-5-1 by Nippon Steel Ltd., and 32-7, JUS289-N, and JUS289-V by Japan Steel Works. Tenelon is a steel developed by the U.S. Steel Corp. We have no information on these steels regarding radiation damage in high-flux neutron fields.

^bNot critical for long-term waste disposal.

Tables 4.1-6 through 4.1-9 show the calculated specific waste disposal ratings for the steels in Table 4.1-5. Both the WITAMIR and MARS first walls are nominally HT-9; the shields are austenitic manganese steels. These tables show the effect of material replacement on WDR. We have not included changes in the design to accomodate these materials.

Of the steels examined, only Tenelon, NM-1, JUS289-N, and JUS289-V can be used in the first walls and shields of WITAMIR or MARS with the resulting induced radioactivity low enough so that the waste can be disposed of directly by near-surface burial. The 2.25 Cr/1 Mo steel can be disposed of directly when used in the shields but not in the first walls. The elements that cause problems in the various steels are nickel, molybdenum, and niobium.

Thus, the above results suggest that only two major natural elements--nickel and molybdenum--prevent near-surface disposal of the activated steels according to the NRC's proposed 10CFR61. Cobalt, copper, niobium, and zirconium could also prevent near-surface disposal, but they can generally be avoided completely in the structural alloys. The natural isotopes of nickel and molybdenum that are particularly troublesome are Ni-58, Ni-60, Ni-62, Ni-64, Mo-92, Mo-94, Mo-95, Mo-98 and Mo-100.

There are three alternatives for selecting structural materials that produce radioactive wastes that can be disposed of solely by near-surface burial. Two of these alternatives are elemental tailoring (eliminating offending elements in the steels) and isotopic tailoring (reducing the relative amounts of the specific natural isotopes that cause unacceptable activities for near-surface burial). The third alternative is to use nonferrous alloys with short-lived activation products that are acceptable for near-surface burial. We have not considered this last alternative; however, the methods of analysis could be extended to nonferrous materials without modification to determine their waste disposal ratings.

Table 4.1-6. WITAMIR first wall (parent elements in parentheses).

Alloy	WDR						Alloy total
	Ni-59	Ni-63	Nb-94	Mo-93	Tc-99	Mn-53	
SS316	26(Ni)	69(Ni)	0.5(Mo)	16(Mo)	2.7(Mo)	0.21(Fe)	114
PCA	29(Ni)	77(Ni)	1800(Nb)	12(Mo)	2.0(Mo)	0.21(Fe)	1920
HT-9	0.91(Ni)	2.4(Ni)	0.15(Mo)	5.9(Mo)	1.0(Mo)	0.27(Fe)	11
2.25-1	--	--	0.15(Mo)	5.9(Mo)	1.0(Mo)	0.31(Fe)	7.4
Tenelon	--	--	--	--	--	0.23(Fe/Mn)	0.23
Nomagne	3.6(Ni)	11(Ni)	--	--	--	0.26(Fe/Mn)	15
NM-1	--	--	--	--	--	0.26(Fe/Mn)	0.26
25-5-1	2(Ni)	5.7(Ni)	4200(Nb)	--	--	0.23(Fe/Mn)	4210
32-7	0.39(Ni)	1.1(Ni)	--	--	--	0.21(Fe/Mn)	1.7
JUS289-N	--	--	--	--	--	0.24(Fe/Mn)	0.24
JUS289-V	--	--	--	--	--	0.24(Fe/Mn)	0.24

Table 4.1-7. MARS first wall WDR (parent elements in parentheses).

Alloy	WDR						Alloy total
	Ni-59	Ni-63	Nb-94	Mo-93	Tc-99	Mn-53	
SS316	23.0(Ni)	71.0(Ni)	1.5(Mo)	26.0(Mo)	3.7(Mo)	0.33(Fe)	130
PCA	26.0(Ni)	83.0(Ni)	1400(Nb)	18.0(Mo)	2.7(Mo)	0.33(Fe)	1500
HT-9	0.82(Ni)	2.6(Ni)	0.5(Mo)	9.1(Mo)	1.3(Mo)	0.42(Fe)	15
2.25-1	--	--	0.5(Mo)	9.1(Mo)	1.3(Mo)	0.45(Fe)	11
Tenelon	--	--	--	--	--	0.37(Fe/Mn)	0.37
Nomagne	3.6(Ni)	11.0(Ni)	-	--	--	0.42(Fe/Mn)	15
NM-1	--	--	--	--	--	0.42(Fe/Mn)	0.42
25-5-1	2.0(Ni)	6.1(Ni)	3200(Nb)	9.1E-5	--	0.37(Fe/Mn)	3200
32.7	0.36(Ni)	1.2(Ni)	--	--	--	0.35(Fe/Mn)	1.9
JUS289-N	--	--	--	--	--	0.43(Fe/Mn)	0.43
JUS289-V	--	--	--	--	--	0.43(Fe/Mn)	0.43

Table 4.1-8. WITAMIR shield WDR (parent elements in parentheses).

Alloy	WDR						Alloy total
	Ni-59	Ni-63	Nb-94	Mo-93	Tc-99	Mn-53	
SS316	18.0(Ni)	100.0(Ni)	5E-5(Mo)	4.6E-2(Mo)	1E-2(Mo)	2E-3(Fe)	1.8
PCA	21.0(Ni)	114.0(Ni)	1200(Nb)	3.2E-2(Mo)	6.7E-3(Mo)	2E-3(Fe)	1300
HT-9	0.64(Ni)	3.6(Ni)	2E-5(Mo)	1.8E-2(Mo)	3.3E-3(Mo)	3E-3(Fe)	4.7
2.25-1	--	--	2E-5(Mo)	1.8E-2(Mo)	3.3E-3(Mo)	3E-3(Fe)	~ 0
Tenelon	--	--	--	--	--	2E-3(Fe)	~ 0
Nomagne	2.7(Ni)	14.0(Ni)	--	--	--	3E-3(Fe)	17
NM-1	--	--	--	--	--	2E-3(Fe)	~ 0
25-5-1	1.5(Ni)	8.6(Ni)	2800(Nb)	--	--	2E-3(Fe)	2800
32-7	0.27(Ni)	1.7(Ni)	--	--	--	2E-3(Fe)	2
JUS289-N	--	--	--	--	--	2E-3(Fe)	~ 0
JUS289-V	--	--	--	--	--	2E-3(Fe)	~ 0

Table 4.1-9. MARS shield WDR (parent elements in parentheses).

Alloy	WDR						Alloy total
	Ni-59	Ni-63	Nb-94	Mo-93	Tc-99	Mn-53	
SS316	6.36(Ni)	26.0(Ni)	1E-4(Mo)	1.4E-2(Mo)	2E-3(Mo)	7.3E-5(Fe)	32
PCA	7.3(Ni)	29.0(Ni)	300(Nb)	9.1E-3(Mo)	1.32-3(Mo)	7.3E-5(Fe)	34C
HT-9	0.23(Ni)	0.91(Ni)	3E-5(Mo)	4.6E-3(Mo)	6.7E-4(Mo)	9.6E-5(Fe)	1.1
2.25-1	--	--	3E-5(Mo)	4.6E-3(Mo)	6.7E-4(Mo)	7.3E-5(Fe)	~ 0
Tenelon	--	--	--	--	--	8.6E-5(Fe)	~ 0
Nomagne	0.91(Ni)	4.0(Ni)	-	-	-	8.6E-5(Fe)	5
NM-1	--	--	--	--	--	7.3E-5(Fe)	~ 0
25-5-1	0.46(Ni)	2.1(Ni)	650(Nb)	--	--	7.3E-5(Fe)	652
32-7	9.1E-2(Ni)	0.43(Ni)	--	--	--	6.3E-5(Fe)	0.5
JUS289-N	--	--	--	--	--	8.2E-5(Fe)	~ 0
JUS289-V	--	--	--	--	--	8.2E-5(Fe)	~ 0

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4.2 LITHIUM-LEAD BLANKET

4.2.1 Blanket Design

4.2.1.1 Design Summary

Objectives. The main objective of the lithium-lead blanket is to produce electricity in the most cost effective manner. (This design differs from the high temperature blanket described in Section 4.3, which has a dual application to provide high temperature heat consistent with synthetic fuel production or cost effective electricity.) We established the following design criteria at the start of this study to guide our work.

Capital Costs. One of our primary objectives was to minimize cost of the product. We found that we could do this best by maximizing the blanket multiplication factor M , while at the same time achieving an adequate tritium breeding ratio (e.g., > 1.1). Costs are also minimized by having a high thermal conversion efficiency η . In fact, the final figure of merit ($\$/kW_e$) strongly depends on the product of $M\eta$, which we maximized.

Ease of Fabrication. Many of the past designs for fusion power reactors required such precise manufacturing tolerances and so many complicated welds that the credibility of the blanket design could be legitimately questioned. Our objective in the MARS lithium-lead blanket design was to work closely with manufacturing experts to ensure that the blanket could be fabricated in present facilities with current, well-understood techniques. The present design satisfies these requirements.

Maintenance. The MARS lithium-lead blanket design should take maximum advantage of the linear geometry of the mirror. To that end, we determined that the following criteria should be met:

1. Small, light blanket modules.
2. Minimum number of coolant connections.
3. Maximum use of remote decoupling of coolant connections.
4. No vacuum seals between modules to be replaced.
5. No movement of superconducting coils to remove damaged modules.
6. "Hands on" maintenance at the bulk of the shield within 2 days.

The present design incorporates all of the above features.

Radioactive Waste. Our objective was to reduce both the level of long term activity and the mass of radioactive waste to be handled. Because MARS has such a high neutron wall loading (hence, short-lived blanket components), we needed to choose a material that is highly resistant to radiation damage and also contains the fewest possible elements that yield long-lived isotopes. We feel that HT-9 satisfies this requirement.

It is apparent that we could not meet all of the objectives simultaneously, but we have made considerable progress in this area and expect even more progress in FY 1983.

General Features of MARS Lithium-Lead Blanket. Figure 4.2-1 shows a vertical cross section of the central cell containing the $\text{Li}_{17}\text{Pb}_{83}$ blanket modules; Fig. 4.2-2 shows the horizontal cross section. Table 4.2-1 lists the main features of the blanket design.

The basic design of the blanket is quite simple. The $\text{Li}_{17}\text{Pb}_{83}$ alloy (LiPb) is fed at 350°C to the HT-9 blanket module through a large coolant header at the top. From there, it flows through (in parallel) the rectangular HT-9 beam structure and the 10-cm-diameter circular HT-9 tube section. The tubes in the front ensure uniform flow in the regions where the neutron heating is the highest, and the rectangular sections are better suited to attain a low void fraction to increase M. At 500°C , the LiPb exits through a large header at the bottom of the blanket and finally through a large-diameter coolant pipe to a double-walled heat exchanger. The large pipes are necessary to reduce the MHD pumping power losses.

The central-cell neutron-produced power is $3892 \text{ MW}_{\text{th}}$. This energy is deposited in a 38-cm-thick blanket module and a 43-cm-thick reflector. It is important to note that the blanket modules are separated from the reflector region by a 2-cm gap so that they can be removed without interference from the reflector and shield region. The latter two water-cooled regions are made from water-cooled austenitic steel because of the lower temperature and because the neutron damage rate is lower in those areas.

The tritium breeding ratio is 1.13 from the LiPb which is enriched to 90% Li-6. An important feature of the LiPb system is its low tritium solubility, only 5 g in the entire blanket and heat transport system. There is actually only 1 g of tritium in the blanket modules at any one time, and the rest is in the heat transport system.

Fig. 4.2-1. Vertical cross section of the central cell for the LiPb blanket.

Fig. 4.2-2. Horizontal cross section of the LiPb blanket.

Table 4.2-1. Major LiPb blanket parameters.

Central cell power (MW_{th})	3892
Structural material	HT-9
Blanket coolant and breeder	$\text{Li}_{17}\text{Pb}_{83}$
First wall radius (cm)	59
Neutron wall loading (MW/m^2)	5
Blanket/reflector multiplication (M)	1.39
Tritium breeding ratio	1.13
Blanket T_2 inventory (kg)	0.005
Net conversion efficiency (%)	38.6

Finally, the net conversion efficiency, including the pumping power for the LiPb, is 38.6%. This is possible because of the high outlet temperature (500°C) and the use of a double-walled heat exchanger to eliminate an intermediate loop.

4.2.1.2 Structural Design

The LiPb blanket consists of two distinct zones:

1. The front zone, which is composed of two rows of closely packed tubes.
2. The back zone, which consists of a single row of hollow rectangular beams that provide the structural support for the blanket.

Figure 4.2-1 shows that both zones are attached to top and bottom manifolds. The plasma is surrounded by two rows of tubes that are bent semi-circularly. The end of each tube is attached to a smaller diameter tube by means of a conical transition section. A small diameter tube is then attached to the manifold. The tubes are arranged so that the structural support in the back is concealed from the direct line of sight of plasma neutrons. The

rectangular beams behind the tube banks are 19 cm deep. Each beam has four square passages. Table 4.2-2 contains the pertinent mechanical parameters of the blanket.

Each blanket module has one coolant inlet pipe header at the top and one outlet pipe header at the bottom. The molten breeding material, $\text{Li}_{17}\text{Pb}_{83}$, comes in through the top header, distributes axially along the top manifold, flows through tubes and beams perpendicularly to the magnetic field lines, converges in the bottom manifold, and finally exits through the bottom pipe header.

The cylindrical blanket modules are enclosed in a 43-cm-thick reflector covered by a 41-cm-thick shield. The total length of the central cell, which contains 84 blanket modules of identical axial width, is 151.68 m. The blanket module is supported on rails attached to the reflector structure. A 2-cm-thick gap is located between the outer surface of the blanket and the inner surface of the reflector to alleviate the thermal stress problems. The vacuum boundary is defined as the inner surface of the reflector. Bellows and O-rings guarantee the vacuum for the penetrations in the shield and reflector, which are required for coolant supply pipes.

The attractive features of this design are:

1. Both the blanket module configuration and fabrication are simple.
2. The coolant channel design for the blanket module and the connected pipe headers constitute an excellent drainable system.
3. Each blanket module has only two coolant connections.
4. The field perturbation is minimized by the uniform distribution of the ferromagnetic structural material HT-9.

Table 4.2-2. Mechanical parameters for the blanket module.

Structural material	HT-9
Axial length, room temperature (cm)	178.6
Axial length, operating temperature (cm)	180.57
Inner radius (cm)	59
Outer radius (cm)	98
Tube zone thickness (cm)	19.2
Beam zone thickness (cm)	19
Tube outer diameter (cm)	10.14
Tube wall thickness (cm)	0.227
Axial tube spacing (cm)	10.45
Radial tube spacing (cm)	9.05
Li ₁₇ Pb ₈₃ weight contained in a module (ton)	26
Structural material weight of a module, drained (ton)	2.7

4.2.1.3 Neutronics

The primary goal of our neutronics analysis is to choose a blanket design that yields a large energy multiplication M with an adequate tritium breeding ratio ($TBR > 1.10$). We performed calculations to determine the appropriate composition and thickness of the blanket and reflector. We used the coupled neutron-gamma continuous-energy Monte Carlo code MCNP¹ with cross section data based on the ENDF/B-V evaluation. In addition, the continuous energy treatment of MCNP avoids any energy group structure effects. Because we are interested only in integral quantities such as TBR and M , low statistical uncertainties can be achieved with a reasonable number of histories. We used 4,000 histories in the calculations resulting in fractional standard deviations less than 1% for the quantities of interest.

The liquid metal eutectic Li₁₇Pb₈₃ is the breeder and coolant in the blanket and the ferritic steel alloy HT-9 is the structural material. As we stated before, the blanket includes a front tube zone composed of several rows of closely packed tubes 10.14 cm in diameter that provide for good heat

removal where the energy deposition rates are highest. The back zone of the blanket consists of a single row of hollow rectangular beams that provide structural support for the blanket. The volumetric fraction in the blanket tube zone was selected as a result of structural and thermal hydraulics considerations.

Our results show that neutron multiplication through the $Pb(n,2n)$ reaction saturates to a value of 1.75 at a blanket thickness of ~ 30 cm. This implies that, from the point of view of neutron multiplication, the lead in the back region of the blanket is not effective. Hence, we chose a thin blanket design. However, lithium enrichment is required to achieve adequate tritium breeding in such a design. We performed neutronics calculations for a thin blanket with the lithium enriched to 90% 6Li and a thick blanket with natural lithium. We saved 49 cm (38.2 vs 87.2 cm) in the blanket thickness with the thin blanket design for the same TBR. This savings in turn reduces the central cell cost because the magnets are smaller. Furthermore, the blanket weighs less and can be easily replaced at end of life. However, the thinner blanket with the HT-9 reflector produces a smaller M than the thick blanket. M is defined as the ratio of the total recoverable energy deposited in the blanket and reflector to the fusion neutron energy of 14.1 MeV.

We have made several attempts to enhance energy multiplication while preserving the advantages of the thin blanket design. Increasing the lithium-lead content in the beam zone of the blanket was found to increase energy multiplication, and we chose a beam zone to achieve high M with adequate structural support. The tube and beam zones of the blanket are 19.2 and 19 cm thick, respectively.

Energy multiplication can be further increased by using appropriate structural material in the reflector. The reflector is water-cooled and the energy deposited in it is a part of the recoverable energy. For a thin blanket, a relatively large fraction of energy is deposited in the reflector and the overall energy multiplication is sensitive to the choice of reflector material and thickness. In our analysis we included HT-9, 316 SS, graphite, and Fe-1422. The low, long-term activation steel, Fe-1422 (14 wt% Mn, 2 wt% Ni, 2 wt% Cr and 82 wt% Fe), yielded the largest energy multiplication. This is primarily because of the large gamma production in Mn. Furthermore, β decay of ^{56}Mn , which is a product of the $^{55}Mn(n,\gamma)$ reaction,

contributes to local nuclear heating. Increasing the reflector thickness from 28 to 43 cm increased M from 1.35 to 1.39. The fraction of energy deposited in the reflector increased from 24.5 to 27.6%; the fraction of energy deposited as low grade heat in the shield decreased from 3.85 to 0.76%.

Figure 4.2-3 shows the effect of reflector thickness and material on the energy multiplication for a thin blanket that uses 90% enriched lithium and a thick blanket with natural lithium. Both blanket designs have similar TBRs. The improvement in M is clearly more pronounced for the thin blanket. Using a thick Fe-1422 reflector with the thin blanket results in M greater than that for the thick blanket. Table 4.2-3 gives the compositions and thicknesses for the different zones of the blanket and reflector chosen for the MARS LiPb blanket design. The main disadvantage of the thin blanket design is the large fraction of heat deposited in the water-cooled reflector (27.55%), which reduces overall thermal efficiency. This was taken into account in the design optimization analysis, presented in Section 4.2.3, which reveals that the thin blanket design has a better overall economic figure of merit than the thick blanket design.

Table 4.2-3. Dimensions and composition of the MARS LiPb blanket and reflector.

	<u>Thickness (cm)</u>	<u>Composition</u>
Blanket tube zone HT-9	19.2	73.7 vol % $\text{Li}_{17}\text{Pb}_{83}$, 7.1 vol %
Blanket beam zone	19	85 vol % $\text{Li}_{17}\text{Pb}_{83}$, 15 vol % HT-9
Reflector	43	95 vol % Fe-1422, 5 vol % H_2O

Fig. 4.2-3. Effect of reflector composition and thickness on energy multiplication.

Table 4.2-4 gives a summary of the tritium production results for the MARS LiPb blanket design. The overall tritium breeding ratio is 1.134. Almost all tritium is produced in the ${}^6\text{Li}(n,\alpha)\text{t}$ reaction. The nuclear heating results are summarized in Table 4.2-5. The total recoverable energy is 19.604 MeV/fusion. About 60% of the total heating comes from gammas. This is a direct result of using a metallic reflector. Based on a neutron wall loading of 5 MW/m^2 , the peak power densities in the blanket, reflector, and shield are 36.0, 3.62, and 0.10 W/cm^3 , respectively.

Table 4.2-4. Tritium production in the MARS LiPb blanket.

	${}^6\text{Li}(n,\alpha)\text{t}$	${}^7\text{Li}(n,n'\alpha)\text{t}$	<u>Total</u>
Tube zone	0.565	0.0022	0.567
Beam zone	0.567	0.0004	0.567
Total blanket	1.132	0.0026	1.134

Table 4.2-5. Nuclear heating (MeV/fusion) in the MARS LiPb blanket.

	<u>Neutron</u>	<u>Gamma</u>	<u>Total</u>
Blanket tube zone	4.488	4.657	9.145
Blanket beam zone	3.296	1.720	5.016
Reflector	0.339	5.103	5.442
Shield	0.016	0.135	0.151
Total	8.139	11.615	19.754

4.2.1.4 Thermal Hydraulics

The flow of a liquid metal perpendicular to a strong magnetic field, called Hartman flow, is laminar with a constant velocity profile and a very thin boundary layer. The heat transfer in such a system is dominated by conduction and, under such conditions, is poor because of the lack of turbulence. For a TMR, the surface heating load in the central cell is small enough that a poor heat transfer coefficient is not a major drawback. For this reason, a tube flow provides enough cooling to the first wall.

We cannot obtain an exact solution for conduction heat transfer in a coolant channel with both surface heating and nonuniform volumetric heating. However, a numerical solution is easily obtained using finite-difference techniques. The energy is derived from neutronic calculations and the coolant velocity is calculated from energy balance. With this input, the temperature profile in a coolant tube can be calculated step-by-step from an initial temperature distribution. The heat transfer calculations are summarized in Table 4.2-6.

Table 4.2-6. Thermal hydraulic parameters of the LiPb blanket.

Structure/coolant	HT-9/Li ₁₇ Pb ₈₃
Neutron wall loading (MW/m ²)	5
Surface heat flux (W/cm ²)	6
Blanket thermal power (MW)	2880
Reflector thermal power (MW)	1012
Blanket coolant inlet/outlet temperature (°C)	330/500
Structural temperature (°C)	330/550
Maximum blanket coolant velocity (m/s)	0.19
Coolant flow rate (kg/hr)	3.9x10 ⁸
Reflector coolant	H ₂ O
Reflector coolant inlet/outlet temperature (°C)	200/300
Reflector coolant velocity (m/s)	3
Reflector coolant flow rate (kg/hr)	5.1x10 ⁶

4.2.1.5 MHD Considerations

The dominant force on a conducting fluid flowing across magnetic field lines is the MHD force. The effect of the MHD force is to increase the pressure drop and retard heat transfer by suppressing turbulence. The MHD pressure drop will increase the stresses in the blanket and will increase the pumping power. Therefore, we have to calculate the effect of the MHD forces on both heat transfer and pressure drop.

The Hartman pressure gradient arises in fully developed laminar flow across a uniform transverse magnetic field. For such a flow in a cylindrical tube, in a uniform magnetic field normal to the tube, the pressure gradient is given by²

$$-\frac{dP}{dx} = \frac{\mu}{a^2} \left(\frac{H^2 \tanh H}{H - \tanh H} + \frac{H^2 C}{1 + C} \right), \quad (4.2-1)$$

in which $C = \text{wall conductance} = q_w t_w / a \sigma$,

$a = \text{radius of the tube,}$

$H = a B_{\perp} \sqrt{\sigma/\mu},$

$v = \text{bulk velocity,}$

$\mu = \text{viscosity,}$

$B_{\perp} = \text{component of the magnetic field perpendicular to the bulk velocity,}$

$\sigma = \text{electrical conductivity of the coolant,}$

$q_w = \text{electrical conductivity of the wall, and}$

$t_w = \text{wall thickness.}$

When the fluid is in a channel with a conducting wall and under large magnetic fields ($H \gg 1 + 1/C$), the second term of the equation dominates, giving

$$-\frac{dP}{dx} \approx v B_{\perp}^2 q_w t_w / [a(1 + C)].$$

For a thin-walled tube, C is on the order of 10^{-2} , or

$$-\frac{dP}{dx} \approx \frac{v B_{\perp}^2 q_w t_w}{a}.$$

This equation contains an interesting conclusion. The MHD pressure drop for fully developed laminar flow across a uniform magnetic field is independent of the properties of the flowing material. The wall thickness t_w of the feed and discharge tubes is kept small by using a 1-mm-thick insert inside the structural tube. The insert is kept separate from the main structural tube by an electrical insulation layer. The eddy current generated by the $v \times B$ effects is thereby reduced.

The calculation of MHD pressure drop is summarized in Table 4.2-7. In this table, ΔP_E is the pressure drop associated with turns and end-of-loop effects and ΔP_H is the Hartman pressure drop. The pressure distribution in the blanket is shown in Fig. 4.2-4. The total pressure drop is 1.32 MPa, which corresponds to the pumping power requirement of 40 MW with an assumed pump efficiency of 50%.

4.2.1.6 LiPb Corrosion

There is a scarcity of corrosion data between $Li_{17}Pb_{83}$ and structural materials. The only available data are for a static system,³ as shown in Figure 4.2-5. By comparing 1000- and 3000-hr tests, we see significant uncertainties in the dependence of the corrosion rate on temperature. The presence of thermal gradients in the capsules increases the corrosion rate above what it would be in an isothermal capsule. The allowable limits on corrosion are not well established; however, the levels indicated in Fig. 4.2-5 are small enough so that material thinning is not a problem. Redeposition of transported corrosion products is the more important effect. The MARS system is a dynamic system. The corrosion rate in MARS is expected to be higher than the rate shown in Fig. 4.2-5. However, there is virtually no turbulence in flow perpendicular to a strong magnetic field with very low velocity. We expect the mass transfer in the liquid side to be dominated by molecular diffusion and, therefore, will resemble a static system more than a dynamic system.

Although there are no dynamic tests of steel with $Li_{17}Pb_{83}$, we expect that $Li_{17}Pb_{83}$ will resemble pure Pb. Observation of corrosion of steel by lead will provide some indication of $Li_{17}Pb_{83}$ compatibility. The corrosion is primarily caused by straightforward dissolution by lead. In commenting on such corrosion, it is essential to be specific not only with respect to

Table 4.2-7. Summary of blanket MHD calculations.

	V (m/s)	a (m)	t _w (mm)	L (m)	B (T)	ΔP _E (MPa)	ΔP _H (MPa)	ΣΔP (MPa) ^a
Feed pipe	0.50	0.30	1	3	4.7	0.2	0.1	0.3
Feed pipe turn to supply header	0.5	0.3	1	-	4.7	0.2	-	0.2
Supply header turn to blanket tube	0.19	0.05	2	-	4.7	0.01	-	0.01
Blanket tube	0.19	0.05	2	1.88	4.7	-	0.3	0.3
Blanket tube turn to return header	0.19	0.05	2	-	4.7	0.01	-	0.01
Return header turn to discharge pipe	0.5	0.3	1	-	4.7	0.2	-	0.2
Discharge pipe	0.5	0.3	1	3	4.7	0.2	0.1	0.3

^a Total pressure drop is 1.32 MPa.

Fig. 4.2-4. Pressure in MARS blanket (static head is not included).

Fig. 4.2-5. LiPb/HT-9 corrosion data.

temperature, but also with respect to temperature gradient, coolant velocity, and coolant impurity level. At 1000°C, one source⁴ reports a rapid intergranular attack of various steels, but another source⁵ cites only a slight attack on 304 SS.

At temperatures around 600°C, however, the alloys are far more corrosion-resistant, as is shown in Table 4.2-8.⁶ For comparison, corrosion of 316 SS by lithium at 600°C is estimated to be 23 mil/yr.⁷ If the temperature is reduced to 400°C, 304 SS shows no attack after 500 hr.⁸ On the basis of these data, we conclude that at temperatures below 500°C, dissolution attack by lead is reasonably slow even for stainless steels.

Because nickel and nickel-based alloys are much more soluble than iron, it is not surprising that the austenitic stainless steels with their higher nickel content are somewhat more easily corroded. Chromium also has a higher solubility than iron, but lower than nickel. At 600°C the solubility of iron was reported⁹ as only 2.3×10^{-4} wt%; at 635°C the solubility of nickel was reported to be 0.85 wt%. For this reason we favor the use of ferritic or martensitic steels rather than the higher nickel austenitic alloys.

Finally, we found that little effort has yet been extended to further reduce the corrosion rate by the use of inhibiting techniques. The addition of trace amounts of zirconium and magnesium has been shown^{11,12} to provide complete protection of Croloy 2-1/4 over three years with temperatures up to 550°C in spite of strong thermal gradients in the test loop.

We have concluded that we can be reasonably optimistic about the use of steels as structural materials in liquid lead (and hence LiPb) up to 500°C. This initial conclusion, however, should not be used as an argument to neglect corrosion studies for these systems. The data we have used, while encouraging, are far from definitive and we heartily endorse the proposal for simple coupon tests to validate the alloys proposed for use in LiPb.

Table 4.2-8. $\text{Li}_{17}\text{Pb}_{83}$ corrosion.

Alloy	Maximum T (°C)	ΔT (°C)	Exposure (hr)	Calculated corrosion rate (mil/yr)
ASTM A106	573	111	5064	12
Croloy 2-1/4	593	111	5156	10
410 SS	655	167	1346	15

4.2.1.7 Lithium-Lead/Water Interactions

We recognize that a molten metal can interact with water to produce hydrogen and an accompanying release of mechanical energy. However, the magnitude of the interaction depends strongly on the metal composition, temperature, and mass as well as on the mode of contact between the metal and water. We have quantified the magnitude of the lithium-lead/water interaction that might occur in the blanket of the fusion power reactor, in the accompanying primary coolant loop piping, or in the metal-water steam generator. However, because some of the physical processes involved with molten metal-water interactions are not fully understood, our analysis is only preliminary and should be verified by experiments and more detailed analysis.

Some preliminary experiments have been performed at Argonne National Laboratory¹³⁻¹⁵ using lithium and lithium-lead alloys as the molten metal, which is poured into a waterpool. The results of the experiments, shown in Table 4.2-9, indicate that molten lithium-lead undergoes a qualitatively more benign reaction than lithium. Two physical processes are involved in a metal-water interaction:

1. Physical explosion (also called vapor explosion or fuel-coolant interaction), where the hotter liquid (molten metal--"fuel") mixes with the colder liquid (water--"coolant"), rapidly producing high pressure steam as the fuel is finally fragmented.

2. Chemical reaction and burning when the molten metal that mixes with the water undergoes exothermic oxidation, produces hydrogen gas, and the hydrogen gas burns (deflagration or detonation).

The results of the ANL experiments suggest that the lithium-lead alloys (in particular $\text{Li}_{17}\text{Pb}_{83}$) do not undergo as vigorous or damaging a reaction as molten lithium.

Although the reasons for such a difference are not completely known at this time, we propose three possible explanations:

1. The relative temperature rise for the lithium-lead alloy ($\text{Li}_{17}\text{Pb}_{83}$) is smaller than for molten lithium during oxidation.
2. The lower reaction temperature coupled with realistic chemical kinetics produces slower reaction rates.
3. The surface area available for reaction is probably smaller for the lithium-lead alloys.

The relative temperature rise for a $\text{Li}_{17}\text{Pb}_{83}$ -water chemical reaction is lower than the relative temperature rise for lithium-water. The thermodynamic maximum reaction zone temperature is ~ 4350 K for lithium and ~ 1750 K for $\text{Li}_{17}\text{Pb}_{83}$. The major reason is the large thermal inertia of the lithium-lead alloy.

This difference in the reaction zone temperature manifests itself in the chemical kinetics of the reaction. As the reaction zone temperature increases, the process of vapor-phase oxidation becomes more dominant. Vapor-phase oxidation occurs when the molten metal temperature increases to such an extent that the metal vapor pressure rises, the metal vapor diffuses toward the water vapor, and oxidation occurs. The kinetics of the vapor-phase reaction are much more rapid than the kinetics of liquid metal-water reaction because the protective oxide layer on the metal surface does not inhibit the vapor phase reaction. For lithium at ~ 4350 K the partial pressure is $\sim 10^5$ Torr, whereas for $\text{Li}_{17}\text{Pb}_{83}$ at 1750 K the lithium partial pressure is ~ 1 Torr. This suggests that vapor phase oxidation would be much less of a concern for $\text{Li}_{17}\text{Pb}_{83}$. In fact, if we use the data from sodium-water reactions, vapor

phase oxidation becomes dominant when the partial pressure of metal vapor rises above 100 Torr. This analogy suggests that the vapor phase combination is not a concern with $\text{Li}_{17}\text{Pb}_{83}$.

In the absence of vapor phase oxidation, the kinetic rate of reaction per unit area of a molten metal in water is controlled by the rate of steam diffusion to the metal surface and the rate of oxygen diffusion through the oxide surface layer into the liquid fuel phase. Although the kinetics of the lithium-lead alloy reaction have not been measured, we believe that the rate of reaction would be lower than the rate of reaction with pure lithium for two reasons. First, the chemical activity of $\text{Li}_{17}\text{Pb}_{83}$ is much lower than lithium. Second, the rate of reaction would be reduced because the lithium would be more quickly depleted near the surface of the metal for lithium-lead because of its low mole fraction.

Finally, the total rate of oxidation is the product of the kinetic rate per unit area and the total molten metal surface area. The rate of mixing and, therefore, the rate of fuel surface area generation is proportional to the square root of the ratio of coolant density to fuel density. Thus, the higher density of $\text{Li}_{17}\text{Pb}_{83}$ causes slower mixing and slower area generation. This implies that less surface area is available for chemical reactions. Experiments should be performed to verify these results.

4.2.1.8 Emergency Cooling

Loss of Flow and Loss of Coolant Accidents. The temperature responses of the first wall due to loss of flow accident (LOFA) and loss of coolant accident (LOCA) are calculated and shown in Fig. 4.2-6. The rates of the first wall temperature rises are very similar in the two cases. This unexpected result is caused by the much higher volumetric heat capacity for steel ($\rho c_p = 3.6 \text{ J/cm}^3\text{-}^\circ\text{C}$) than for the $\text{Li}_{17}\text{Pb}_{83}$ ($\rho c_p = 1.5 \text{ J/cm}^3\text{-}^\circ\text{C}$). Therefore, in the event of a LOFA, the temperature rise of the $\text{Li}_{17}\text{Pb}_{83}$ is actually faster than that of the structure, although the volumetric heating rate is less. The coolant will heat up the first wall and, consequently, the first wall is heated up faster than in the event of a LOCA. As the time reaches $\sim 20 \text{ s}$, the $\text{Li}_{17}\text{Pb}_{83}$ farther away from the first wall (and, consequently, with a much lower volumetric heating) starts to cool the first wall by conduction. This explains the cross-over point of the two temperature-response curves.

Fig. 4.2-6. Temperature response of the first wall due to loss of flow and loss of coolant accidents.

If a LOFA or LOCA occurs, the blanket will suffer permanent damage if the plasma is on for 10 s. If the plasma is not stopped in ~ 35 s, the first wall will melt.

In a liquid-metal flow system, a surge tank is always provided to maintain a free surface. The $\text{Li}_{17}\text{Pb}_{83}$ in the surge tank can be used as the emergency coolant by using gravitational force to pump. A coolant velocity of 7 cm/s can be reached by using the gravitational head. The temperature response of the first wall, shown in Fig. 4.2-7, will reach a maximum of 800°C in ~ 30 s. The volumetric flow rate required for the coolant is $5 \text{ m}^3/\text{s}$ for the entire reactor. The total emergency cooling time provided has to be optimized between the surge tank cost and accident detection time.

Table 4.2-9. Reactions of LiPb alloys and lithium with water.

Case	Composition	Sample state ^a	Temp (K)	Water Temp (K)	Reaction
1	Li_7Pb_2	s	773	298	Modest
2	Li_7Pb_2	s	773	369	Vigorous
3	Li_7Pb_2	l	873	368	Vigorous
4	Li_7Pb_2	l	1103	368	Very vigorous
5	$\text{Li}_{62}\text{Pb}_{38}$	l	773	368	Vigorous
6	$\text{Li}_{17}\text{Pb}_{83}$	l	773	368	Very modest
7	Li	l	773	368	H_2 Detonation
8	Li^b	l	773	368	Detonation

^a Where s = solid, l = liquid.

^b Injected under water.

Fig. 4.2-7. Temperature response of the first wall with gravitational coolant flow.

Afterheat Calculations. The afterheat levels in the blanket and shield are shown in Figs. 4.2-8 and 4.2-9. Figure 4.2-8 shows the afterheat with the blanket filled with $\text{Li}_{17}\text{Pb}_{83}$ and Fig. 4.2-9 shows the afterheat with the $\text{Li}_{17}\text{Pb}_{83}$ drained. Table 4.2-10 shows the adiabatic temperature rise rate in different regions of the blanket and shield. Because of the manganese in Fe-1422, the afterheat in the reflector is very high 1 hr after the reactor is shutdown. The half-life of ^{56}Mn is 2.6 hr; therefore, the afterheat in the reflector decays rapidly in the first day. After one day, the reflector can be used as the sink to radiation-cool the blanket.

Table 4.2-11 summarizes the thermal radiation calculation for the blanket one day after the reactor is shut down. We see that the blanket can be cooled effectively by thermal radiation. The best estimate of emissivity of the steel tube is 0.4. The maximum temperature that can be reached is 390°C at $\epsilon = 0.4$.

Table 4.2-10. Rate of adiabatic temperature rise (in $^\circ\text{C}$).

	With $\text{Li}_{17}\text{Pb}_{83}$		Without $\text{Li}_{17}\text{Pb}_{83}$	
	1 hr after shutdown	1 day after shutdown	1 hr after shutdown	1 day after shutdown
First wall	7.7×10^{-2}	7.6×10^{-3}	1.0×10^{-1}	1.6×10^{-2}
First blanket	1.7×10^{-2}	2.5×10^{-3}	6.2×10^{-2}	9.3×10^{-3}
Second blanket	9.1×10^{-3}	6.9×10^{-4}	3.5×10^{-2}	2.7×10^{-3}
Reflector	3.3×10^{-2}	9.7×10^{-5}	3.3×10^{-2}	1.4×10^{-4}
Shield	1.0×10^{-3}	3.3×10^{-6}	1.0×10^{-3}	3.3×10^{-6}

Fig. 4.2-8. Afterheat in different region with $\text{Li}_{17}\text{Pb}_{83}$.

Fig. 4.2-9. Afterheat in different regions after $\text{Li}_{17}\text{Pb}_{83}$ is drained.

Table 4.2-11. Radiation-cooled blanket temperature. (Forced cooling was stopped one day after shutdown.)

	Maximum blanket temperature (°C) Steel tube emissitivity		
	0.5	0.4	0.2
First wall	357	390	508
Second wall	347	379	493
Third wall	331	360	467
Fourth wall	307	332	426
Second blanket	272	290	363
Reflector	200	200	200

4.2.1.9 Radiation Effects on Central Cell Materials

Five important materials in the central cell need to be analyzed:

- HT-9 (blanket material);
- Fe-1422 (reflector and shield structure);
- Copper (stabilizer in the magnets);
- NbTi (superconductor in the magnets); and
- Polyimide (magnet electrical insulation).

Figure 4.2-10 shows the spatial variation of dpa and helium production in the blanket and shield. Figure 4.2-11 shows the spatial variation in the dpa rate for the Cu stabilizer.

An analysis of these figures reveals that the maximum damage rate in the HT-9 is 70.6 dpa/FPY, while helium is produced at a rate of 561 appm per FPY. The present assessment of the useful life of HT-9 at 500°C is 10 to 20 MW-yr/m² (100 to 200 dpa) so that we expect a first wall life of 1.5 to 3 FPY.

Fig. 4.2-10. Spatial variation of displacement damage and helium production in MARS.

Fig. 4.2-11. Spatial variation of displacement damage in copper stabilizer in MARS central-cell solenoid magnets.

The maximum damage rate in the Fe-1422 is 3.8 dpa/FPY and 4.9 appm He/FPY at 300°C. Without high surface heat fluxes or cycling there is no apparent reason why this steel cannot achieve useful lifetimes of ~ 100 dpa at such a low temperature. This means that it will be reasonable to expect a full reactor lifetime (~ 24 FPY) for the reflector and shield.

The damage rate of 9×10^{-6} dpa/FPY in the copper stabilizer is sufficient to achieve 12 FPY of operation before annealing is required (at 1.1×10^{-4} dpa). Even assuming 80% recovery for each anneal means that only three anneals are required in the 24 FPY of reactor lifetime.

The low damage level in the NbTi superconductor ($\sim 8.9 \times 10^{-6}$ dpa/24 FPY) indicates that we should expect full reactor lifetime from the magnet. The threshold for property degradation in NbTi is $\sim 10^{-3}$ dpa.

Finally, the maximum dose in the polyimide magnet insulation is 4.3×10^7 rad/FPY. The cumulative damage is only 1×10^9 rad in 24 FPY which is well below the design limit of 5×10^9 rad.

In summary, there appear to be no major lifetime problems in the central cell blanket, reflector, shield or magnet structures. Scheduled replacement of the blanket modules on a 2 to 3 year cycle does not appear to be too restrictive from an availability standpoint.

4.2.1.10 Magnetic Effects on Structural Materials

The use of HT-9, which has a high resistance to radiation damage, should greatly increase the operating life of the MARS blanket. However, because this alloy is ferromagnetic, one must be concerned about the effect of the ferritic structure on the magnetic field and the effect of the magnetic field on the ferritic structure. It is well known that the presence of a ferromagnetic material in a magnetic field will change the shape of the magnetic field in a way that depends on the geometry and the magnetization of the structure. Relatively small changes in the field ripple can cause enhanced particle leakage and possibly reduce the stability of the plasma. It is also known that the ferromagnetic structure will be subjected to magnetic forces and moments if placed in a magnetic field. Such forces can require a substantial amount of support material. Therefore, an accurate description of the magnetic loading is required from both safety and economic points of view.

Magnetic Forces. The forces on a magnetic body depend on the magnetization of the body and the gradient of the field within the body. In regions where the field is above 1.5 T, the HT-9 structure will be magnetically saturated independent of its orientation with respect to the field. Therefore, in these regions, the magnitude of the magnetization will be almost constant and the force variations will depend mainly on the gradient of the magnetic field and the direction of the magnetization. The latter quantity depends on the direction of the field.

Two cases illustrate the magnitude of the magnet force variations. The inlet coolant pipe for the liquid metal blanket was placed in two different locations. It has an inner diameter of 43 cm and thickness of 0.6 cm, and is simulated by a solid beam of square cross section 9.1 x 9.1 cm, i.e., an equivalent volume of the iron. The beam is 460 cm long. The beam and the field along its center are shown in Fig. 4.2-12, and the calculated magnetic forces per unit length are shown in Fig. 4.2-13. In this figure the forces are relative to the magnitude of the weight of HT-9 per unit length. The large arrow in Fig. 4.2-13 represents the magnitude of the weight per unit length of the LiPb coolant that would fill the original pipe (in the same units). In this case, the magnetic force could have a maximum value slightly larger than the weight of the coolant.

When the position of the pipe is raised to that shown in Fig. 4.2-14, the magnetic forces on the corresponding beam are much smaller as shown in Fig. 4.2-15.

The general conclusions drawn from these preliminary calculations are: the forces on such a beam could vary according to its location; the force distribution must be known to design the appropriate support structure; the magnitudes of these forces are not very high compared to those already associated with the liquid-metal-filled pipe itself.

Field Perturbation. As stated previously, the magnetic field that confines the plasma will be perturbed by the presence of the ferromagnetic steel. These perturbations can be categorized as either azimuthal or axial. The azimuthal perturbation will most probably be caused by a nonuniform azimuthal distribution of the HT-9; the axial perturbation is merely an end

Fig. 4.2-12. The first position of the beam and the magnetic field along its axis.

Fig. 4.2-13. The force distribution on the beam shown in Fig. 4.2-12. The large arrow represents the magnitude of the weight per unit length of LiPb that would fill the original pipe.

Fig. 4.2-14. The second position of the beam and the magnetic field along its axis.

Fig. 4.2-15. The force distribution on the beam shown in Fig. 4.2-14.

effect that depends on the length of the blanket (see Ref. 16 for a detailed description). In FY 1983 we will calculate the field ripple associated with the MARS blanket, but at this time we expect it to be less than 4%.

4.2.2 Shield and Radioactivity Considerations

4.2.2.1 Design Criteria

Shielding is required to protect the central cell solenoids from neutron and gamma radiation. The blanket is a poor attenuator of low energy neutrons because of the large volume fraction of lead, and a separate shield is needed.

The shielding requirements for the central cell magnets are determined by a number of radiation limits. The damage to the copper stabilizer should not exceed 1.1×10^{-4} dpa before the first anneal. This corresponds to a 20% increase in resistivity. Annealing at room temperature results in about 80% recovery of radiation induced defects.¹⁷ For an estimated reactor life of 24 FPY and 5 FPY before the first anneal, at least 1 FPY is available between the final two anneals. Therefore, the damage rate in Cu should not exceed 2.2×10^{-5} dpa/FPY.

Mechanical strength tests of irradiated magnet insulators have shown that polyimides are 5 to 10 times more radiation-resistant than comparably prepared epoxies.¹⁸ Polyimide is used as an electrical insulator in the MARS central-cell coils. The radiation effects on the organic insulators used in the magnets are not reversible. Hence, it is essential that they last for the whole reactor life. The design criterion used for polyimide is that the radiation dose should not exceed 5×10^9 rad after 24 FPY. The limit on the peak heat load in the magnet winding pack is determined by the refrigeration requirements and is taken to be 0.06 mW/cm^3 .

Although these constraints adequately protect the radiation-sensitive components of the magnets, other considerations influence the blanket and shield design. One of these is the desire to minimize long-term radioactivity in the system. Previous experience has shown that nickel in structural materials produces significant long-term radioactivity. Consequently, we have selected Fe-1422 as the basic structural material for the blanket and shield because it is a low-nickel high-manganese steel.

The final design constraint is the requirement for hands-on maintenance after shutdown. The selection of an allowable dose at the back of the shield is based on the limits set in part 20 of chapter 10 of the Code of Federal

Regulations (10CFR20). Stated very briefly, these regulations require that the dose to workers in a restricted area be kept as low as possible and no greater than 1.25 rem/calendar quarter. This dose rate is equivalent to a continuous exposure of 100 mrem for a 40-hr work week, or 2.5 mrem/hr. In interpreting these requirements, we considered several alternative criteria both with regard to dose and to the time after a shutdown at which the exposure would start. The alternative criteria considered were (1) 2.5 mrem/hr after 1 day, (2) 2.5 mrem/hr after 2 days, (3) 10 mrem/hr after 1 day, (4) 10 mrem/hr after 2 days, and (5) 100 mrem for 5 working days after 1 to 2 days. Although the first criterion is preferable because it permits relatively quick access and low dose, it is overly restrictive. Both 10-mrem criteria were rejected since they could result in excessive doses. We adopted the last criterion (100 mrem for 5 working days after 1 to 2 days) for this design because it satisfies the limits set in 10CFR20 for the first week, results in lower doses for subsequent periods, and still permits reasonably prompt access for servicing.

4.2.2.2 Neutronics

We performed one-dimensional neutronics calculations to determine the thickness of the shield required to protect the magnets. We considered a water-cooled shield consisting of layers of Fe-1422, B₄C, and lead. The calculations were performed with ANISN¹⁹ using a P₃-S₈ approximation. A coupled 46-neutron 21-gamma-group cross section set collapsed from the 'RSIC DLC-41B/VITAMIN-C library,²⁰ and the DLC-60B/MACKLIB-IV-82 response library²¹ was used. These data libraries are based on the ENDF/B-IV evaluation.

The model used for the magnet shielding calculations is shown in Fig. 4.2-16. The magnet case, cryostat, and vacuum dewar were included in the model as 6.3 cm of stainless steel. The shield consists of four zones. Shield A is used primarily to attenuate and moderate the high-energy neutrons that penetrate the blanket and reflector. The lead in shield B effectively attenuates gammas produced in the steel structure of the blanket, reflector, and shield. The boron carbide in shield C thermalizes neutrons and subsequent neutron absorption in boron produces only a soft 0.5-MeV gamma and an easily absorbed alpha particle. The Fe-1422 structure used in shields B and C will

0				
		Plasma		44
44				
		Void		15
59				
	Blanket I	73.7 vol % $\text{Li}_{17}\text{Pb}_{83}$	7.1 vol % HT-9	19.2
78.2				
		Void		0.8
79.0				
	Blanket II	85 vol % $\text{Li}_{17}\text{Pb}_{82}$	15 vol % HT-9	19.0
98				
		Void		2
100				
		Reflector		
		95 vol % Fe-1422	5 vol % H_2O	43
143				
	Shield A	95 vol % Fe-1422	5 vol % H_2O	10.67
153.67				
	Shield B	90 vol % Pb	5 vol % Fe-1422 5 vol % H_2O	8.0
161.67				
	Shield C	86 vol % B_4C	10 vol % Fe-1422 4 vol % H_2O	13.89
175.56				
	Shield D	90 vol % Pb	5 vol % Al 5 vol % H_2O	8.44
184				
		Void		8
192				
		Dewar	304 stainless steel	1
193				
		Void		3
196				
		LN_2 radiation shield (1/8-in. 304 stainless steel)		1
197				
		Void		7
204				
		Casing	304 stainless steel	5
209				
		Polyimide		1
210				
		Winding pack		
		35 vol % 304 stainless steel	15 vol % polyimide	117.3
		31.5 vol % Cu	3.5 vol % NbTi	
		15 vol % LHe		
327.3				
Radius (cm)				Thickness (cm)

Fig. 4.2-16. Geometrical model for magnet shielding calculations.

help attenuate the high-energy neutrons that succeed in penetrating that deep in the shield. Shield D is required primarily to attenuate the gammas resulting from radioactive decay of activated materials in the reactor. Al 6061 is used as a structural material in shield D to reduce the activation and to yield acceptable biological doses at the back of the shield after shutdown, as discussed in Section 4.2.2.5. Although a smaller thickness of this shield zone can provide adequate protection for the magnets, a zone thickness of 8.44 cm is used based on radioactivity considerations.

Using the shield configuration shown in Fig. 4.2-16, we obtained a peak damage rate of 8.94×10^{-6} dpa/FPY in the copper stabilizer. This implies that the first magnet anneal is required after 12 FPY. The peak radiation dose in the polyimide insulator after 24 FPY is 1.0×10^9 rad, which is well below the design limit of 5×10^9 rad. The peak heat load in the magnet is 0.05 mW/cm^3 , which is also lower than the specified limit. Therefore, the shield configuration used provides adequate magnet protection with a comfortable margin that allows for possible data uncertainties and calculational deficiencies.

4.2.2.3 Structural Design

One possible design for the 41-cm-thick shield is shown in Fig. 4.2-17. The pockets in the shield are filled with canned slabs of B_4C and lead, with spaces in between for cooling water. The pockets are then capped off and welded shut.

Nuclear heating in the shield will be low enough to allow low-velocity and low-pressure water cooling. Thus, water traversing one shield pocket from one end to the other will be routed onto the next, snaking its way around the shield. In this way a minimum number of coolant connections will be necessary.

Critical areas in the design are:

1. The penetrations in the shield required for coolant supply pipes.
2. Shield cutting positions for unexpected component replacement (see Section 4.2.6).

Fig. 4.2-17. A possible design option for the shield structure.

3. The bulk shield and built-in reflector in the servicing stations (see Section 4.2.6).

The shield should be firmly supported, since it also carries the weight of the blanket modules and reflector structure. We expect that the support structure will resemble the magnet support structure. Both of them should be able to move either in the axial direction or in the transverse direction for installation and maintenance.

4.2.2.4 Thermal/Hydraulics

The shield is cooled by water entering at 50°C and exiting at 80°C. The maximum water temperature is kept below 100°C to avoid pressurized containment. The total thermal energy deposited in the shield is 150 MW. Shield parameters are summarized in Table 4.2-12.

4.2.2.5 Radioactivity

The neutron fluxes from Sections 4.2.1.3 and 4.2.2.2 were used with the DKR code to calculate the activity of the blanket and shield after 2 FPY. The results of these calculations are summarized in Fig. 4.2-18. There is a fairly rapid initial decrease corresponding to the decay of $^{56}\text{Mn}(t_{1/2} = 2.58 \text{ hr})$. Subsequently, the activity is dominated by $^{54}\text{Mn}(t_{1/2} = 312 \text{ d})$ followed by $^{55}\text{Fe}(t_{1/2} = 2.7 \text{ yr})$. In terms of activity per unit volume of material, the average activity after 10 yr is 1.7 Ci/cm^3 but is reduced to $2.8 \times 10^{-3} \text{ Ci/cm}^3$ after 100 yr. The activity at long times is dominated by activities from molybdenum, specifically $^{93}\text{Mo}(t_{1/2} = 3 \times 10^3 \text{ yr})$, $^{92}\text{Nb}(t_{1/2} = 2 \times 10^7 \text{ yr})$, and $^{99}\text{Tc}(t_{1/2} = 2 \times 10^5 \text{ yr})$.

With the activity levels of the various isotopes calculated as a function of position in the blanket and shield, we used ANISN to calculate the gamma dose rate at the outer edge of the shield. As mentioned in Section 4.2.2.2, although the original shield design provides adequate protection for the magnets, it does not give adequate biological protection. The calculated dose rates were 250 mrem/hr after 1 day, 70 mrem/hr after 2 days, and 60 mrem/hr after 1 week. To reduce this dose rate, we added 10 cm of shielding (95% Pb, 5% Fe-1422), which reduced the dose rates to 18 mrem/hr after 1 day, 10.5 mrem/hr after 2 days, and 10.3 mrem/hr after 1 week. While this was a significant improvement, it was still not adequate to meet the design criterion.

Fig. 4.2-18. Radioactivity vs time after shutdown following two years of operation.

Table 4.2-12. Thermal hydraulic parameters for the shield.

Thermal power (MW)	150
Coolant	H ₂ O
Coolant temperature (°C)	50/80
Coolant flow rate (kg/hr)	4.3×10^6
Maximum water temperature (°C)	90
Maximum shield temperature (°C)	140
Maximum coolant velocity (m/s)	1
Maximum coolant pressure drop (kPa)	20
Coolant pumping requirement (kW)	10

ion. Previous studies have shown that in many cases the activated material in the outer part of the shield contributes significantly to the radiation dose. We confirmed this in our study by running a case in which the structural material in the outer region of the shield was concentrated on the inner side of that region. The resulting dose was further reduced to 6.8 mrem/hr after 1 day and only 0.5 mrem/hr after 2 days. Because this arrangement of the structural material would not be satisfactory from a mechanical design point of view, we replaced the Fe-1422 structure with Al 6061 in the outer region of the shield. Subsequent calculations showed that the dose rate for this arrangement would be 34 mrem/hr after 1 day, 11.6 mrem/hr after 2 days, and 0.6 mrem/hr after 1 week. The major contributor to the dose during this time period is ^{24}Na ($t_{1/2} = 15$ hr). Calculating the integrated dose for a 40-hr work week showed that a 100-mrem dose would be obtained if entrance was delayed to 2.3 days after shutdown. Thus, by adding 10 cm of shielding and replacing Fe-1422 with Al 6061 in the outer layer, the criterion of 10 mrem/hr could be met after slightly more than 2 days. More importantly, the limit of 100 mrem in 1 working week could be met after 2.3 days and any subsequent doses received would be significantly smaller.

It must be noted that the activation of materials near the edge of the shield plays an important role in determining radiation levels. Consequently, we expect that activating the components outside the shield will also contribute to the radiation level. Chief among these components are the magnets as well as their associated dewars and support structures. This problem is complex because of the geometry of the system and we cannot attempt to analyze it until these components are more completely designed. Thus, their contribution of materials near the edge of the shield to the radiation level is undetermined but must be considered before the shield design is finished.

4.2.3 Blanket Design Optimization

4.2.3.1 Design Philosophy

The MARS lithium-lead blanket has a flowing LiPb coolant/breeder contained in a ferritic steel structure. It is simple in concept, easy to fabricate, and provides for convenient maintenance. Given these characteristics, how should the blanket be designed with regard to neutronic and economic performance? For example, what value of tritium breeding ratio is required? How high should the blanket multiplication factor M be? What maximum fraction of the neutron energy should be deposited in the reflector? Do we design a thick blanket in an attempt to maximize M , or should we design a thin blanket in an attempt to minimize central cell costs? Would a thin blanket and corresponding thick shield be more expensive than a thick blanket with a corresponding thin shield? How do superconducting magnet radiation constraints affect the overall design, etc?

We examined in detail design questions such as these during the early design phase of the LiPb blanket; we concluded that all constraints and requirements should be integrated into a systematic trade study. Accordingly, we developed a blanket design optimization procedure to assess LiPb blanket designs in terms of their relative economic performance. This procedure is necessarily iterative and requires that an economic figure of merit be minimized for the overall reactor system.

4.2.3.2 Design Optimization Procedure

We developed the following procedure for blanket design optimization:

1. For the desired tritium breeding ratio (TBR), establish a LiPb blanket and reflector point design.
2. Perform neutronics analyses to obtain the blanket multiplication factor M and energy deposition in the blanket and reflector.
3. Given the blanket and reflector thermal powers, determine the gross efficiency η and LiPb pumping power.

4. Determine both the neutronics design constraints for the central cell magnets (i.e., maximum dpa in the stabilizer, maximum dose limit for the organic insulator, maximum nuclear heating rate) and the required radioactivity conditions at the back of the shield.
5. Given these magnet and radioactivity constraints, design the blanket shielding so that all conditions are satisfied.
6. Given the outer radius of the shield from above, determine the minimum inside radius of the central cell magnet winding pack.
7. Specify the magnitude and on-axis ripple (i.e., $\Delta B/B$) of the central-cell magnetic field and, given the inside radius of the winding pack from above, determine the magnet winding pack dimensions and center-center spacing.
8. For these magnet dimensions, ensure that the magnet configuration is consistent with blanket maintenance requirements.
9. Determine the cost of the blanket, reflector, shield, magnets, thermal hydraulic system, turbine plant equipment, and electrical plant equipment (i.e., those items whose costs depend on the blanket multiplication factor and, therefore, the blanket design).
10. Establish a self-consistent economic figure of merit F for overall system optimization.
11. Repeat the above procedure and, by varying the blanket/reflector design, minimize F through iteration.

With regard to this procedure, the following points should be noted:

1. When varying the blanket design in this iterative manner, it is very difficult (if not impossible) to precisely achieve a predetermined tritium breeding ratio (TBR). Accordingly, we specified a desired range for TBR of 1.05 to 1.15 at the outset and tailored subsequent design iterations to lie somewhere in this range.
2. Only the radiation dose limit to the organic magnet insulation is an absolute design constraint. The other radiation limits are tradeoffs of shield cost vs cryogenic system cost, magnet cost, or cost of lower availability.

3. For the LiPb blanket, the limiting constraint on the shield design was imposed by the required radioactivity conditions at the back of the shield.

4.2.3.3 Figure-of-Merit Parameter

It is now necessary to select an appropriate economic figure of merit F for the blanket system so that minimization of F leads to an optimized design. From a cursory view of this problem, it would appear expedient to minimize the capital cost of the central cell. However, a low-cost blanket may yield only modest values of recoverable power. Therefore, a better figure of merit would be to normalize the central cell costs by the recoverable central cell thermal power, i.e.,

$$F_1 = \frac{C_{CC}}{P_{CC}^t} = \frac{C_{CC}}{P_N M}, \quad (4.2-2)$$

where

C_{CC} = total central cell costs (blanket, reflector, shield, and magnets),

P_{CC}^t = central-cell thermal power, and

P_N = fusion neutron power.

Therefore, F_1 is minimized by minimizing central cell costs while maximizing M .

However, Eq. 4.2-2 is deficient in that it does not account for the thermal conversion efficiency, the costs of the thermal hydraulic system, or the LiPb pumping power. Therefore, it might be expected that an improved figure of merit would be

$$F_2 = \frac{C_{CC} + C_{TH}}{P_{CC}^e - P_p^e} = \frac{C_{CC} + C_{TH}}{P_N M - P_p^e},$$

where M = neutron energy multiplication factor,

C_{TH} = cost of thermal hydraulic system,

P_{CC}^e = gross central-cell electric power,

P_p^e = electric power requirements of LiPb pumps, and

η = thermal cycle efficiency.

Again, however, use of F_2 leads to spurious conclusions. In particular, the blanket optimization process should seek to minimize the overall capital cost of the reactor through blanket design. This requires that the figure of merit explicitly contain the cost of other reactor items such as the plasma heating systems, miscellaneous plant equipment, direct converter, etc., even though they are not dependent on the blanket design. Accordingly, the figure of merit chosen for this study is

$$F = \frac{C_{CC} + C_{TH} + C_{TP} + C_{EP} + C_{MP} + C_{PM} + C_H + C_{DC} + C_B}{P_{CCN}^e + P_{DC}^e - P_R^e}, \quad (4.2-3)$$

where

C_{CC} = total central cell costs,

C_{TH} = cost of thermal hydraulic system,

C_{TP} = cost of turbine plant equipment,

C_{EP} = cost of electrical plant equipment,

C_{MP} = cost of miscellaneous plant equipment,

C_{PM} = cost of plug magnets,

C_{DC} = cost of direct convertor,

C_H = cost of supplemental heating systems,

C_B = cost of buildings and site facilities,

P_{CCN}^e = net electric power from central cell ($P_{CC}^e - P_p^e$),

P_{DC}^e = electric power from direct convertor and halo convertors, and

P_R^e = recirculating electrical power requirements for reactor (except LiPb pumps).

Here we define F as the overall capital cost of the reactor normalized to the net electrical output. It is commonly expressed in units of \$/kW_e.

Note the following interesting properties of F from Eq. 4.2-3. The first four costs (C_{CC} , C_{TH} , C_{TP} , and C_{EP}) are dependent on the blanket and thermal cycle properties, while the last five terms (C_{MP} , C_{PM} , C_H , C_{DC} and C_B) are independent of the blanket properties. In the denominator, P_{CCN}^e is blanket-dependent while P_{DC}^e and P_R^e are blanket-independent. However, for a tandem mirror reactor such as MARS, the electrical output from the direct convertor P_{DC}^e and the recirculating electric power requirements P_R^e are comparable in magnitude and approximately cancel each other in Eq. 4.2-3. It is therefore possible to represent this figure of merit by

$$F = \frac{C + C'}{P_{CCN}^e}, \quad (4.2-4)$$

where

C = cost of all blanket-dependent items,

C' = cost of all non-blanket-dependent items, and

P_{CCN}^e = net electric power from central cell.

Note that now there is a large term C that is independent of the blanket multiplication factor M and thermal cycle efficiency η , while the denominator P_{CCN}^e is directly proportional to $M\eta$. The figure of merit parameter F is, therefore, quite sensitive to changes in P_{CCN}^e due to changes in $M\eta$. It is not as sensitive to changes in the blanket-dependent costs C because of the large constant term C' . Basically, we wish to amortize the end cell costs over a central cell that produces as much electricity as possible. Additional details of this methodology are given in Ref. 22.

4.2.3.4 Figure-of-Merit Application

We studied the performances of two near-optimum designs for the LiPb blanket and reached two main conclusions. First, maximum values of the product of $M\eta$ (and thus maximum values of P_{CC}^e) are obtained for a blanket with a large radial thickness and a natural isotopic composition of lithium in the LiPb coolant/breeder ("thick blanket"). Second, the cost of the blanket-dependent items is minimized for a blanket with small radial thickness and highly enriched lithium ("thin blanket"). However, the thin blanket has a

lower M than the thick blanket and, in addition, has a higher fraction of the total neutron energy deposition in the reflector, thus resulting in a lower value of η . Further details of these neutronics analyses are found in Section 4.2.1.3. The optimum blanket is, therefore, that which minimizes the figure-of-merit parameter F in terms of overall $\$/kW_e$.

Table 4.2-13 lists the central cell parameters for the thick and thin blankets. Table 4.2-14 itemizes approximate costs for those items that depend on the blanket design and, therefore, on the parameters M and η . Note that the central cell costs are dominated by the magnets as is the difference between the total blanket-dependent costs. Table 4.2-15 shows an approximate cost breakdown for the blanket-independent items in the reactor system. These figures are based on rough estimates of direct capital costs and should not be taken as final or definitive cost figures for MARS.

Table 4.2-16 summarizes the economic tradeoff using the figure-of-merit parameter F . Table 4.2-15 shows that F for the thin blanket is $\sim 2.4\%$ smaller than F for the thick blanket. This is a small but significant difference and indicates that the thin blanket is an improved blanket design. It might be argued that, because of the uncertain costs of the blanket-independent items in Table 4.2-16, a 2.4% difference in F is not significant. However, Fig. 4.2-19 shows the ratio of F for the thin blanket to that of the thick blanket plotted as a function of the cost of the blanket-independent items (C'). Although this ratio remains less than unity, the thin blanket is economically preferable. The present design point of $C' = 1610$ $\$M$ with an F -parameter ratio of 0.976 is also indicated. To change the conclusion as to which blanket is preferred, the magnitude of C' would have to change by a factor of about 2.7 to ~ 4375 $\$M$. The uncertainty in the ratio of the F parameter is, therefore, very insensitive to the uncertainty in C' .

Table 4.2-13. LiPb blanket parameters.

	Thin blanket	Thick blanket
Blanket thickness (m)	0.382	0.872
Reflector thickness (m)	0.43	0.28
Shield thickness (m)	0.41	0.44
Inside radius of magnet winding pack (m)	2.10	2.48
Winding pack dimensions (m ³)	0.78 x 0.78	0.90 x 0.90
CC magnet spacing (m)	2.75	3.225
⁶ Li enrichment (%)	90	Nat.
Fraction of energy in reflector (%)	27.6	13.4
Blanket multiplication M	1.390	1.374
Tritium breeding ratio	1.135	1.078
Gross cycle efficiency η (%)	40.0	41.7
$M \times \eta$		0.556 0.572
LiPb pumping power (MW _e)	54.7	63.6
Net cycle efficiency (%)	38.6	40.0
CC thermal power (MW _t)	3892	3847
CC gross electrical power (MW _e)	1556	1603
CC net electrical power (MW _e)	1501	1539

Table 4.2-14. Costs for blanket-dependent items.

	<u>Thin blanket</u>	<u>Thick blanket</u>
Blanket cost/m (\$/m) ^a	0.241	0.425
Reflector cost/m (\$/m) ^a	0.356	0.312
Shield cost/m (\$/m) ^a	0.207	0.272
Magnet cost/m (\$/m) ^a	2.060	2.602
Total CC cost/m (\$/m) ^a	2.863	3.611
Total CC cost (\$M) ^a	429.5	541.7
Thermal hydraulic system (\$M) ^b	262.5	279.1
Turbine plant equipment (\$M) ^c	262.7	266.8
Electric plant equipment (\$M) ^c	<u>158.1</u>	<u>161.2</u>
Total (\$M)	1113	1249

^a Costed on a consistent \$/unit-mass basis.

^b Costed on a \$/unit-mass basis with suitable representative costs for values, pumps, double-walled HX, etc.

^c Costed via algorithms from STARFIRE²³

Table 4.2-15. Costs for blanket-independent items. (All costs are in \$M).

	<u>Subsystem</u>	<u>System</u>
Miscellaneous plant equipment		150
Plug magnets		250
Direct convertor		142
Buildings and site facilities		300
Heating systems		768
ECRH	240	
Neutral beams	528	
Total blanket-independent items		1610

Table 4.2-16. Summary of blanket optimization.

	<u>Thin blanket</u>	<u>Thick blanket</u>
Blanket-dependent items (\$M)	1113	1249
Blanket-independent items (\$M)	1610	1610
Total direct costs (\$M)	2722	2858
Blanket net electrical power (MW_e)	1501	1539
Direct converter electric power (MW_e)	565	565
Recirculating electric power (MW_e)	527	527
System net electric power (MW_e)	1529	1577
Figure of merit (\$/ kW_e)	1769	1812
Normalized figure-of-merit	1.0	1.024

Fig. 4.2-19. Ratio of the thin blanket/thick blanket F-parameters as a function of the cost of the blanket-independent items.

4.2.4 Tritium Removal

4.2.4.1 Tritium Extraction from LiPb

Tritium is bred in the $\text{Li}_{17}\text{Pb}_{83}$ eutectic at a rate of 0.59 g/day and must be extracted at this rate. Tritium can be removed from lithium-lead alloys^{24,25} by vacuum pumping, inert gas sparging, and molten salt extraction. We chose vacuum pumping for MARS as the primary tritium recovery scheme. The lithium-lead system is a unique liquid breeder because of its extremely low tritium solubility. The Sieverts' constant for tritium in $\text{Li}_{17}\text{Pb}_{83}$ in the temperature range from 400 to 600°C is 0.051 wppm $\text{T/Torr}^{1/2}$.²⁶ Even at very low concentration levels (wppb), tritium will establish a reasonably high partial pressure above the alloy. Vacuum pumping at or below this partial pressure should provide an effective means of removing the tritium in a relatively simple and economical fashion. Although no experiments are presently being conducted on vacuum removal of gases from LiPb, considerable experience exists in the degassing of liquid steels to remove hydrogen and other volatile impurities.²⁷ We chose vacuum pumping without a helium purge gas in the base design to avoid possible large helium flow rates and helium/tritium separation at large He:T_2 ratios. This option, however, is still being investigated. Molten salt extraction from lithium-lead alloys appears feasible and has been suggested in other designs.^{24,25,28} Vacuum removal is a simpler alternative and it avoids the possible transport of molten salt residues through the coolant loop.

To determine the optimum steady-state concentration level in the breeding material, both the extraction and the containment systems must be considered. High concentrations place a burden on the containment systems whereas low concentrations make extraction more difficult. The trade-off between the partial pressure and the pumping requirements is illustrated in Fig. 4.2-20. The major containment problem is in the transport of tritium through the heat exchanger. For a single-walled tube with an oxide coating giving a resistance factor of 100, the tritium partial pressure must be 2.5×10^{-9} Torr to keep losses through the heat exchanger to 10 Ci/d. The pumping rate of 2.1×10^{10} liter/s at 500°C prohibits extraction at this low pressure.

Fig. 4.2-20. Volumetric flow rate for tritium extraction by vacuum pumping at various tritium partial pressures.

Table 4.2-17. Point design parameters for tritium removal system by vacuum pumping.

Tritium removal rate (kg/day)	0.59
Vacuum pumping speed at 10^{-4} Torr and 500°C (liter/s)	5.3×10^5
Tritium removed (%)	44
LiPb sent to extractor (%)	22
LiPb flow rate into extractor (kg/s)	3.1×10^4
Extraction temperature	500°C
Tritium inventory in pumps (g) ^a	49
Inlet tritium conditions	
Pressure (Torr)	1×10^{-4}
Concentration (wppm)	5.1×10^{-4}
Outlet tritium conditions	
Pressure (Torr)	3.0×10^{-5}
Concentration (wppm)	2.8×10^{-4}

^a 2 hr on-line.

We selected a tritium pressure of 10^{-4} Torr for the point design to ease the burden on the extraction system. At this pressure and 500°C , the pumping rate is 5.3×10^5 liter/s and the vacuum system cost is low, about \$100,000. This pressure will require a confinement system that reduces the tritium permeation through the heat exchanger by a factor of $\sim 10^5$. The double-walled tubes in the heat exchanger are expected to be able to provide a barrier of this magnitude. (See Section 4.2.5.3.)

The parameters for the preliminary point design of the tritium recovery system (TRS)--using vacuum pumping at a tritium pressure of 10^{-4} Torr--are given in Table 4.2-17. With a LiPb flow rate about 1.4×10^5 kg/s, the tritium flow rate is 7.1×10^{-5} kg/s. To extract tritium at the breeding rate of 6.8×10^{-6} kg/s, 9.6% of the tritium must be removed. The extractor is assumed to remove 44% of the tritium. Therefore, 22% of the LiPb must be continually diverted to the TRS.

At 500°C the vapor pressure above the eutectic corresponds to $P_{\text{Pb}} = 1.4 \times 10^{-5}$ Torr and $P_{\text{Li}} = 3.7 \times 10^{-7}$ Torr. In the vacuum system, both lithium and lead will be transported from the alloy in the ratio of their vapor-phase composition, at transport rates of 2.9×10^{-8} kg/s and 3.3×10^{-5} kg/s, respectively. Cooled surfaces will be used to trap the lithium and lead vapors before they can reach the cryopumps.

There are several possible ways of creating favorable transport conditions in the liquid metal to allow for tritium removal by vacuum: large-surface-area pools, pools with agitation or gas sparging, or a thin stream or droplet spray introduced into the vacuum vessel. We are currently comparing these methods for extraction capability, heat loss from the liquid metal, and simplicity of design. The following are some preliminary assessments and calculations for various extraction schemes:

Degassing From Liquid Metal Pools. The steps for transporting tritium gas from a liquid metal pool are:²⁷

1. Transport from the interior to the interface of the metal. This occurs in liquids by convective transport processes in the interior to a zone near the surface and from there by diffusion to the interface.
2. Transition from the dissolved state to the adsorbed state at the surface.
3. Association of gas atoms in the adsorbed layer with each other to form molecules.
4. Desorption of molecules from the surface.
5. Diffusion of gas molecules into the gas space and removal by the pumping system.

Step 1, transport in the liquid phase, determines the rate of the entire process. Because mass transport in the liquid interior is by convection, generating additional convection in the pool with a stirring coil or an inert gas purge improves performance. The key parameter is the mass transfer coefficient β , which is not known for hydrogen in $\text{Li}_{17}\text{Pb}_{83}$. Known values of 3.9×10^{-2} cm/s for hydrogen in liquid aluminum at 700°C²⁹ and 1 cm/s for hydrogen in liquid steel at 1600°C³⁰ give an idea of the order of magnitude. The mass

transfer coefficient can also be determined from the diffusion coefficient and boundary layer thickness, which are themselves unknown. An estimate gives $\beta \approx 1$. Experiments and further calculations are necessary to determine the value for the mass transfer coefficient.

Figure 4.2-21 shows the dependence of β on the extraction system parameters. For $\beta \lesssim 10^{-1}$, the extraction system requires high surface-area-to-volume ratios for residence times of $\lesssim 100$ s. An extraction system that creates a large surface area, such as a droplet spray, would be an alternative to creating large thin pools. If $\beta \approx 1$, then the ratio of area to volume is low enough to create a reasonably sized pool for residence times $\lesssim 100$ s. Table 4.2-18 gives an example of the parameters for a pool degassing system with $\beta \approx 1$.

Gas Sparging. The use of a scavenging gas has two important effects on mass transfer. First, it provides an increased surface area available for release; second, it causes agitation of the melt. Tritium is removed from the bulk of the liquid by diffusion into the gas bubbles and from the surface by the mechanism described in Section 4.2.4.1. Surface losses may be increased because of the increased surface area caused by the boiling action of the bubbles.

We developed a simplified model to give a preliminary assessment of the purge gas requirements for tritium removal from the base design. The total tritium lost from the system is the tritium removed by transport into the gas bubbles and the tritium that diffuses from the surface. The tritium that enters the bubbles is calculated by assuming that the mass transfer into the bubbles is perfect and, therefore, the bubble is always saturated at the equilibrium tritium pressure. This assumption should give the most optimistic result.

Table 4.2-19 shows the inert-gas flow rate required to remove various amounts of T_2 . The amount of tritium removed from the bulk by the inert gas is small unless relatively large purge-gas flows are used; tritium losses from the surface dominate at low flow rates. Tritium can be separated from helium at a molar ratio of 10^{-4} ; however, to avoid this separation process we did not use the helium purge in the baseline design.

Fig. 4.2-21. Effect of the mass transfer coefficient, β , on area-to-volume ratio and the residence time for liquid metal degassing.

Table 4.2-18. Vacuum extraction from a liquid-metal-pool by vacuum pumping with stirring.

Mass transfer coefficient (cm/s)	1
Residence time (s)	100
Mass of LiPb in extractor (kg)	3.1×10^6
Tritium inventory in LiPb (g)	1.6
Pool dimensions	
Radius (m)	7.8
Height (m)	1.7
Surface area (m ²)	1.9×10^2
Volume (m ³)	3.3×10^2

Table 4.2-19. Removal of tritium from the bulk of the melt with an inert gas purge.^a

<u>% T₂</u> <u>removed</u>	<u>Mol T₂/s</u> <u>removed</u>	<u>Volumetric flow</u> <u>rate (liter/s)^b</u>	<u>Purge rate</u> <u>(g He/s)</u>
100	1.1×10^{-3}	2.2×10^8	190
10	1.1×10^{-4}	2.2×10^7	19
1	1.1×10^{-5}	2.2×10^6	1.9
0.1	1.1×10^{-6}	2.2×10^5	0.19

^a Does not include surface losses, which may be dominant.

^b The volumetric flow rate of helium is calculated at 450°C and 10⁻² Torr.

Stream Degassing. In stream degassing, the liquid metal is forced through a nozzle to form a stream of droplets that fall from a predetermined height into a pool. The height of fall and dwell time are minimized to reduce heat losses. We have studied the rate of diffusion from a spherical droplet in a vacuum and the time of fall.³¹

Table 4.2-20 shows the amount of gas liberated from falling drops with radii of 1 to 3 mm for fall times of 0.5 to 1 s. The desired value of 44% removal can be achieved for fall times slightly greater than 0.2 s with radii of 1 mm. Some degassing will also occur in the pool at the bottom of the chamber. This process can achieve a high degree of degasification in a short time period and, therefore, it appears to be the superior method. We are continuing to study the design of this system.

4.2.4.2 Tritium Isolation

The secondary and tertiary containment systems minimize personnel exposure to tritium and environmental effects of tritium by preventing tritium from escaping by permeation, leakage, or system malfunction. Containment systems have been developed for ISTA and other tritium facilities and should be well-established on the time scale for a commercial fusion reactor.

We are particularly concerned about tritium losses from systems containing LiPb at high-tritium partial pressures of 10^{-4} Torr. This problem is especially critical in the heat exchanger; an analysis of tritium isolation by the use of double-walled tubes is given in Section 4.2.5.3. In the central cell tritium that permeates through the blanket tubing is pumped by the reactor vacuum system along with the exhaust gases. The tritium extraction unit and liquid metal holding tanks will have thick, well-insulated walls to prevent heat loss and reduce tritium permeation. Large-diameter thick-walled well-insulated piping is used to transport the LiPb. In addition, aluminum sleeves can be used to provide a secondary containment barrier around the pipes. Additional jacketing or glove boxes can be used to enclose valves or other components where leakage may occur.

Table 4.2-20. Gas liberation from droplets during stream degassing.

Drop radius (mm)	<u>% gas liberated</u> <u>Time of fall (s)</u>		
	0.2	0.5	1.0
1	0.42	0.60	0.77
3	0.15	0.24	0.32

4.2.5 Energy Conversion System

4.2.5.1 Heat Exchanger Arrangement and Power Cycle

Figure 4.2-22 shows the power cycle for the LiPb blanket. The 2940-MW_t heat input is split in two places to increase the effectiveness of heat transfer. The steam system is 2400 psig/482°C (900°F) with 482°C (900°F) reheat. The heat transfer temperature diagram in Fig. 4.2-23 illustrates the effectiveness of the cycle in a system where the first-stage reheat and economizer do not share the blanket heat. The evaporator pinch point increases from 23.5 to 30% of the total heat transferred, and the ΔT is raised from 15 to 25°C. This causes larger log-mean-temperature differences (LMTD) in the evaporator, first-stage reheater, and economizer, which in turn reduces heating surface requirements and LiPb inventory. A larger pinch-point ΔT may make it possible to use a higher steam-cycle pressure, which would increase steam-cycle efficiency over the efficiency possible at 2400-psig.

Similar procedures can be used advantageously to integrate power recovery from water used to cool the reflector. Cooling water at 300°C with a 27.8°C ΔT can use PWR-type steam generators to produce 640-psig saturated steam. A reflector power of 980 MWt will generate 4.04×10^6 lb-steam/hr from 204°C feedwater.

To improve the thermal efficiency of this steam cycle, the 600-psi steam (at turbine throttle) can be superheated to 357°C from the blanket coolant (152.5 MWt) by adding a superheater in parallel with the first stage reheater and economizer shown in Fig. 4.2-22. This will obviously improve the 600-psi reflector power cycle. In addition, it will shift the pinch point in Fig. 4.2-23 from 30 to 35% to further improve component LMTDs and provide greater flexibility in going to a higher blanket-power-cycle pressure.

Similarly, cooling water heated to 250°C can be used to produce 250-psia saturated steam (at turbine throttle) for a third power cycle. This can be superheated by reflector cooling water at 300°C and possibly reheated by blanket-derived-steam at 600-psi.

Fig. 4.2-22. Heat exchanger arrangement.

Fig. 4.2-23. MARS temperature diagram for 2400-psia 900°F/900°F steam cycle.

4.2.5.2 Double-Walled Steam Generator

Heat exchange equipment for the MARS power cycle must fulfill exacting requirements while at the same time retaining design features acceptable to the utility industry. The design requirements of liquid-metal-to-water heat exchangers are complicated by two factors:

- It must provide a tritium diffusion barrier between blanket coolant and steam cycle.
- It must permit practical inspection and maintenance procedures in spite of radiation levels produced by activated corrosion products present in the blanket coolant.

The double-walled steam generator described in Figs. 4.2-24 through 4.2-27 was created to meet the above requirements. It provides the following features:

1. An adequate tritium diffusion barrier.
2. Detection of potential tube defects that could lead to metal/water reactions.
3. Avoidance of size-limitation problems typical of double-walled heat exchangers.
4. Detection, location, and repair of leaks by conventional methods (eddy current).
5. More compact construction.
6. More conventional, less difficult component assembly.

The space between the inner and outer shells and between the inner and outer walls of each tube assembly is filled with helium that contains oxygen at a partial pressure of 1 Torr. A pumping system (not shown) is used to circulate this gas longitudinally through each tube wall gap as a sweep (or monitor) gas to detect water vapor.

The hemispherical heads shown in Fig. 4.2-24 are similar in configuration to those used for "once-through" PWR steam generators. Thus, the tube inspection and repair equipment developed for this steam generator is applicable to

Fig. 4.2-24. General arrangement.

Fig. 4.2-25. Barrier tube to inner tubesheet detail.

Fig. 4.2-26. Double walled tube structure.

Fig. 4.2-27. Tube support detail.

the design shown in Fig. 4.2-24. This equipment has been used satisfactorily to perform inspection and maintenance work in radiation levels up to about 15 rem at the hemispherical head tube face.

To cope with higher radiation levels, the manways can be replaced by full opening closures, which in turn will permit use of rotating plugs and other equipment developed in the past for hot maintenance. The effect of activated corrosion-product radiation on inspection and maintenance tends to be minimized by the following:

1. Vertical (instead of horizontal) tube sheet faces that reduce collection of corrosion particles on the surfaces.
2. Inner and outer tube sheets with a space between that attenuates streaming of gamma rays through tube inside diameters.
3. Corrosion products will tend to collect at the top of the inner shell. Radiation from this location will not be in line with tube sheet holes.

4.2.5.3 Tritium Isolation

Tritium confinement is one of the most difficult problems stemming from the use of $\text{Li}_{17}\text{Pb}_{83}$. The very low tritium solubility in $\text{Li}_{17}\text{Pb}_{83}$ results in a very high tritium partial pressure. The most vulnerable region in the primary loop for tritium leakage is the primary heat exchanger. To improve heat transfer, the surface of the heat exchanger is large, with thin-walled tube construction. Therefore, an effective tritium diffusion barrier is required between the primary loop and the steam cycle to reduce the tritium leakage to an acceptable level.

Hydrogen permeability through different ferritic steels is summarized in Fig. 4.2-28. The best estimation of tritium permeability through HT-9 is

$$P_e = \frac{1.8 \times 10^3}{\sqrt{3}} \exp\left(-\frac{11100}{AT}\right) \frac{\text{mol } T_2 \cdot \text{mm}}{\text{d} \cdot \text{m}^2 \cdot \text{atm}^{1/2}}$$

With this permeability and a steam generator area of $3.46 \times 10^4 \text{ m}^2$, the tritium leakage rate is $3.5 \times 10^5 \text{ Ci/day}$. Therefore, a tritium diffusion barrier of 10^5 is needed.

Fig. 4.2-28. Hydrogen permeation through chromium ferritic steels.

In MARS, a double-walled heat exchanger provides the required tritium diffusion barrier. The construction, with two tubes in close contact, is shown in Fig. 4.2-26. A helium purge with 1 Torr of oxygen pressure passes through the channels to provide an oxidizing atmosphere between the two tubes. The oxide films formed between the two tubes, as well as the oxide film on the water side, provide an effective diffusion barrier.

Figure 4.2-29 shows two tritium diffusion paths through the double-walled steam generator. This is an enlarged view around one contact point between the tubes.

The resistance to the diffusion across the gap is the product of the resistance of the oxide films and the resistance of the gap. Figure 4.2-30 shows the attenuation factor caused by the gap as a function of the gap width; Fig. 4.2-31 shows the attenuation factor resulting from the combined effect of three oxide coatings as a function of single oxide coating layer factor. If the gap width is 10^{-3} cm, the attenuation factor from the gap is 3×10^{-3} . To obtain a total attenuation factor of 10^{-5} , an attenuation factor of 3×10^{-3} is needed from the oxide coatings, which corresponds to a single layer factor of 20. Experiments have indicated that a single-layer factor of a few hundred is available for ferritic steel.³² It can be concluded, therefore, that the attenuation factor of $\ll 10^{-5}$ is available for tritium leakage across the gap.

We performed a two-dimensional finite-difference tritium diffusion calculation for tritium leakage along path 2 (through the contact point). The result shows that an attenuation of 10^{-5} is also available along this path. The total attenuation is the sum of the attenuation along those two paths and is, therefore, $< 10^{-5}$.

4.2.5.4 Removal of Corrosion Products

The corrosion in the blanket causes two problems. The first is thinning of the blanket structural material, which can weaken the structure. This effect appears to be acceptable. The second problem is caused by corrosion products transported to the primary heat exchanger and deposited there. This may cause problems such as channel blockage, which will require the heat exchanger to be remotely maintained.

Fig. 4.2-29. Tritium diffusion path.

Fig. 4.2-30. Attenuation factor due to gap.

Fig. 4.2-31. Attenuation factor due to oxide coatings.

The LiPb blanket has a surface area of 10^4 m^2 . The corrosion rate in a static system is estimated to be $2 \times 10^{-7} \text{ g/cm}^2\text{-hr.}$ ³³ If the MHD effects dominate the mass transfer in the liquid phase, a dynamic system in a strong MHD environment will resemble a static system. For such a system, the total corrosion rate from the blanket is 175 kg/yr. This appears to be an acceptable amount.

Corrosion product transport in a liquid-metal system is significantly different from transport in a non-liquid-metal system. For a liquid-metal system, the Prandtl number is much smaller than the Schmidt number, or the heat transfer is much faster than mass transfer. Therefore, the liquid metal in the center of the flow region will be cooled to supersaturation before the mass in the center of the flow region is about to diffuse out. This is the reason for the well-known fact that dissolved material in a liquid metal will form particulates and be suspended in the flow when the flow is being cooled. In contrast, in a regular coolant, the dissolved material will be deposited on the wall.

As the coolant passes the heat exchanger, the majority of the corrosion products will be suspended in the flow and pass through the heat exchanger. Various methods can be used to partially separate the suspended particulates and the coolant. The methods tried in other liquid-metal systems are filtration processes and magnetic separation. We are still working to determine which is the preferable process and its separation efficiency.

4.2.6 Blanket Maintenance

4.2.6.1 Maintenance Philosophy

The components of the central cell must be replaceable. By synthesizing and comparing several design options, we have developed a straightforward maintenance approach that emphasizes high availability and totally remote handling in two distinct maintenance categories:

1. Routine blanket module changeout. Maintenance operations are performed only on the blankets themselves, so the shield is not cut and the magnets are not moved. Spares are provided and pretested; as one section of the blanket is removed, an immediate replacement is available.

2. **Unscheduled component replacement.** Provision for maintaining the long-term components, such as the coils, the rails on the reflector, and a section of shield, etc., must be included. These components are designed for maintainability in all contingencies. Spares for superconducting magnets, standard reflector, and shield structure are also provided.

Remote maintenance is necessary because of the radiation hazard to plant personnel. Also, the use of remote equipment permits maintenance with a minimal cool-down period. Personnel access is not allowed once the shield is removed.

The design philosophy results in the following maintenance design features:

1. The cylindrical blanket module lying on the rails attached to the reflector structure is capable of being translated axially within the reflector tunnel by a specially designed machine.
2. The coolant supply pipe headers--patterned after the WITAMIR-1 design--are designed for remote disconnection and reconnection, which takes advantage of the fact that the $\text{Li}_{17}\text{Pb}_{83}$ coolant/breeder is an excellent solder. A trace heater and a coolant line are located on the flange edge to control the temperature so that the headers can be separated or frozen together with the help of a mechanical, hinged bracket as shown in Fig. 4.2-32.
3. Accessibility to the blanket module is provided by 12 servicing stations, or ports for blanket module removal. The vacuum seals for the ports are flat flanges. In addition, a vacuum barrier is designed for redundancy. Figure 4.2-33 shows the cross section of the servicing stations, each of which services the seven nearest blankets.
4. The axial dimensions are defined in Figs. 4.2-34 and 4.2-35. Because the magnets remain in position during routine maintenance, space is provided to translate the blanket module and manifold to the service station. The inlet and exit pipes pass through the shield in the vertical direction. The attachment position to the blanket varies, which necessitates four different blanket patterns.

Fig. 4.2-32. Pipe connection and vacuum seals.

Fig. 4.2-33. Vertical cross section of the central cell showing the blanket servicing region.

Fig. 4.2-34. Cross section of the central cell showing details of the blanket module.

84 blankets
 12 servicing stations
 Total length of central cell 151.68 m

Fig. 4.2-35. Cross section of the central cell showing the reflected water pipes.

5. The shield and reflector can be cut for replacing a failed component. The cutting positions are shown in Figs. 4.2-34 and 4.2-35 by dashed lines perpendicular to the reactor axis; the details of the structure are shown in Fig. 4.2-36. The upper and lower parts of the piece of the shield surrounding the vacuum seal can be removed separately. Each section of the central cell, including the associated magnets, is capable of being translated radially through the side of the reactor. The magnets can then be translated axially to the right or left and removed.

4.2.6.2 Maintenance Procedure

The routine maintenance sequence would be as follows:

1. Drain the coolant into dump tanks.
2. Heat solid LiPb on pipe connections until molten.
3. Release the mechanical, hinged brackets.
4. Lift all top pipe headers up 1 to 2 cm.
5. Pull out the pipe headers in the servicing stations.
6. Open flanges at servicing stations.
7. Withdraw the bulk shield and built-in reflector in the servicing stations.
8. Extract the blankets immediately below.
9. Translate the six adjacent blankets to each servicing station.
10. Repeat step 8.
11. Reverse steps 3 to 10 for inserting new modules.
12. Bake-out the whole reactor system except the flange of the coolant pipe headers.
13. Evacuate.
14. Reverse step 1.
15. Restart.

Fig. 4.2-37 shows the exploded view of the maintenance procedure.

We are investigating a timetable for each of the above operations. We anticipate that the most time-consuming aspects are steps 6, 9, 12, and 13, including their reverse operations (where applicable).

Fig. 4.2-36. Details of the vacuum seal weld for reflector disassembly (schematic).

Fig. 4.2-37. Blanket maintenance procedure.

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4.3 HIGH TEMPERATURE BLANKET

The principal goal of the high temperature blanket design is to produce high temperature heat for synthetic fuel or electricity production. This year we have concentrated on electricity production; the blanket design has been optimized in conjunction with the energy conversion cycle to maximize thermal-to-electrical efficiency. Additional goals that have had a significant effect on the design are manufacturability, low tritium inventory, no reactive liquid metals, no exotic materials, and a maintenance concept that allows all central cell components to be replaced as part of normal operations.

4.3.1 Blanket Design

The MARS high temperature blanket concept is pictured in Fig. 4.3-1. All structural steel (HT-9) is in direct contact with $\text{Li}_{17}\text{Pb}_{83}$ for cooling and to minimize neutron capture in the steel. The high temperature zone consists of pods containing silicon carbide pebbles doped with 2% lithium aluminate for breeding tritium and cooled with 80 atm helium exiting at 900°C.

The major goal of the high temperature blanket design is to meet the dual purpose of high efficiency electricity or synfuels production. Performance goals include a high temperature (900 to 1000°C) heat fraction greater than 40%, tritium self sufficiency ($\text{TBR} > 1.05$), and a 3-to-4 yr first wall/blanket lifetime. Major safety goals are low tritium inventory (less than 1 kg), no reactive liquid metals, minimum long-term activation, and a passive loss of coolant flow safety system. Other design goals are the elimination of exotic materials (because of limited resources and costs), a blanket and shield thickness less than 1.85 m to reduce central-cell magnet costs, and easily removable magnets to simplify module change out and magnet replacement.

4.3.1.1 Performance Summary

Neutronics analyses show that the high temperature blanket has an energy multiplication M of 1.15, a TBR of 1.13 and a deposited fraction of high temperature heat FHT of 0.48. First wall damage is 69 dpa/FPY. Thermal losses from the high temperature to low temperature zones reduce FHT to 0.46. A maximum structural steel temperature of 520°C is expected, with a 24°C maximum ΔT through the first wall. Silicon carbide temperatures are expected to remain below 1000°C with the 900°C helium exit temperature. The helium

Fig. 4.3-1. High temperature blanket module.

pumping power in the blanket is calculated to be 1.6 MW. Helium inlet/outlet temperatures are 450 and 900°C, and lithium-lead inlet/outlet temperatures are 372 and 482°C. The 3-to-4-calendar year lifetime at 80% availability corresponds to 12 to 16 MW yr/m² at the first wall. Fusion blanket lifetime studies remain speculative; however, we consider 3-to-4 calendar years reasonable at 5 MW/m² wall loading.

The high temperature blanket and energy conversion cycle have a gross thermal to electrical conversion efficiency of 46%. Tritium breeding in the high temperature zone is necessary to simultaneously achieve tritium self sufficiency and a high FHT. Using a small amount (2% by volume) of lithium aluminate reduces the problems associated with sintering while exceeding the tritium and heat fraction goals. Tritium inventory in the solid breeder is predicted to be about 100 g.

We are currently studying blanket lifetime. At this time we can only estimate the effects of swelling, embrittlement, and creep of structural steels at the fusion neutron fluences and energies on lifetime because we have only recently begun studies of the combined effects of these phenomena. The blanket has been designed to tolerate swelling by allowing room for expansion. Predicted swelling and creep rates and neutron effects on ductile-to-brittle transition temperature are given in Section 4.3.1.8.

Tritium inventory in the solid breeder is predicted to be about 100 g. Activation is minimized by materials selection. The most activated materials in the blanket are elements added to the steel such as molybdenum, nickel, and manganese. HT-9 with isotopically tailored molybdenum may be a feasible low activation structural material. Minimizing the steel in the blanket also reduces the amount of activated material. We have not yet analyzed LOCA and LOFA. The two coolant blanket design should prove advantageous if one of the coolants is lost.

We have successfully avoided exotic materials in this blanket concept. The blanket and shield thickness of 1.6 m has not yet been fully optimized; further work is expected to reduce this value. The current magnet mounting system is designed for simple removal. Work is continuing on the magnet mounts to reduce heat leak.

The major issues associated with the high temperature blanket design are: LiPb corrosion, structural lifetime, MHD effects on liquid metal cooling, and tritium inventory in the solid breeder. LiPb corrosion of HT-9 is currently being investigated in the fusion community. New experimental results are not likely to be available in time to affect the design. We can only roughly estimate the lifetime of structures in the fusion environment, and this will remain an open issue for all fusion reactors until better analytical tools and experimental results are available. Tritium hold-up in the porous silicon carbide and the lithium aluminate solid breeder is expected to be small. However, neutronic effects on sintering, radiochemical effects on solubility, and precise rates of diffusion and removal are not well known.

4.3.1.2 Module Description

The MARS high temperature blanket is a two temperature zone blanket: a helium-cooled high-temperature (900°C) zone and LiPb-cooled low-temperature (500°C) zone. The blanket structural material is HT-9 ferritic steel kept below 520°C by the LiPb coolant. Each blanket module is composed of a cylindrical first wall structure and two LiPb end-closure plena; a set of 12 pods (submodules) that are suspended within the LiPb form the high temperature zone. The entire mass plus the magnets are supported by the shield structure (see Fig. 4.3-1).

Low-Temperature Zone Structure. The low temperature structure is cooled by axially flowing LiPb surrounding the pods. All steel components are kept below 520°C. This region consists of: (1) the first wall structure, (2) interface panels between pods, (3) an edge support panel between the shield and back of the pods, (4) the pod support rails, and (5) the end plenum closures. These are shown in Figs. 4.3-2 and 4.3-3.

The first wall consists of a cylindrical, corrugated sandwich wall of HT-9. The composite wall is 5 cm thick, with a 0.5-cm first wall and a 1.5-cm second wall. The corrugation stabilizes the structure and provides flow direction for the LiPb [see Fig. 4.3-3(a)]. Corrugated steel interface panels are placed between adjacent pods to provide LiPb flow passages for cooling and structural support for the pod sidewalls [see Fig. 4.3-3(b)]. The edge support panels are similar to the interface panel, consisting of corrugated, sandwich steel panels placed between the back wall of the pods and the shield to pro-

Fig. 4.3-2. High temperature blanket structure.

Fig. 4.3-3. Blanket first wall and interface structure.

vide LiPb flow passages for cooling, and to transfer the helium pressure load from the pods into the shield structure [see Fig. 4.3-3(c)]. The pod support system is a T-section support rail of HT-9 attached to each back corner of the pods. The support rails are mounted to the shield structure for support during assembly (see Fig. 4.3-3). The end plena consist of an annular end closure cap (5 mm thick) welded to each end of the first wall and the blanket shield (see Fig. 4.3-2).

High-Temperature Zone Structure. The high temperature pod is a 30-deg circular-sector-shaped pressure vessel constructed of HT-9. The vessel is 592 cm long and 100 cm high with a nose radius of 23 cm. The vessel wall is 1.1 cm thick with a 2.1-cm-thick semi-elliptical dome welded to each end, as shown in Fig. 4.3-4. The steel vessel wall is maintained at or below 520°C by LiPb circulating on the outside of the pod.

Silicon carbide (SiC) spherical pebbles (3.4 cm in diameter) and support panels (3 cm thick) are supported on a 1 cm thick SiC basket. The pebbles contain 2 atomic % lithium aluminate. The basket supports the pebbles and panels and separates them from the steel vessel. Thermal insulation (low-density alumina-silica mat) fills a 1-cm gap between the steel vessel wall and SiC basket. The gap is provided by 1-cm-high ceramic spacing pedestals placed between the steel vessel wall and the basket in alignment with the support panels (see Fig. 4.3-4). There are 23 support panels in each pod, spaced at 20-cm intervals, except in the central helium inlet/outlet area. The support panels preclude a domino failure in which a sudden loss of pressure in one pod causes adjacent pods to burst in series.

Module Structures. The shield is the main structural skeleton of the high temperature blanket. The end plena and high temperature pods are attached to and supported by the shield. Because the shield, magnet supports, piping structures, and module support have been integrally designed with the blanket, these structures are described here.

Magnet Support. The shield structure acts as an integrated box beam and as such is designed to transmit loads resulting from the magnet weight (200 tons) and critical failure condition (13,000 tons axially). The magnet loads are transmitted to the shield via the eight-pin joint-attach fittings shown in Fig. 4.3-5, which are secured to a continuous circumferential-structural ring that attaches through bolt patterns to the shield.

Fig. 4.3-4. Helium pod.

Fig. 4.3-5. Magnet support structure.

This type of support has several unique design advantages that permit minimum effort for removal of the magnets, restrict paths for minimizing magnet heat loss, and can accommodate magnet failure loads. Magnets can be separated from the module by removing the shear bolts used to secure the structural ring to the shield. Enough stiffeners are provided to ensure minimum out-of-round deflection of the ring. Conduction heat paths are intercepted at the pin joint by a 2.5-cm polyimide insulating collar. Active liquid-nitrogen cooling can be introduced within the pin (by providing a 1.5-in.-diameter hole) to increase the collar effectiveness. Additional intercepts can be provided by isolating the magnet support arms with a pin/truss type structure.

The superconductor will contract during chilldown by approximately 2 cm. This deflection, as well as vertical alignment tolerances, can be accommodated by incorporating vertical translation slots into the support . . . fitting. Because of the geometry of the magnet support structure, this also defines a single predictable load path for the magnet weight; the magnet is essentially hung from the top pin. Because the support pins are sized for the failure load, they provide a simple method of supporting the weight while relieving tolerance requirements.

The liquid-nitrogen magnet heat shield is attached directly to the magnet vacuum case by spacers, while the vacuum case itself is directly supported on the pin and is therefore free from physical contact with the superconductor. The vacuum is isolated at the pin joint by insulating vacuum seals.

Lithium-Lead and Helium Piping Structures. Figures 4.3-6 and 4.3-7 show the flow paths for the LiPb and helium coolants. The relatively low temperature and pressure requirements for the LiPb allows the use of rectangular flow paths with conventional steel plate construction. The shield construction geometry allows the water and LiPb flow paths to be separated from each other by double wall construction, and also allows the separation of any interface weld joints. The high-temperature, high-pressure helium pipes are insulation-lined.

Assembly Structural Support. The module weight and magnet loads are carried to the floor with conventional plate steel construction. This support structure uses box-type construction to maximize rigidity and redundancy. Loads can be reacted in the floor through grips and/or bolt tie downs.

Fig. 4.3-6. LiPb coolant flow paths.

Fig. 4.3-7. Helium coolant flow paths.

Reactor alignment is provided by module-mounted jacks. Transhaulers are used to move the modules into the reactor for positioning and alignment. Six of these rail-type movers are required to move a module. They are remotely operated, permit 360-deg module movement and incorporate a vertical lift stroke of 19 cm.

4.3.1.3 Structural Analysis

First Wall. The first wall is a cylindrical shell of corrugated sandwich construction with the corrugation oriented in the axial direction. This shell is subjected to an external pressure load from the LiPb. The shell first-wall structure is classified as an orthotropic shell. Reference 1 gives the procedures used to analyze sandwich cylinders. However, if the structure has to be designed for a specific lifetime at an elevated temperature, an isochronous stress-strain diagram for the material is required. The ASME Boiler Code² only contains procedures for analyzing monocoque cylindrical shells under external pressure. To use the design procedures of the boiler code, we derived an equivalent cylinder thickness based on the sandwich shell having equal stiffness in both the hoop and axial directions for a sandwich construction of a total thickness of 5 cm, with the outer facing 1.5 cm thick and inner facing 0.5 cm thick; the equivalent thickness is computed to be 4.05 cm. The allowable pressure is not currently available in the boiler code for HT-9. We used the allowable for nickel-iron-chromium alloy 800 because the mechanical properties for this alloy are close to those of HT-9. The design based on this analysis is rated for 200-psi allowable external pressure, which produces a hoop stress of 5.9 ksi. Boiler code section 4.2.12.1 lists the 100,000-hr creep rupture stress of 16 ksi for HT-9. Hence we have concluded that the chosen geometry satisfies the design requirements.

Pod Analysis. The pods are basically single-wall HT-9 construction under internal pressure of 80 atm. A corrugated sandwich panel is inserted between the side walls of the adjacent pods to reduce the bending stress on the flat portion of the pods. The nose section of the pods is of cylindrical construction. The minimum thickness of the nose section was obtained using VG-27(C) (1).² Using an allowable stress of 28 ksi (i.e., 2/3 yield stress), we computed the minimum thickness of the pod nose section to be 1.4 cm.

The design has 23 SiC panels 3 cm thick. These panels are of nonuniform width to accommodate the pod section and have cutouts. If a fault condition arises where the pressure in a pod drops to zero, the pressure from the pods on either side has to be resisted to avoid a propagating failure. The structures that resist these forces are the side wall acting as a sandwich panel under bending and the SiC panels acting as plates under compression. A preliminary analysis showed that these panels do not have a problem resisting the bending and compressive loads.

The side walls of the pods can be treated as flat rectangular sandwich plates supported by SiC panels. Other considerations dictate a maximum spacing of 100 cm between the silicon carbide panels near the helium entrance piping. Treating this 100-x-70-cm panel as a simply supported sandwich plate and using the procedure outlined in Ref. 3, we computed that a pressure of 850 psi produces a maximum bending stress of 42 ksi. In bending, the boiler code allows the stress level to reach as high as the yield stress. This means that the sidewalls of the pods can withstand a pressure drop of 850 psi as a fault condition. Analysis has shown that an 8-in.-diameter hole in a pod will cause a maximum pressure drop of 350 psi between adjacent pods.

End Closure Analysis. There are two types of end closures: individual pod end closures of approximately semi-elliptical dome shape under an internal pressure of 80 atm, and a toroidal ring end plenum under an internal pressure of 200 psi. For preliminary analysis purposes, both end closures may be approximated as ellipsoidal heads with a ratio of 2:1. Boiler code² equation V6-32d is used to determine the minimum thickness. This results in a minimum thickness of 2.1 cm for the pod end closure and of 0.5 cm for the end plenum.

Magnet Support Analysis. The magnet support consists of eight double-shear-pin assemblies that transfer magnetic and gravitational forces from the magnet into the support structure. The support structure then transfers these forces to the ground. The gravitational forces are from the weight of the magnet and the structure. The magnetic forces are caused by a fault condition where one magnet deenergizes but the others keep functioning. This results in a 2.7-x-10⁷-lb load axially applied on the magnet center line.

Materials and dimensions of some of the magnet support components have not yet been specified. We have analyzed the key areas of high stress (the support pins and the magnet mounting bolts). A von Mises failure criterion is

assumed for a biaxial state of stress with a safety factor of 1.5. The pin was analyzed as two short cantilevers, rigidly fixed at the center section. Because of the shear and bending, the 10-in.-diameter pin material needs to have a yield stress of ~140 ksi (e.g., SS 304LN). When the material is selected, a fracture analysis will be performed to avoid catastrophic brittle failure. The bolts mounting the magnet assembly to the module support structure require a shear yield strength of 80 ksi (e.g., heat-treated SS 316). A total of 20 2-in.-diameter bolts is required for each pin support assembly (10 per side).

Module Structural Support Analysis. Module structural supports are also in the preliminary stages of design. The critical section for the support occurs midway down where a simple box structure resists the bending moment and shear. At this section, assuming 2-in. thickness on inner walls and 4-in. thickness on outer walls, material yield stress should be on the order of 64 ksi. The base section for transfer of the loads to the ground should have a yield stress of 78 ksi. Because high-strength heat-treated materials are impractical for the large support structures, these areas will be reinforced. We have reported the strength requirements for this first cut design because they show that the stresses are within reason, requiring less than a factor of two reduction to allow the use of any of several steels.

Main Areas for Study. We have presented the results of our preliminary analysis for the first wall, pods, and end closures. A detailed analysis is still necessary, including thermal stresses based on realistic values of thermal gradients and stresses due to swelling of the materials, if any. A detailed analysis of the pod support system and the discontinuity areas of the structure is also necessary. The structural integrity requires that fracture-mechanics-based crack growth analysis be made to show that there is no fatigue crack growth problem.

4.3.1.4 Neutronics

Our neutronics analyses have concentrated on the analysis of module lifetime, tritium self-sufficiency, and the fraction of energy deposition in the form of high temperature heat. We have covered the following areas:

1. Blanket ceramics (other than graphite) to overcome swelling

- problems (lifetime constraint).
2. Addition of lithium-bearing compounds to the solid breeder (tritium production constraint).
3. Addition of a thick, frontal LiPb zone to enhance breeding and reduce parasitics in the solid breeder (tritium production constraint).
4. Dimensional adjustment to balance tritium production, energy multiplication, and FHT.
5. Three-dimensional calculations to fine-tune the design.
6. Burn-up calculation to verify adequate module lifetime and demonstrate adequate module performance over that lifetime.

One-Dimensional Neutronics Analyses. Table 4.3-1 presents selected results from a series of one-dimensional (1-D) calculations to evaluate the thickness of the front LiPb zone and thickness of the silicon carbide "breeder" region. The LiPb has natural lithium, while the breeder has 1 vol % LiAlO_2 , 90% enriched in ^6Li , in SiC. Earlier neutronics results indicated tritium production in the breeder was clearly dominated by the ^6Li (n,T) reaction; to reduce sintering, the volume fraction of lithium-bearing compound was minimized by ^6Li enrichment. We selected LiPb as the lithium-bearing compound for reasons of its chemical compatibility with SiC, as is discussed in Section 4.3.1.9.

Tables 4.3-1 and 4.3-2 present results of selected calculations of PbLi lithium enrichment and volume fraction of LiAlO_2 for fixed blanket dimension corresponding to a front LiPb zone of 5 cm and a breeder zone of 1 m.

Table 4.3-3 shows spatial energy deposition for the one-dimensional model shown in Fig. 4.3-8: FHT is equal to the higher of the bracketing values given in Table 4.3-2. The FHT bracketing calculations are done by first assuming that the LiPb at the pod edge is to be ignored (generating the lower limit) and second by assuming the LiPb at the edge is uniformly distributed throughout the pod (generating the upper limit). As input to thermal analysis, the upper limit on FHT is the most conservative.

Three Dimensional Neutronics. Figure 4.3-8 shows the cross-sectional equivalence between 1- and 3-D neutronics models; not shown are the axial zones corresponding to LiPb plena and module ends. The 3-D model was deemed

Fig. 4.3-8. One- and three-dimensional neutronics models (axial details not depicted).

Table 4.3-1. Blanket thickness analysis.

Thickness of front PbLi (cm)	Thickness of breeder region (cm)	M	TBR	FHT	First wall damage (dpa/yr)	Leakage
10	100	1.25	1.31	0.54	71	9.3 E-4
14	148	1.26	1.38	0.62	74	4.5 E-5
14	60	1.29	1.23	0.45	73	6.6 E-3
5	148	1.22	1.25	0.74	68	8.6 E-5
5	60	1.25	1.04	0.53	66	1.1 E-2

Table 4.3-2. Lithium enrichment analysis.

⁶ Li Enrich- ment in PbLi (%)	Fraction LiAlO ₂ in SiC breeder	M	TBR	FHT	First wall damage (dpa/yr)	Energy deposition (W/cm ³)
7.6 ^a	0.01	1.24	1.21	0.35 to 0.53	69.0	38.0
7.6	0.02	1.23	1.24	0.39 to 0.54	69.0	37.8
1	0.01	1.26	1.13	0.41 to 0.54	69.4	39.2
1	0.02	1.24	1.19	0.44 to 0.56	69.4	38.7

^a Natural.

Table 4.3-3. Blanket energy deposition.

Zone	Inner radius (cm)	Material	Energy deposition (MeV/source neutron)
1	62	Steel	0.569
2	62.5	LiPb	2.498
3	65.0	Steel	1.695
4	67.0	LiPb	2.361
5	72.0	LiPb	0.267
6	72.75	Breeder+steel+LiPb	1.345
7	77.5	Breeder+steel+LiPb	0.092
8	82.5	Breeder+steel+LiPb	0.874
9	87.5	Breeder+steel+LiPb	1.462
10	92.5	Breeder+steel+LiPb	1.143
11	102.5	Breeder+steel+LiPb	1.543
12	112.5	Breeder+steel+LiPb	0.948
13	132.5	Breeder+steel+LiPb	0.449
14	152.5	Breeder+steel+LiPb	0.013
15	171	Steel	0.107
16	172	LiPb	0.107
17	182	Shield	<u>0.241</u>
Total			17.65
FHT			0.56

necessary to adequately assess the axial competition for neutrons between silicon carbide regions without LiPb and the LiPb-filled regions between adjacent pods and at the module ends. Because the LiPb regions are not intimately dispersed within the breeder material, 1-D analysis will tend to overpredict tritium production and underpredict parasitic capture. FHT is also difficult to treat adequately in 1-D analyses. (Table 4.3-2 shows the bounds.)

The 3-D performance parameters are seen to be 1.13, 1.15, and 0.48 for TBR, M, and FHT, respectively. These compare to the 1-D parameters of 1.24, 1.19 and 0.44 to 0.56. It is worth noting that the depletion of ^6Li in the LiPb diminishes the disparity between the 1-D homogenized zone descriptions and the 3-D system they model. Were the MARS high-temperature blanket design to use natural or enriched lithium in LiPb, the 1-D results would tend to further overpredict tritium production and energy multiplication.

Burnup Calculations. Using a solid breeder for both tritium breeding and high temperature heat production requires a time-dependent calculation to evaluate the effects of burnup. Lithium burnup is particularly important since the neutronics performance is affected by the size of the regions and the distribution of ^6Li and ^7Li . The LiPb, which is distributed among all the modules and mixed, is assumed to be maintained at a constant concentration. The principal effect of neutron interactions with LiAlO_2 is a reduction in lithium concentration. The ^7Li burnup is almost 3 orders of magnitude less than the ^6Li burnup and will be less than 1% over the life of the module. Therefore, only ^6Li burnup is explicitly calculated. An average burnup rate for each zone is calculated from the 3-D TART run by

$$K = \frac{RF}{L},$$

where

K is the burnup per atom of ^6Li per year,

R is the reactions per source neutron (TART output),

F is the source neutrons per module-year (for a wall loading of 4.9 MW/m^2), and

L is the ^6Li atoms per zone.

The ${}^6\text{Li}$ is then depleted at this rate for one year by $L(T + 1) = L(T)e^{-K}$, and a new 3-D TART run is performed. One-year time steps can allow burnup of 30% of the ${}^6\text{Li}$ in the front zone, which changes the burnup rate by 15%, but has at most a few percent effect on performance. TART is a Monte Carlo calculation; results for individual zones have standard deviations of about 1%; performance summary results, combining several zones, have correspondingly better statistics.

The module performance for 4 FPY is summarized in Table 4.3-4 and in Fig. 4.3-9. In the top portion of Fig. 4.3-9, tritium breeding is shown for four regions. Middle, front, and back refer to radial position in the solid breeder, and liquid refers to the LiPb. In the lower portion, low temperature and high-temperature zone energy depositions are shown. Energy multiplication is the sum of the two divided by 14.1, while FHT is their ratio. As ${}^6\text{Li}$ is depleted (particularly in the front), tritium breeding and energy deposition shift back in the breeder and out to the LiPb. The increase in M is primarily caused by increased neutron capture in lead.

These results demonstrate the neutronic viability of the solid breeder concept. Tritium breeding, based on average breeding from modules of different ages, is sufficient beyond 4 FPY. In the first 3 FPY, performance changes only slightly. The TBR drops only 4% while FHT drops 3% for an individual module. The change in TBR and FHT averaged over the central-cell is a factor of two lower.

First Wall Damage. Damage calculations were performed with the 1-D code ONEDANT. The neutron flux was normalized to 4.9 MW/m^2 at the first wall. The results presented in Table 4.3-5 show the highest damage in the first wall of about 70 dpa/yr.

Areas Requiring Further Analysis. Foremost among the blanket analysis needs will be interactive development of a module-to-module interface design and the effect of the interface on neutronic performance and shielding. Activation analysis of major blanket components will be required as an integral part of the development of a maintenance scheme and as a component of the waste disposal contributions to systems economics.

Table 4.3-4. Burn-up summary.

Time (FPY)	M	FHT	Tritium breeding ratio		
			Total	LiAlO_2	LiPb
0	1.153	0.481	1.126	1.056	0.070
1	1.155	0.474	1.104	1.029	0.075
2	1.162	0.465	1.092	1.009	0.083
3	1.173	0.457	1.087	0.997	0.090
4	1.162	0.448	1.030	0.932	0.098

Table 4.3-5. Neutron damage.

	<u>Radius (cm)</u>	<u>dpa/FPY</u>
First wall		
	62.125	69.3
	62.375	66.4
Corrugation		
	62.75	63.3
	63.25	60.2
	63.75	57.3
	64.25	54.6
	64.75	52.1
Second wall		
	65.5	47.8
	66.5	42.3
Pod wall		
	72.375	27.3
	73.542	24.9
	75.125	22.3
	76.708	19.9
	78.333	17.7
	80.0	15.9

Fig. 4.3-9. Blanket lithium burnup.

4.3.1.5 Thermal Hydraulics

High Temperature Zone. Helium is used as the high-temperature working fluid to carry away the heat absorbed in the silicon carbide and spherical pebbles. Calculation of the heat transfer coefficient in the packed bed is done using a Stanton number correlation⁴

$$St = 0.40 Re^{-0.437} Pr^{-2/3},$$

where

- St = Stanton number = h/GC_p ,
- Re = Reynolds number,
- Pr = Prandtl number,
- C_p = Specific heat,
- G = fluid mass velocity based on unpacked tube, and
- h = heat transfer coefficient.

The temperature drop between the surface of the ball and the surrounding helium can then be calculated.

The temperature distribution in the packed bed varies significantly with pebble radius. Figure 4.3-10 depicts the relationship that exists between the pebble diameter and the film drop profile through the bed. At small pebble diameters, the larger surface area keeps the film drop low, causing a large front-to-back temperature gradient in the surface temperature of the pebbles, side wall, and support blocks. A ball radius exists that produces a nearly flat, pebble-surface temperature profile throughout the bed, with a large film drop in the front of the bed, and a smaller film drop in the pack of the bed. This ball radius is about 1.9 cm as is shown Fig. 4.3-10. At this radius, the structural blocks and sidewalls will not be subjected to large front-to-back temperature gradients. The maximum film drop temperature of 455°C occurs in the front of the bed and maximum SiC ball surface temperature is 910°C. The maximum ball centerline temperature is 950°C.

Helium Fluid Hydraulics. One consideration of using helium as the high-temperature working fluid is the pumping power needed to flow large volumes of the gas through the system. The pressure drop through the module has been calculated for the flow paths shown in Figs. 4.3-2 and 4.3-7. The

Fig. 4.3-10. Effect of pebble size on bed temperature.

coolant operates at 80 atm with T_{in} at 450°C and T_{out} at 900°C. A simplified flow schematic of the helium piping system is shown in Fig. 4.3-11, illustrating the detail of the pressure drop calculation. The total pressure drop is a summation of the pressure drops through inlet/outlet manifold straight pipes and manifold tees, pod distribution and collector pipes, and packed bed.

To estimate the pressure drop through the pipes, we used a conservative value of friction factor to account for high-temperature piping insulating material. This value may be relatively rough. We calculated the pressure drop in the manifold and pod entrance/exit tees using the equivalent length method outlined in Ref. 4. This method estimates the equivalent length of straight pipe that would cause the same frictional pressure drop as flow through a standard tee.

We estimated the pressure drop in the pod distribution entrance pipe and the pod collector pipe according to the procedure outlined in Ref. 5. The method provides empirical data for calculating the pressure change through holes of 90-deg turns of flow into and from a branch side duct.

We calculated the pressure drop in an incompressible packed bed composed of nearly spherical particles as described and developed in Ref. 4. The continuity equation was our basis for calculating the bulk velocity of the helium in the packed bed. The total pressure drop through the module helium piping system is 5.6 psi. The pumping power output requirements is 0.067 MW per 6.3-m module.

Further work must be done to size the holes in the pod diffuser and collector for proper distribution of the flow through the pod. Also, flow orifices will be needed in the large feed manifold.

Low Temperature Zone. The first wall coolant, LiPb, is a conducting liquid metal; the effect of the strong magnetic field on the heat transfer of the coolant must be considered. Not only is the Reynold's number (Re) used to describe the fluid flow in the first wall, but an additional parameter, the Hartmann number (Ha) is used to describe the flow regime:

$$Ha = a B \frac{\sigma_L}{\mu} ,$$

Fig. 4.3-11. Simplified schematic of helium system.

where

- a = channel half-width,
- B = field strength,
- σ_L = fluid electrical conductivity, and
- μ = dynamic viscosity.

The factor Re/Ha , which represents the ratio of viscous to electro-magnetic forces, is used as a measure of the importance of the magnetic field effects on the flow of the fluid. Turbulent flow will be dampened at $Re/Ha < 60$ for flow parallel to the magnetic field.⁶ Since Re/Ha for our design is approximately 200, no significant dampening effect is expected to occur. Thus, we used the following turbulent, liquid-metal Nusselt-number (Nu) empirical correlation⁶

$$Nu = 6.5 + 0.025 (RePr)^{0.8},$$

where

$$Pr = \text{Prandtl number.}$$

The main objective in cooling the first wall is to maintain a maximum temperature in the steel below 520°C. The LiPb enters the module at 372°C and exits at 482°C; thus, a total LiPb mass flow of 3600 kg/s is needed to cool the low temperature region. The maximum heat deposition of 28 W/cm³ occurs in the first wall. Figure 4.3-12 shows the temperature profile at the maximum temperature locations of the first and second walls. The film drops for the first and second walls are 12 and 8°C, while the conduction temperature drops in the first and second steel walls are 24 and 18°C, respectively. The maximum temperature that the steel reaches in the first wall region is about 515°C.

Thermal Interface. To maximize FHT, the heat flow between zones must be kept low. As the number of pods increases or the inner radius of the module increases, the heat leak from the high to low temperature zone increases as a result of the increase in surface area between the two regions. This was one reason for selecting a small number of pods and the small inner radius of the module. The operating conditions of the module also affect the performance of the blanket: the larger the temperature difference between the two regions, the more heat that is lost.

Fig. 4.3-12. First and second wall temperature maxima.

It is necessary to insulate the two regions from one another. The following properties of the insulation are essential: (1) high resistance to radiation damage; (2) the ability to maintain structural integrity of the blanket; (3) low density to minimize internal heat generation; and (4) low effective thermal conductivity. Table 4.3-6 shows the experimental values of thermal conductivity for some low-density fibrous insulators at high temperatures in stagnant gas.⁷ The effective thermal conductivity approaches that of the conductivity of the stagnant gas.

As Fig. 4.3-13 shows, the MARS baseline insulation design consists of ceramic (MgO) pedestals separating the pebble basket and the pod wall, fibrous aluminum silica mat between pedestals with stagnant helium filling the voids. As determined by the structural analysis, the ceramic pedestals are assumed to cover 2% of the total surface area of the interface between the high and low temperature regions. Also shown in Fig. 4.3-13 is the relationship between the amount of heat lost to the low temperature region and the thickness of insulation and the pebble radius. As the insulation thickness increases, the heat leak decreases. At the larger pebble radius (1.9 cm), the heat leak is larger because SiC surface temperature increases at the front of the blanket. For the baseline design of a 1.9-cm pebble radius and insulation thickness of 2 cm, the heat loss to the low temperature zone is only 3.4% of the total high temperature heat.

Fig. 4.3-13. Performance of insulation at various pebble radii and thicknesses.

4.3.1.6 MHD Pressure Drop

Magnetohydrodynamic effects dominate pressure, pumping, and heat transfer in the lithium-lead loop. MHD pumping power is high enough (20 MW) to noticeably affect net plant efficiency, and MHD pressure strongly affects the structural design and lifetime of the first wall. High pressures are more easily handled in piping and toward the back of the blanket where thicker structures can be used and where these structures are not under buckling loads.

As discussed in the preceding section, magnetic fields affect heat transfer by changing the laminar/turbulent flow transition velocity.⁸ Our knowledge of liquid-metal MHD flow is limited because there is a lack of experiments in this area and because of the complexity of possible current paths in changing flow situations.⁹ Experiments have been performed for flow in straight pipes and between conducting plates in uniform magnetic fields.¹⁰⁻¹⁴ However, the flow paths in the MARS high temperature blanket are considerably more complex than those verified experimentally, making the calculational results somewhat uncertain.

The Hartman and end-of-loop pressure drops were calculated using

$$\Delta P = \frac{VB^2 \alpha_w t_w L}{a(1+c)} \quad c = \alpha_w t_w / \sigma \quad (4.3-1)$$

$$\Delta P = 0.062 \sigma VB^2, \quad (4.3-2)$$

where

- V = velocity of fluid,
- B = magnetic field strength,
- α_w = electrical conductivity of wall,
- t_w = wall thickness,
- a = hydraulic radius,
- σ = electrical conductivity of fluid, and
- L = flow path length.

Equation (4.3-1) was used for flow perpendicular to the magnetic field and Eq. (4.3-2) for turns and flow into and out of the field.¹⁵

Figure 4.3-14 and Table 4.3-7 show the locations and magnitudes of the field strengths used and the calculated MD pressure drops. The maximum first wall pressure is 1.32 MPa (194 psi) including the static head.

4.3.1.7 Lithium-Lead Compatibility

The upper temperature for LiPb will be limited by either the creep rupture strength of the structure or by corrosion. Static tests of HT-9 in LiPb have been performed,¹⁶ but are not directly applicable to the flowing conditions and 110° C temperature difference in the high temperature blanket. Dynamic tests of HT-9 in flowing LiPb are being performed at ORNL and ANL. Until these data become available, the maximum LiPb interface temperatures cannot be chosen with confidence. The maximum LiPb/HT-9 interface temperature is 490°C, corresponding to a corrosion rate of 5.9 mg/m²-hr based on the 1000-hr static test data presented in Fig. 4.3-15. These data indicate that the LiPb had reached saturation by 3000 hr at all three temperatures. At 500°C the corrosion rate had apparently already begun to level off at the 1000-hr test. Figure 4.3-16 shows the corrosion rates for the 3000- and 1000-hr tests. The points plotted are weight lost at the end of each static test. The data at 1000 hr may be closer to flowing conditions because saturation has not affected the results as much as in the 3000-hr data as indicated by the curved lines in Fig. 4.3-15. On the other hand, short term surface effects may dominate the 1000-hr tests. We believe that more experimental results are needed. As mentioned above, these data are not directly applicable to flowing conditions, but the temperature dependence indicated by the 1000-hr results in Fig. 4.3-15 may be significant.

At the corrosion rates expected in MARS, the major problems will occur on the cold side of the loop where material precipitates out of the coolant, possibly clogging the valves and steam generator tubes. Methods of removing these corrosion products from the system are discussed in Section 4.3.5.5.

4.3.1.8 Radiation Effects on Structural Materials

As we stated previously, we have chosen ferritic steels for the blanket because of their excellent physical and mechanical properties and their resistance to neutron-induced void swelling and helium embrittlement. Ferritic steels (HT-9 and 9 Cr-2 Mo) have been shown to reach a state of

Fig. 4.3-14. LiPb flow.

Fig. 4.3-15. Results of static corrosion test of HT-9 in LiPb.

Fig. 4.3-16. LiPb/HT-9 corrosion data.

Table 4.3-7. MHD pressure drop.

Position ^a	B (T)	V (m/s)	Flow analyzed	ΔP (kPa)
1	3.0	0.184	Inlet/outlet	68.8
2 ^b	3.5	0.234	Field	315
3	4.0	0.291	Turn	27.7
4	5.4	0.291	Viscous	0.2
5	4.1	0.291	Turn	83.7
6	4.3	0.239	Field	505
7	4.5	0.239	Turn	36.0
8	4.5	1.01	Viscous	19.4
9	4.5	0.239	Turn	36.0
10	4.3	0.239	Field	505
11	4.1	0.291	Turn	83.7
12	5.4	0.291	Viscous	0.2
13	4.0	0.291	Turn	27.7
14 ^b	3.5	0.234	Field	315
15	3.0	0.184	Inlet/outlet	68.8

^a See Fig. 4.3-14.

^b Toroidal flow around module, average velocity.

magnetic saturation at relatively low field strengths (1 to 2 T), indicating that ferrromagnetism will not present a problem in the tandem mirror.¹⁷ We have considered using HT-9 (12 Cr - 1 Mo) and 2-1/4 Cr-1 Mo for the MARS high temperature blanket. These steels are limited in their useful lifetimes by irradiation embrittlement at low temperatures, by swelling, and by creep rupture and liquid-metal corrosion at high temperatures. We have updated and used already-developed¹⁸ design equations for the shift in ductile-to-brittle transition temperature ($\Delta DBTT$), swelling, and creep rupture time.

Neutron embrittlement causes crack propagation at low applied stress. Crack growth rates, and hence failure criteria, are usually based on static fracture toughness measurements (K_{IC}) for plane strain. On the other hand, the majority of reactor designs in the nuclear industry are based on mitigating the shift in the ductile-to-brittle transition temperature. This may be a more conservative approach where crack propagation due to dynamic loading is avoided. We have followed this philosophy in our analysis.

Figure 4.3-17 shows the estimated DBTT for HT-9 during irradiation. The DBTT is calculated for the worst case, which is the minimum structure temperature (388°C), and the shift shown to saturate after only a few months of operation. After operation for about 1 yr, the plant is shutdown for scheduled maintenance and the structure temperature is held constant at 450°C for approximately 60 hr. This allows an almost full recovery of the $\Delta DBTT$ as shown in Fig. 4.3-17. The procedure is repeated once every scheduled maintenance (approximately every year). The annealing procedure is calculated according to a functional fit to the results of the RANEL annealing computer model.¹⁹

Neutron-induced swelling of ferritic steels depends on dpa, irradiation temperature, and chromium content.^(18,20) Iron-chromium-carbon alloys show a systematic dependence on chromium content, as shown in Fig. 4.3-18. Unfortunately, the peak swelling temperature for HT-9 and 2-1/4 Cr-1 Mo is approximately 426°C, occurring within the expected operating temperature range. The predicted first-wall volumetric swelling after three calendar years (80% availability) is 4.0% and 1.8% ($\Delta V/V\%$) for HT-9 and 2-1/4 Cr-1 Mo, respectively. This extrapolation is for a similar heat treatment of steel assuming a predominantly bainitic microstructure.

Fig. 4.3-17. Ductile-to-brittle transition temperature vs radiation and annealing time.

Fig. 4.3-18. Effect of chromium content on swelling of ferritic steels.

The lower swelling of 2-1/4 Cr-1 Mo, as compared with HT-9, is the primary reason for considering it for interest in the MARS design. However, the high yield and thermal creep strength of HT-9 mean that less material will be required (thinner sections), resulting in lower costs, less activated material to dispose of, and possibly reduced swelling-induced stresses. Swelling-induced stress can be dependent on the thickness of structural members when temperature (and therefore swelling) differentials are dependent on thickness, and when swelling strains are taken up in bending. The temperature drop through the first wall causes a swelling differential across the wall, inducing stress in the section. Table 4.3-8 compares the two alloys in this respect. The first wall thickness is assumed to vary inversely with yield strength, the peak swelling temperature (426°C) is assumed to occur on the inside surface of the first wall, and the section is assumed to be constrained. Stress relief due to creep is ignored. The stress in HT-9 is 82% of its yield strength and the stress in the 2-1/4 Cr-1 Mo is 86% of yield.

Swelling strain (elongation) of the first wall parallel to the plasma axis is taken up in bending of the toroidal end caps. Because the stronger HT-9 will allow thinner end caps than 2-1/4 Cr-1 Mo, lower stress would be induced in the HT-9 by the same amount of bending. The HT-9 will, of course, swell more and therefore bend more, but the thinner section and higher working strength will tend to cancel the higher swelling.

It has been shown that irradiation creep significantly relaxes the swelling-induced stresses.²¹ As shown above, the stresses generated by swelling differentials will not lead to plastic deformation at the end of life (3 yr) for either HT-9 or 2-1/4 Cr-1 Mo. The fractional stress is slightly higher in 2-1/4 Cr-1 Mo than in HT-9. When irradiation creep is included, the stress in the first wall will be a function of irradiation time. The magnitude of the stress is determined as a balance between swelling and irradiation creep. Watson²¹ has shown that, for a clamped plate, the steady-state residual stress is proportional to the ratio of swelling to creep compliance. Even though the swelling is a factor of two smaller in 2-1/4 Cr-1 Mo than in HT-9, the magnitude of creep compliance is uncertain. It is premature, therefore, to make a final selection before a complete analysis is performed.

Table 4.3-8. First wall temperature-dependent swelling-induced stress. Creep relaxation is neglected.

	Material			Material	
	HT-9	2-1/2 Cr - 1 Mo		HT-9	2-1/2 Cr - 1 Mo
Yield strength (MPa at 400°C)	290	218	Swelling difference (%)	0.61	0.46
Thickness (cm)	0.50	0.67	Young's modulus (GPa at 400°C)	175	183
Thermal conductivity (W/MK)	29	34	Induced stress (MPa)	±238	±187
ΔT (°C)	24	32	Percent of yield	±82	±86

The slow deformation of structures at high temperature under applied stress will eventually lead to failure. Creep will also tend to relieve swelling and thermal stress. In some areas creep and swelling can combine to cause excessive elongation. For example, swelling and creep will cause the first wall of the MARS high-temperature blanket to grow in axial length because of the tension caused by the LiPb pressure acting on the toroidal end caps. Creep can therefore limit lifetime by contributing to excessive deformation or by creep rupture. Thus, total deformation at end of life as well as the time to rupture should be quantified. Ghoniem and Conn^{18,20} have developed a method to calculate time to rupture. It appears that HT-9 will not be creep-rupture-limited below about 520°C. Swelling elongation of the first wall is predicted to be 1.3% or 8 cm over the length of the module (3-yr lifetime). We have not yet calculated creep elongation, but we expect it to be small compared with swelling elongation because the stress in this direction is low.

We have chosen HT-9 over 2-1/4 Cr-1 Mo because its strength advantages were judged to outweigh the problems associated with swelling. The higher yield strength and creep strength of HT-9 allow the use of less structural material. This affects cost both by reducing the amount of material itself and because of easier manufacturing and handling of the thinner sections. Less material also results in lower activation. The HT-9 contains 1/2% nickel, while 2-1/4 Cr-1 Mo does not, but long-term activation of both steels is dominated by molybdenum. Isotopic separation of molybdenum or substitution of molybdenum with vanadium would greatly reduce structural activation, making it desirable to reconsider using 2-1/4 Cr-1 Mo to eliminate the nickel in HT-9.²⁰

The study of fusion blanket lifetimes is in its infancy. Analyses of the combined effects of swelling, creep, embrittlement and thermal stresses have been performed in only a few cases.²¹ A finite-element code that includes radiation effects would be a very useful tool in this type of study. Quantification of the combined stresses and failure criteria in complex structures such as the MARS high temperature blanket will require a detailed structural analysis that includes radiation effects on material properties.

4.3.1.9 Solid Breeder Performance

One of our major concerns is the chemical compatibility of the solid breeder materials, including possible chemical reactions between a breeding material and the SiC carrier. Sintering of these materials is also a concern. Although many lithium compounds have been considered as breeding materials, only a few are suitable for the MARS blanket because of its relatively high operating temperature (1000°C). We reviewed the literature on solid breeders and selected potential candidates for the MARS high temperature blanket. Because there is a scarcity of data on solid breeders in the temperature range of interest, some preliminary screening tests were performed as a part of a TRW research project.

Potential lithium compounds have been categorized and discussed by many authors.^{22,23,24} These can be put into several groups including liquid metals (e.g., Li, $\text{Li}_{17}\text{Pb}_{83}$), intermetallics (e.g., LiAl , Li_7Pb_2) binary compounds (e.g., Li_2O , Li_2C_2 , LiF , Li_3N , Li_4Si) and ternary oxides (e.g., LiAlO_2 , Li_2SiO_3 , Li_2TiO_3 , LiFeO_2 , Li_4SiO_4 , Li_2ZrO_8). All compounds that melted or decomposed at less than 900°C were rejected. In addition, any material that was believed to react with oxygen or SiC was eliminated. The most promising compounds remaining were Li_2O and the ternary oxides.

Lithium oxide has a high melting point, high lithium-atom density, good tritium extraction, and relatively low chemical reactivity. However, formation of LiOT from Li_2O can cause serious problems, such as an acceleration of sintering and corrosion of structural materials. Therefore, we turned our attention to the ternary oxides. One of the disadvantages of the ternary oxides is their low lithium density; thus, a neutron multiplier is required. In the MARS high temperature blanket, however, the $\text{Li}_{17}\text{Pb}_{83}$ coolant provides adequate multiplication. The remaining concern was high temperature operation because of self-sintering or sintering to the SiC carrier. Based on melting point and neutronics, we selected LiAlO_2 and Li_2TiO_3 for testing.

Lithium Titanate and Silicon Carbide Heated Separately. We conducted separate tests for possible self-sintering of Li_2TiO_3 and SiC powder. The tests took place in the BREW furnace at 1100°C and 1×10^{-5} Torr for 5 hr. The SiC did not show any sign of self-sintering; Li_2TiO_3 sintered and its color changed from white to bluish gray. The color change of Li_2TiO_3 may be caused by trace impurities. Based on the x-ray diffraction pattern, no

chemical reaction occurred. The SEM micrograph (Fig. 4.3-19) shows a self-sintering of Li_2TiO_3 .

Mixtures of Lithium Titanate and Silicon Carbide. The test conditions were identical to those in the previous experiment. Two mixture ratios were tested; both mixtures (1:1 and 1:6) sintered during heating. The shape of the particles, determined by SEM, seems to indicate both self-sintering of Li_2TiO_3 and Li_2TiO_3 -SiC sintering. The x-ray diffraction shown in Fig. 4.3-20 gives some evidence of a chemical reaction between Li_2TiO_3 and SiC. The Li_2TiO_3 seemed to disappear and new peaks appeared after heating corresponded to Li_2SiO_3 . Unfortunately, the x-ray diffraction spectra of TiC is identical to that of SiC, and the formation of TiC cannot be confirmed by x-ray diffraction analysis. The thermodynamic calculation predicts the following reaction to occur spontaneously:

$$\Delta G^\circ_{\text{reaction}} = -20.305 \text{ kcal.}$$

Although we have not seen definitive evidence, it appears that the above reaction took place during the heating at 1100°C . From this result and based on the thermodynamics, it is also likely that lithium zirconate (Li_2ZrO_3) another solid breeder candidate, will react with silicon carbide to form lithium silicate and zirconium carbide.

Lithium Aluminate and Silicon Carbide. Mixtures of LiAlO_2 and SiC (1:1 and 1:5) were heated to 1100°C in a similar manner as in the previous case. When the furnace was opened, we found that most of the sample powders spilled all over the furnace. An excessive outgassing must have occurred at approximately 300°C . Because of this problem, we carried out a degassing by laboratory box furnace. The LiAlO_2 powder ($100\mu\text{m}$) was heated at 750 to 800°C for 4 hr. The weight loss by heating was approximately 25%. Additional heating at 800°C for 4 hr did not change the weight appreciably. The x-ray diffraction spectra (Fig. 4.3-21) showed that the commercial LiAlO_2 contains a hydrated LiAlO_2 and some Li_2CO_3 .

A thermal gravimetric analysis of LiAlO_2 indicated a weight loss of 21% at 200 to 300°C and 4% at 550 to 650°C . The first weight loss is due to

Li_2TiO_3

Fig. 4.3-19. Li_2TiO_3 after 1100°C 4-hr vacuum exposure.

$\text{Li}_2\text{TiO}_3/\text{SiC}$ (1:1) Before exposure

$\text{Li}_2\text{TiO}_3/\text{SiC}$ (1:1) After exposure (at 1100°C for 5 hr)

Fig. 4.3-20. X-ray diffraction pattern of $\text{Li}_2\text{TiO}_3/\text{SiC}$ (1:1).

Fig. 4.3-21. X-ray diffraction pattern of LiAlO₂.

dehydration of $\text{LiAlO}_2 \times \text{H}_2\text{O}$ and the second weight loss seems to be due to a decomposition of Li_2CO_3 . The hydration number is calculated to be 1.

We again blended silicon carbide and the preheated lithium aluminate to make 1:1 and 1:5 ratios. We then conducted the compatibility test in a similar manner as in the previous case. This time, we did not see an excessive pressure rise as the temperature was increased to 500°C in the vacuum. The weight change was only 1.7 and 0.8% for the 1:1 and 1:5 mixtures, respectively. The x-ray diffraction spectra did not indicate a significant change except for the change of a $\alpha\text{-LiAlO}_2$ to $\gamma\text{-LiAlO}_2$ during the heating at 1100°C , as shown in Fig. 4.3-22. This phase change was also reported by Johnson, et al.,²⁵ The irreversibility of the phase change is reconfirmed by the present work.

Table 4.3-9 summarizes the chemical compatibility test of a lithium compound with silicon carbide. The following conclusions may be drawn from the present study:

1. Li_2TiO_3 reacted with SiC and formed Li_2SiO_3 at 1100°C after 5 hr.
2. Li_2TiO_3 showed self-sintering during heating at 1100°C for 5 hr.
3. Based on the preliminary test results, LiAlO_2 is compatible with SiC at 1100°C ; no evidence of chemical reaction or sintering was detected.

Suggestions for Future Study. Our preliminary test results indicate that the LiAlO_2 and SiC mixture is the leading candidate as the breeder material. Future tests should include tritium solubility as a function of temperature. The effects of radiation damage on solid breeder performance must also be assessed experimentally. Experimental data must be systematically compiled before we can make a conclusion on the $\text{LiAlO}_2\text{-SiC}$ system.

$\text{LiAlO}_2/\text{SiC}$ (1:1) Before exposure

$\text{LiAlO}_2/\text{SiC}$ (1:1) After exposure (at 1100°C)

Fig. 4.3-22. X-ray diffraction pattern of $\text{LiAlO}_2/\text{SiC}$ (1:1).

Table 4.3-9. A summary of chemical compatibility tests of lithium compounds and silicon carbide.

	Weight change (g)	X-ray diffraction	SEM	Observation
SiC	0.472/3.682	No change in the spectra	-	No sintering
Li ₂ TiO ₃	0.444/2.767	No change in the spectra	Self-sintering	Color change, white to bluish gray, self sintering
LiAlO ₂ ^a	5.256/20.351	Substantial change in the spectra--clear peaks of α & γ LiAlO ₂	-	No sintering
Li ₂ TiO ₃ /SiC	0.200/6.146	Disappearing of Li ₂ TiO ₃ peaks--appearing of Li ₂ SiO ₃ peaks	Shows sintering	Color change, gray to black sintering,
LiAlO ₂ /SiC	0.056/3.2239	Disappearing of α LiAlO ₂ , no new peaks	-	No sintering

^a Air heated at approximately 750°C for 4 hr.

4.3.1.10 Pod Rupture Analysis

During steady-state operation, the pressure in all 12 pods should be identical so that there are no unbalanced stresses in the pod walls. We performed a preliminary analysis to determine the magnitude of the maximum pressure differentials resulting between any two pods in the event of a pod rupture, and the blowdown that follows it.

When a pod ruptures, a transient blowdown will occur. During this process all module helium supply and return lines must be closed to prevent additional helium from escaping. All helium present in the pods and manifolds will exit to the plasma region through the failure point. Figure 4.3-23 shows the 12-pod arrangement and also indicates helium flow directions during a failure blowdown.

A worst case analysis has shown that the maximum pressure differential existing between pods across the common wall regions will be 24 atm for an 8-in.-diameter hole and 80-atm operating pressure. Analysis of the pod structure shows that it can withstand a 58-atm differential. The maximum pressure drop occurs near the inner radius of the power blanket across the common pod wall only at the instant of failure, and decrease very rapidly as the blowdown proceeds. Figure 4.3-24 shows the maximum and average pressure differentials as a function of hole size for 50- and 80-atm operating pressures.

The approach used to determine the pressure differential between adjacent pods involves calculating the pressure drop through a pod for a given rupture hole diameter and the corresponding mass flow rate. The total pressure drop through a failed pod consists of three parts. The first pressure drop is from flow through the helium outlet ducting. This is the sum of losses in the straight header section, the perforated manifold, and numerous perforations in the manifold. The second major pressure drop is from flow through the SiC packed bed. The third major pressure drop is from losses through the inlet header and its perforated manifold.

The largest failure size investigated, which is not shown in Fig. 4.3-24, was a tear along the length of the pod 0.5 in. wide and 13.0 ft long, simulating a separation or stress crack failure. The local maximum and average pressure differentials for such a failure at a 50-atm operating pressure are 30.8 and 15.9 atm, respectively. For an 80-atm operating pressure, these

Fig. 4.3-23. Pod rupture flowdown paths.

Fig. 4.3-24. Pod-to-pod pressure differentials vs failure hole size.

values are 50.4 and 29.1 atm, respectively. Thus, even in the case of a split down two-thirds of the length of a pod, the pressure differential is below the 58-atm limit for the structure. We believe that the pod design is strong enough to preclude a domino failure.

4.3.2 Shield

The shield design has only been scoped at this time to show that it is feasible to meet the requirements for the shield. Further design iterations will refine and optimize the concept. The function of this shield is to protect the superconducting magnets from radiation and to protect workers in the reactor area during maintenance. The MARS high-temperature blanket shield uses steel as the shielding and main structural material. It also provides structural support for the blanket and magnets. Figure 4.3-25 shows the shield materials and thicknesses used in the neutronics and thermal hydraulics scoping analyses described below. Further optimization is expected to result in a thinner shield.

4.3.2.1 Shield Design Criteria

The principal criteria for shield design is to protect the superconducting magnets from neutron and gamma radiation. The same magnet shielding criteria discussed in Section 4.2.2.1 apply to the LiPb and high temperature blankets. To reiterate, three values characterize the central-cell shield requirements: (1) 2.2×10^{-5} dpa/FPY in the Cu stabilizer to allow 5 FPY before the first anneal; (2) 2.0×10^8 rad (insulator)/FPY to ensure a lifetime dose less than 5×10^9 rad; (3) 0.06 MW/cm^3 peak heat load in the winding to minimize cryogenic costs.

4.3.2.2 General Structural Description

The primary purpose of the cylindrical shield is to provide radiation protection for the central cell magnets during reactor operation. In addition, the shield has been structurally integrated into and forms a part of the module support structure and, therefore, provides support for the magnets and blanket.

The shield is a box structure supporting the blanket and magnet. It encloses circulating borated water for cooling and lead and polyethylene slabs

Section A-A

Fig. 4.3-25. Shield.

to attenuate neutron and gamma radiation. The 5-cm steel-box floor (inner radius) interfaces with the blanket LiPb coolant (maximum temperature $\sim 500^{\circ}\text{C}$) and pods on one side, and the shield borated water coolant (maximum temperature $\sim 100^{\circ}\text{C}$) on the other. An insulation layer is required in this region. The water coolant is enclosed with 2 cm of steel, which also acts as a support for the two 7.5 cm lead slabs. The three polyethylene slabs are capped by a 5-cm steel cover. Channels located on the shield floor at the LiPb interface are designed to accept the pod support rails.

4.3.2.3 Shield Neutronics

We have demonstrated the feasibility of the shield for the MARS high-temperature blanket. The final design of the shield has not been completed, but it presents little conceptual difficulty. We still need to address the shield design in the vicinity of the module-to-module interfaces.

We performed a one-dimensional neutronics analysis of the shield using the ONEDANT code. The shield geometry is shown in Fig. 4.3-25. Its performance against the criteria is given in Table 4.3-10, and the energy deposition as a function of position is given in Table 4.3-11.

Although the proposed shield fails to satisfy the first criterion, it appears that compliance can be achieved by trading some lead for polyethylene. Future work will address optimization and model plumbing penetrations (e.g., helium and LiPb pipes).

Table 4.3-10. Shield performance.

	Criteria	Performance
Stabilizer damage (dpa/FPY)	$< 2.2 \times 10^{-5}$	3.5×10^{-5}
Insulator dose (rad/FPY)	$< 2.0 \times 10^8$	3.6×10^6
Peak conductor heat deposition (mW/cm^2)	< 0.06	0.053

Table 4.3-11. Energy deposition profile.

Material	Radius (cm)	Energy deposition	
		Neutrons (mW/cm ³)	Gamma (mW/cm ³)
Steel	182.8	2.37	256
	184.5	1.74	301
	186.2	1.19	304
Water	187.8	9.50	34.1
	189.5	4.52	33.0
	191.2	3.25	32.2
Steel	193.0	0.33	212
Lead	195.5	0.039	134
	198.5	0.035	9.6
	201.5	0.029	2.5
	204.5	0.024	1.3
	207.5	0.020	1.7
Polyethylene	210.4	0.346	0.17
	213.1	0.168	0.20
Borated polyethylene	215.1	5.58	0.18
	216.4	0.507	0.17
Steel	218.3	0.0115	0.87
	220.8	0.0092	0.31

4.3.2.4 Shield Thermal Hydraulics

The shield configuration is shown in Fig. 4.3-25. The thermal-analyzer finite-element computer model²⁶ was used to predict temperature profiles in the shield. One requirement is to maintain a maximum temperature in the polyethylene below 120°C.²⁷ Also, the heat leak from the LiPb region should be low, because the heat carried out by the water will be low temperature heat. It is necessary to keep thermal gradients in the shield low to maintain structural integrity.

Insulation will be required between the LiPb in the back wall region and the shield. Without this insulation, two design problems exist: (1) a large temperature differential on the order of 300 to 400°C will occur through the first 5 cm of the shield, and (2) a large heat leak on the order of 16 W/cm² will occur between the LiPb region to the water-cooled shield, causing intolerable film drop temperatures in the water cooling system.

To keep the water pressure around 100 to 150 psi, the inner shell must be insulated from the LiPb so that the maximum temperature of the inner shell is around 150 to 200°C. This decreases the heat leak and reduces the film drop on the inner shell side to a reasonable value. If the inner shell of the shield is constrained at 165°C and the maximum water temperature is 56°C, polyethylene reaches a temperature of 120°C, which just meets the maximum service temperature. Thus trace cooling will not be needed at the outer shell of the shield. The film drop temperatures are 53°C on the shield side and 84°C on the blanket side.

The 6.32-m-long shield module section must accommodate about 1.9 MW of heating, not including the heat leak from the LiPb region. Assuming an inlet water temperature of 40°C and a temperature rise of 16°C, a water flow rate of 28 kg/s is required. The water velocity of 0.146 m/s will cause a very low pressure drop through the channels, requiring only 0.14 W of pumping power per module.

4.3.3 Seals

4.3.3.1 Vacuum Seals

The module interface with each other through module-to-module seals designed to maintain the reactor vacuum and yet, for safety, allow the joint to be sealed and disconnected by remote operation. Studies indicate the seal can use brazing techniques, allow for verification of each joint without requiring a reactor vacuum, and yet be flexible enough to permit deflections resulting from module temperature expansions. Figure 4.3-26 shows a schematic of the intermodule seal concept.

The flat seal plate is closed against the grooved seal plate on the adjacent module by pressurizing the bellows. The bellows chamber contains a steel plug for shielding. Backing plates guide and support the movable bellows, seal plate, and shield plug assembly. The seal is formed by allowing a molten brazing alloy to flow into the grooves in the stationary seal plate surface. Resistance heating melts the alloy and warms the flow paths. This allows a relatively low pressure in the bellows to position the seal. Two seal grooves allow the seal integrity to be tested through a port between the grooves.

4.3.3.2 Remote Actuated Brazing Lithium-Lead Connections

For protection against potential tritium residue and to minimize disengagement time, the LiPb pipe connections are also designed to be operated remotely. Because of the relatively low operating temperatures, the LiPb joint can be brazed with a design similar to that used in the module vacuum seal. The joints use brazing metal from a reservoir that is allowed to flow into two areas located between the two surfaces to be joined. Heating coils in the joint areas ensure continuous flow of the brazing material. Cooling jackets, if required, are provided to ensure that the brazing compound remains confined while the seal hardens. A pressure/vacuum test connection port to a chamber between the two joints allows the integrity of the seal to be verified. The concept is shown in Fig. 4.3-27.

Features

Metal sealing
Remote coupling/decoupling
Seal verification

1. Bellows
2. Backing plate
3. Pressurization feed
4. Seal plate
5. Shield plug attach
6. Brazing alloy reservoir
7. Alloy drain
8. Insulation
9. Alloy distribution groove
10. Shield plug
11. Seal base
12. Module clearance
13. Test connection

Fig. 4.3-26. Module-to-module interface seal.

Fig. 4.3-27. LiPb pipe coupling.

4.3.4 Tritium Removal

Tritium is produced in the high temperature blanket at a rate of 450 g/day. The tritium is bred in a solid lithium-containing material and diffuses into the helium coolant. Tritium inventory in the solid breeder is estimated at 100 g. A partial pressure of oxygen on the order of 1 Torr in the helium maintains the tritium primarily as T_2O . By maintaining the chemical form of tritium as T_2O , the containment and recovery of tritium is considerably easier than containment and recovery in a system that contains molecular tritium (T_2). This is because T_2O does not permeate materials as does T_2 , and T_2O can be recovered from helium with present-day technology while the technology for recovering T_2 from helium is less developed. The tritium path through the high-temperature helium loop begins in the breeding blanket, then passes through the recirculating helium (which comprises the power loop), and finally to recovery from the helium by physical adsorption. A small amount of tritium, on the order of 50 g/day, is bred in the LiPb. It is recovered by a flash unit operation.

Tritium loss from the process system occurs mainly in the form of T_2O the high-temperature power loop through turbine and compressor leaks. Additional loss is as T_2 permeation and leaks from the LiPb loop. Accidental release of a fraction of the tritium in these loops is possible, and the fraction released provides a basis for sizing the tritium control equipment for the reactor hall.

4.3.4.1 Solid Breeder Tritium Inventory

We have made estimates of the tritium inventory and the rate of formation of T_2O in the high-temperature solid breeder. The tritium inventory depends on the particle size of the breeder compound and is estimated at 100 g for the reactor if the particle radius is 50 μm (roughly 150 mesh material). This estimate is influenced strongly by the tritium diffusivity value, which is poorly known and little understood in the lithium compounds of interest. Assuming 1 ppm oxygen in the helium stream at 1000°C, calculations indicate that the rate of T_2O formation is controlled by diffusion of tritium through the solid breeder compound, and that tritium will react with oxygen as soon as it reaches a solid surface that contains adsorbed oxygen.

The tritium inventory in a breeder blanket has been estimated based on the assumptions that:

1. Tritium is swept away by the coolant gas (containing 1 ppm O_2) as soon as T_2O is formed.
2. Negligible T_2O is present in the SiC portion of the solid blanket.
3. All the "stationary inventory" of tritium is present as tritium in the lithium compound.
4. The lithium compound particles are spherical.

Under these assumptions, the inventory of tritium in each particle, S , is the volume integral of the tritium concentration, i.e.,

$$S = \int_0^{r_0} C_T(r) 4\pi r^2 dr,$$

which is

$$S = \frac{4\pi R_T}{45D} r_0^5,$$

where

- R_T = tritium generation rate in $(\text{atoms}/\text{cm}^3\text{-s})$,
 D = diffusivity of tritium in the breeder compound (cm^2/s) , and
 r_0 = radius of particle sphere (cm) .

The total tritium inventory is obtained by using the total generation rate and assuming uniform particle size, such that

$$S(\text{total}) = \frac{R(\text{total})}{15D} r_0^2.$$

At this point, it is necessary to estimate values for the D because no data are available for the diffusivity of hydrogen (much less tritium) in Li_2TiO_3 or $LiAlO_2$.

We chose a value of D based on the diffusivity of tritium in Al_2O_3 where

$$D = 4 \times 10^{-10} \text{ cm}^2/\text{s}.$$

With this value, the total reactor inventory is 25 g. We note that the estimated value depends strongly on the diffusivity of tritium in the breeder, which can be altered many orders of magnitude by heat treatment, environmental exposure, and bombardment by ionizing radiation. We recommend that tritium

diffusion in lithium-bearing compounds be further examined and analyzed.

Finally, the calculated inventory values for the MARS solid breeder scales as the particle radius squared. For a 100- μm particle it is 100 g. This has been used as the baseline. If the diffusion coefficient were an order of magnitude lower, $4 \times 10^{-11} \text{ cm}^2/\text{s}$, the tritium inventory would be 260 g. We have not considered T_2O formation in the solid, which could increase the inventory.

4.3.4.2 T_2O Formation Rate

A key factor in the blanket design is the formation of T_2O by the oxygenated helium stream to prevent tritium loss by permeation. How fast this occurs is the critical issue and to determine this we had to examine the data available on hydrogen oxidation. A large amount of data is summarized in Fig. 4.3-28, where different rate-limiting regimes are identified.²⁸ The regime of interest is the extreme lower right corner because the total pressure of H_2 and O_2 is 0.12 mm Hg at 1000°C. This is the regime where an explosion will not occur but where the oxidation of hydrogen is limited by recombination at a wall. The important point is that the gaseous mixture is not in the explosive regime. Thus, we turn to an analysis of the heterogeneous reaction rates.

The rate of oxygen adsorption on a solid surface can be expressed as

$$r(\text{ads}) = \frac{3.51 \times 10^{22}}{(MT)^{1/2}} Pf \left(\frac{\text{molecules}}{\text{cm}^2\text{-s}} \right),$$

where

P = partial pressure of O_2 = (0.06 Torr),

M = molecular weight of O_2 (32),

T = absolute temperature (1273 K), and

f = fraction of the collisions that result in chemisorption.

The fraction of collisions f that result in chemisorption is

$$f = e^{\frac{-AE}{RT}},$$

where ΔE is the activation energy (for the chemisorption process).

As a general rule-of-thumb, E is 10 to 15 kcal/mole, and we used these values because no data were available for the activation energy of O_2 on Li_2TiO_3 or $LiAlO_2$.

Thus, $r_{ads} = 2.0 \times 10^{17}$ for $E = 10$ kcal/mole, and $r_{ads} = 2.8 \times 10^{16}$ for $E = 15$ kcal/mole.

Later results show that this sensitivity is not important. This is the rate of chemisorption of oxygen on any solid surface in the breeder module; this, of course, includes the surfaces of the solid breeder compounds.

The next task is to determine the rate of arrival of tritium atoms from within the breeder compound. The flux of tritium at the surface is found from the solution of the diffusion equation assuming a diffusivity of 4×10^{-10} cm^2/s and a rate of tritium generation that is constant throughout the particles. For a particle size of 50×10^{-4} cm ($50 \mu m$), the flux of tritium atoms at the surface is 1.3×10^{11} atom T/cm^3 -s.

This atomic tritium flux is much lower than the rate of chemisorption of oxygen (6×10^{16}), and indicates that the rate of tritium oxidation is limited by the rate of diffusion of tritium through the solid breeder. The surface is effectively covered with oxygen and, based on Fig. 4.3-28, the oxidation rate at the surface is the same as the diffusion rate. As a rough estimate, the fraction of tritium desorbed as T_2 is proportional to the ratio of diffusion of T to adsorption of O_2 . This is approximately 2.2×10^{-6} .

Fig. 4.3-28. Nature of reaction of stoichiometric mixtures of hydrogen and oxygen.

4.3.4.3 Tritium Extraction from Helium

Tritium is produced in the solid breeding blanket and diffuses into the helium loop as T_2O at the rate of 450 g/day. The function of the tritium extraction process is to recover the tritium produced in the blanket from the helium loop.

A slip-stream recovery process for steady-state tritium extraction is illustrated in Fig. 4.3-29. The input variables for the process design are the tritium production rate, the chemical form of tritium, the slip-stream flow rate, and the main helium flow rate. The engineering variables to be specified or calculated are the selection of the thermodynamic sink for T_2O in the recovery process, the operating temperature for the T_2O recovery process, and the tritium concentrations in all the process streams. Figure 4.3-39 and the above independent/dependent variables apply to any tritium extraction process for the helium process loop, as shown.

The tritium extraction process chosen for T_2O recovery is physical adsorption using a molecular sieve. The molecular sieve, designated 4A, provides the thermodynamic sink for T_2O and results in a T_2O partial pressure at the exit of the recovery unit on the order of 0.01 Torr at 60°C.²⁹ The 60°C process temperature exists in the main helium loop at the exit of the No. 2 precooler and before reaching to the helium compressor, as discussed in Section 4.3.5. A higher or lower extraction temperature produces a correspondingly higher or lower T_2O exit partial pressure. Lowering this temperature to 40°C would result in a T_2O exit partial pressure of approximately 0.001 Torr.

The molecular sieve bed is regenerated to recover T_2O by raising the bed temperature to 200 to 300°C and purging off the T_2O , or by vacuum pumping.³⁰ The T_2O is recovered in a cold trap and the liquid T_2O delivered for final T_2 purification by electrolysis and cryogenic distillation.

Table 4.3-12 contains the results of calculations of the pertinent quantities of a steady-state T_2O material balance for slip-stream fractions of 1 and 5%. The recirculating tritium radioactive inventory, i.e., that tritium as T_2O in helium, is calculated from $CI = 8.7 \times 10^9(y)(t_r)$, where t_r is the helium residence time (time to complete one loop of the coolant circuit) in seconds and y is the T_2O concentration in moles T_2O /mole He. For the case of

Fig. 4.3-29. Material balance diagram of slip-stream tritium recovery process.

Table 4.3-12. Steady state material balance for a T₂O slip stream recovery.

<u>Input variables</u>	
Tritium production (g/day)	450
Helium flow @ 80 atm (lbm/s)	1322
T ₂ O recovery exit pressure @ 60°C (Torr)	0.01
Blanket exit temperature (°C)	900
<u>1% slip stream</u>	
Slip stream exit concentration T ₂ O (moles/mole)	1.645×10^{-7}
Return concentration (moles/mole)	7.374×10^{-7}
Blanket exit concentration (moles/mole)	7.432×10^{-7}
T ₂ O partial pressure entering blanket (Torr)	4.483×10^{-2}
T ₂ O partial pressure exiting blanket (Torr)	4.519×10^{-2}
Slip stream flow (m ³ /g)	0.512
<u>5% slip stream</u>	
Slip stream exit concentration (moles/mole)	1.645×10^{-7}
Return concentration (moles/mole)	2.744×10^{-7}
Blanket exit concentration (moles/mole)	2.802×10^{-7}
T ₂ O partial pressure entering blanket (Torr)	1.669×10^{-2}
T ₂ O partial pressure exiting blanket (Torr)	1.704×10^{-2}
Slip stream flow (m ³ /g)	2.56

a 1% slip stream where the T_2O concentration is approximately 7.4×10^{-7} moles T_2O /mole He, the recirculating activities in curies is $6.4 \times 10^3 t_p$; for a 5% slip stream the result is $2.4 \times 10^3 t_p$.

To design the hardware for T_2O recovery using a 4A molecular sieve, we had to specify the size of the vessels. Vessel sizing is based on 24-hr operation, a 1% slip stream, a tritium loading of 450 g (as T_2O , 1650 g), a loading of 1 lb of tritiated water per 100 lb of sieve, and a superficial velocity of 22.8 m/min. These specifications produce a process pressure drop of approximately 3.4 kPa (0.5 psi), a bed area of $1.33 m^2$, a vessel diameter of 1.2 m and a bed depth of 33 cm. The cost for a single stainless steel vessel rated at $1.03 \times 10^4 kPa$ (1500 psi) is \$66,000³¹. Three vessels are required with one in operation, one in regeneration, and one in standby mode.

The tradeoffs for the process design of the tritium extraction system involve cost vs tritium in the process system. For the base case we selected a slip stream of 1% and a cycle time of 24 hr. This results in approximately 470 g of tritium ($4.5 \times 10^6 Ci$) in inventory (450 g on the bed, and 20 g recirculating with a 30-s residence time). Clearly, cutting the cycle time in half would result in a 250-g tritium inventory reduction. However, at the present it is not clear how fast a molecular sieve vessel can be heated, purged or vacuum pumped, and cooled for return to the process. The 24-hr cycle time is a subjective specification and appears reasonable from an engineering perspective. Thus, the apparent cycle time could be shortened by adding more vessels. Noting that the single stainless-steel vessel cost is \$66,000 and the installed cost is approximately 5.4 times this cost (including 25% contingency), increasing the number of vessels costs \$355,000 per installed vessel. Further trade-off studies must involve cost versus the tritium process inventory.

4.3.4.4 Tritium Extraction from LiPb

Tritium will be extracted from the LiPb by evacuation at a free surface. The system is identical (except in size) to the tritium extraction system for the LiPb blanket discussed in Section 4.2.4.1. The process relies on the very low solubility of tritium in LiPb.

4.3.4.5 Tritium Control in the Reactor Hall

Tritium will enter the MARS reactor-hall atmosphere either as the oxide or as molecular tritium. It can enter the hall as a result of accidents, normal process leaks such as helium (T_2O) from the turbines and compressors, LiPb leaks containing molecular tritium (T_2), and permeation losses from molecular tritium inventories. In any event, the reactor-hall tritium-control process must recover tritium that exists as T_2O , HTO, or T_2 .

The tritium recovery process that accepts T_2O and T_2 as feed is called catalytic oxidation/adsorption. It is shown in Fig. 4.3-30 with minor modifications.³² In the process, the reactor-hall atmosphere is accepted and compressed to approximately 405 kPa (3 atm). The compressed gas is heated and passed through a catalytic recombiner where molecular tritium (or HT) is oxidized to T_2O (HTO). The gas exiting the catalytic recombiner is cooled and refrigerated to 275 K (2°C) to condense water. The condensed water is drained from the process and this is essentially the only recovered-water exit from the process. The dried gas from the refrigerated dryer then passes through a molecular-sieve bed, to remove more water. At the exit of this bed, steam is added to the process gas and the gas is passed through a final molecular sieve bed. The steam is added to allow a larger fraction of the tritiated water to be recovered. By increasing the dew point of the process stream before the final molecular sieve bed, a larger percentage of the water entering this bed can be recovered. Because the tritiated water and the added steam will be well-mixed, the same percentage of steam and water will be recovered. If the steam were not added, the potential for mass transfer of the water would be relatively low and thus its percentage of recovery would be low. The exiting gas from the last molecular sieve bed is vented to the stack or returned to the reactor hall. The molecular sieve beds are regenerated with hot process gas and the regeneration exhaust is recycled to the entrance of the process. The unit capital cost of this catalytic oxidation/adsorption process is estimated to be \$690,000 per 30 m³/min.³²

To estimate the number of catalytic oxidation/adsorption units required to control the tritium concentration in the reactor hall, it is necessary to assume values for certain independent variables. The reactor-hall volume is assumed to be on the order of 1.2×10^6 m³. An instantaneous tritium input of

Fig. 4.3-30. MARS reactor-hall tritium recovery by catalytic oxidation/adsorption.

6.46×10^4 Ci is assumed from the helium loop (approximately one-third of the recirculating inventory for the base case T_2O /helium loop because the total loss of pressure in the helium loop can be prevented by isolation valves). The system cleanup objective is assumed to be a reactor hall tritium concentration of 500×10^{-6} Ci/m³ in 14 days (i.e., bubble suit protection). Assuming the reactor-hall atmosphere to be well mixed, the tritium concentration as a function of time will be $C/C_0 = \exp[-vt/V]$, where C_0 is the time = 0 concentration, V is the reactor hall volume, and v is the total feed rate capacity of the catalytic oxidation/adsorption process. Our assumption that the steam and water are well-mixed ignores tritium adsorption on the surfaces of the reactor hall. The required catalytic oxidation/adsorption flow rate for our assumptions is 280 m³/min, or 10 units. Scaling directly at \$690,000 per unit yields a total cost of $\$6.9 \times 10^6$. We anticipate that the total cost for the capacity required will scale to some power less than unity. Therefore, because our assumptions are only preliminary and because of the lack of construction experience in this area, the stated cost should be regarded as an upper bound.

We still have to address the issue of the validity of the assumptions used in sizing the process equipment for the reactor hall. This equipment is well understood and in an advanced state of design and testing³². However, to determine equipment requirements, a range of material balance conditions must be provided. For this particular case, we must quantify tritium input to the reactor hall atmosphere by way of normal process leaks and potential accidents. We also need to specify the acceptable steady-state tritium concentration in the reactor hall atmosphere and the allowable time to attain a specified tritium concentration after accidental release.

4.3.5 Energy Conversion System

We have developed a highly efficient energy conversion system for the high temperature blanket. The two-coolant two-temperature scheme using LiPb and helium offers an exciting opportunity for a hybrid heat transport/energy conversion system designed for maximum conversion efficiency. Within the constraints of blanket neutronic performance, energy multiplication M , tritium breeding ratio TBR and structural requirements, it is possible to vary the high temperature fraction FHT. Such flexibility gives us the opportunity to

optimize the integrated blanket and energy conversion systems.

The baseline system has both a closed-cycle high-temperature helium-gas turbine and a conventional steam-turbine cycle fed by the LiPb. The system is a hybrid because exhaust heat from the the helium turbine is used as a source of feedwater heating in the steam cycle. A gross thermal efficiency of 45.3% is achieved with a FHT of 46%.

The equipment used for the baseline is either commercially available or state of the art and, therefore, does not require extensive R&D programs for implementation. We have chosen a modular concept to minimize system cost, improve availability, and increase system maintainability.

4.3.5.1 System Analysis

As noted above, the two-fluid two-temperature blanket approach offers the opportunity for a hybrid system of maximum thermal conversion efficiency. The LiPb has a blanket outlet temperature of 482°C (900°F) with a return temperature of 372°C (700°F). When these temperatures are combined with the helium outlet temperature of 900°C (1652°F) and an allowable blanket helium ΔT of 450°C, a total blanket thermal energy of 3250 MWt is provided.

A wide variety of hybrid and energy conversion options are possible. In each of the cases studied, LiPb is used to produce steam for a more-or-less conversion steam-turbine cycle. For the high temperature helium, a number of options are potentially attractive. The major considerations in evaluating each energy conversion system option include gross electric output, net electric output (gross electric minus pumping power), tritium permeation and leakage, availability of system components, and overall system cost. The high-temperature blanket design allows a range of FHT, which is defined as that percent of total blanket energy available as 900°C helium. As the FHT is varied, the gross cycle efficiency and, thus, the gross and net electric energy generated vary. Because M and TBR also vary as a function of FHT, some iteration with the blanket design is necessary to optimize electricity generation. Fortunately, for the cycle chosen, system efficiency is fairly flat in the FHT range of interest.

The baseline cycle is depicted in the form of an abbreviated heat balance (shown in Fig. 4.3-31). The thermal input is 1750 MWt in LiPb and 1500 MWt in

Fig. 4.3-31. MARS heat balance.

helium. The 200°F ΔT for LiPb requires a flow of 805×10^6 lb/hr. This LiPb flow is passed through a number of heat exchangers to transfer the energy to a more-or-less conventional superheater/reheater steam turbine cycle. All LiPb/ H_2O heat exchangers use the double wall steam generator design developed by B&W and discussed in Section 4.2.5.3.

Steam conditions are 865°F at 2400 psia for the main steam and 865°F at 570 psia for the reheat steam. It should be noted that the steam flow of 5.71×10^6 lb/hr is maintained for both the superheat and reheat cycles. Because helium turbine exhaust is used for feedwater heating, steam extraction for this purpose is not required, thereby increasing the mass flow through the low pressure turbines. The steam turbine generator produces 939 MWe.

The helium flow of 5.09×10^6 lb/hr passes through a conventional closed-loop gas turbine cycle. The gas turbines produce 1115 MW of shaft power while the helium compressor consumes 551 MW. Accounting for some 28 MW of gas turbine/compressor losses yields a net gas turbine generation output of 536 MWe. The precooler normally associated with a closed loop gas turbine cycle has been divided in two stages to recover some of the gas turbine exhaust energy. The first stage is, in effect, a feedwater heater for the steam turbine cycle. Approximately 868 MWt are transferred to the steam cycle via this precooler/feedwater heater, thus eliminating the need for extraction steam. The remaining 248 MWt of helium is rejected to the atmosphere via the second stage and the station cooling towers.

Table 4.3-13 presents a gross energy balance for the heat transport/energy conversion system. Gross thermal efficiency is calculated to be 45.4%. Pumping power for the LiPb is 57 MWe, largely attributable to the high MHD pressure-drop through the blanket. Subtracting this pumping power from the gross thermal electric power of 1475 MWe yields a net electric output of 1418 MWe for a net thermal efficiency of 43.6%.

The 248 MWt of rejected heat from the helium cycle implies that we have room for improving the overall hybrid cycle efficiency. By decreasing the FHT, the LiPb and steam flows are increased, creating a greater demand for feedwater heating in the nonextraction steam cycle. This greater demand can be satisfied via larger and/or additional helium to feedwater heaters. Preliminary calculations indicate a gain of approximately 1% in gross thermal

Table 4.3-13. Energy conversion system.

Gross blanket thermal power (MW_t)	3250
Gross blanket electric power (MW_t)	1475
Steam turbine	939
Helium turbine	536
Gross thermal efficiency (%)	45.4
Lithium-lead pumping power (MW_e)	57
Net thermal output (MW_e)	1418
Net thermal efficiency (%)	43.6

efficiency if the FHT is lowered to 40%. The increased LiPb flow will require a slightly greater LiPb pumping power. Therefore, the gain in net thermal efficiency will be somewhat under 1%.

4.3.5.2 Component Description

The heat transport/energy conversion system is shown schematically in Fig. 4.3-31. There are three main subsystems of this system: the lithium-lead loop, the helium loop, and the feedwater loop.

Lithium-Lead Loop. To remove the 1750 MWt from the central cell in LiPb requires a total flow of 805×10^6 lb/hr. The high density of LiPb and the length of the central cell (150 m) have a major influence on component selection. The piping loops for transporting LiPb are a major part of heat transport system costs and, therefore, piping runs must be kept as short as practical. To provide for a reasonable pipe support system under seismic conditions with the high-density fluid, we restricted the maximum pipe diameter to 1 m except for short headers and manifolds. With this restriction, 12 primary pipe loops are needed to pass the LiPb flow at an average velocity of 6.4 fps. Relatively thin-wall pipe (schedule 30) may be used because the system operating pressure is generally low.

We decided to use a modular design for the heat exchanger/steam generator layout because of the number of pipe loops, the length of the central cell, and the desire to minimize the length of pipe runs. Our decision was reinforced by reliability/maintenance considerations, with the need to keep the weight of individual components within reason when full. There are four steam generator/superheater loops, each with one economizer, two evaporators, and a superheater. The reheat cycle has three heat exchangers operating in parallel. All heat exchangers, with the exception of the evaporators where boiling takes place, will be mounted horizontally to facilitate structural support. The five LiPb loops (four superheat and one reheat) each require approximately the same flow; therefore, five pumps are baselined. Each pump is driven by a 15000-HP motor.

Helium Loop. A helium flow of 5.09×10^6 lb/hr is needed to remove the 1500 MWt. Again, we chose a modular design because of the length of the central cell and reliability/maintenance considerations. In this case, there

are two identical, helium-gas turbine loops, one at each end of the central cell. A total of six pipe loops, three for each helium turbine, are required to keep helium velocity under 20,000 fps with 30-in. pipe. The high pressure pipe from the blanket modules to the turbine inlet will be refractory-lined to handle the 900°C (1652°F) helium.

Each gas turbine will pass one half the helium flow for a combined total shaft power of 1115 MW. The shaft-driven helium compressors, which also satisfy helium pumping requirements, consume 551 MW. Each turbine-driven generator then produces 268 MWe after subtracting turbine and compressor losses.

There are two recuperators per gas turbine, each with a shell and tube design and a surface area of approximately 5000 ft². Precooler 1, which contains the feedwater heaters, is composed of four shell and tube heat exchangers in series for each gas turbine. Heat is rejected by one heat exchanger (Precooler 2) for each gas turbine.

Steam/Feedwater Loop. The remaining major component of the energy conversion system is the steam turbine generator. We have chosen a 3600-rpm unit with a single-flow high-pressure turbine, a double-flow intermediate-pressure turbine, and a six-flow low-pressure turbine. Throttle flow is 5.71×10^6 lb/hr and inlet conditions are 865°F at slightly under 2400 psia for the high- and intermediate-pressure turbines and 865°F at about 570 psia for the low-pressure turbine.

4.3.5.3 Double-Walled Steam Generator

A double-walled steam generator is the baseline for the LiPb loop because it provides for tritium isolation, inspection and maintenance and can be manufactured in large sizes. The steam generator is discussed in more detail in Section 4.2.5.2.

4.3.5.4 Tritium Isolation

Tritium breeding takes place in both the helium and LiPb zones of the high temperature blanket. As such, both coolants will contain significant quantities of tritium. The high temperature of the working fluids and, therefore, the high temperature of the metallic containment for these fluids

facilitates tritium permeation. The major potential tritium leakage paths will be through the high temperature piping, heat exchangers, and helium gas turbines. Permeation of the tritium through the steam cycle is adequately controlled by the double-walled steam generator discussed in Sections 4.2.5.3 and 4.3.5.3.

We have designed at least two lines of defense to guard against tritium leakage. First, the insulation required for all high temperature components to minimize thermal energy losses also provides an adequate tritium barrier. Each high temperature pipe, heat exchanger, turbine, and pump will be sheathed by a metallic skin outside of the thermal insulation. Because these skins will be at room temperature, the tritium barrier need not be thick, nor is any special coating anticipated. The skin must of course be continuous throughout the heat transport system.

Channels may be carved in the thermal insulation to provide for a purge gas, probably helium doped with oxygen, to collect tritium in the annular space between the pipe or component wall and thin metallic skin. Special bagging provisions may be required around turbine seals, pump shafts, and valve stems.

The second line of defense is to build containment around those areas that could become contaminated. The reactor hall and all heat transport equipment, with the exception of steam turbine generators, must be located within these secondary tritium barriers. All entrances and penetrations must be sealed against tritium permeation and the enclosed areas kept at a negative pressure. The areas may be divided into separate zones, each sealed-off from the other, to minimize the total volume that could potentially be contaminated.

4.3.5.5 Corrosion Product Removal

Corrosion problems in liquid metal loops are usually most severe on the cold side, where corrosion products precipitate and can clog steam generators and valves. This problem can be dealt with in several ways. In the steam generator, heat transfer to the tube walls will occur faster than mass transfer, causing a super-saturated solution of the corrosion products in LiPb. The addition of particulates will cause most of the corrosion products to

precipitate out in the flowing stream (rather than on the tube walls), allowing them to be removed by filtering, magnetic separations, and other methods. Alternatively, it may be possible to keep corrosion product concentrations to fairly low levels by removing them on chevrons at the cold side of the loop. The static test data (see Fig. 4.3-15) indicate that it will take about 1000 hr to reach saturation at 500°C. Because this is a long time compared to a single loop through the system, it may be possible to remove corrosion products in a slip stream, keeping concentrations in the LiPb to acceptable levels. Another method of controlling corrosion is to add a corrosion inhibitor to the LiPb.

Without improved measurements of the corrosion rates it is difficult to choose an appropriate control method or to determine if corrosion control is needed. We are currently working to quantify and further define some of the concepts mentioned above. At present it appears feasible to use LiPb with HT-9 at ~ 500°C.

4.3.6 Blanket Maintenance

4.3.6.1 Maintenance Philosophy

The objective of the blanket module maintenance system on MARS is to minimize machine down-time for normal and off-normal events while safely removing and replacing the blanket modules. Remote, manual, and automated handling methods will be used depending on radiation levels and the task. Because blanket replacement is a scheduled yearly maintenance operation, we are expending considerable effort to design the modules and module handling systems for rapid removal and replacement. The use of remote techniques increases the time required to perform any task. Although good component design for remote handling can lessen maintenance time, it cannot compare with normal manual servicing. Therefore, it is prudent to maximize the number of operations that can be performed "hands-on." Maintenance time can be reduced by increasing shielding in certain high-maintenance areas, but the cost of additional shielding must be compared with the cost of providing for remote maintenance procedures. The availability of the machine is maximized by moving a small number of large integral units on a fixed-rail or roller system. The number of operations, their complexity, and the cost of equipment is also

reduced. This technique has been used successfully in shipbuilding programs to decrease construction time. All central cell components are designed for rapid replacement. Failure of any component, including a central-cell superconducting magnet, will not result in excessive downtime.

In general, blanket module components weighing several hundred tons or less will be handled with an overhead (gantry) crane. Heavier items will be moved on a track or roller system specifically designed for and incorporated into the MARS facility. The use of several mobile transporters (see Section 4.3.6.3) will eliminate the need to have a transport system built into each of the 24 modules, which produces considerable cost savings and reduced machine complexity. In such a system, access to the modules must be provided. Consequently, services lines, main coolant piping, and bus systems must approach the machine from one side and from the top of the machine. Disconnection and reconnection of these connections could be done by hands-on maintenance.

A separate blanket maintenance cell must be provided apart from the machine containment area for module repair work. This cell will be capable of handling sufficient modules simultaneously to allow repair of six blankets within the 1-yr period between changeouts. This area will be sealed from the main reactor containment cell following module replacement to allow replacement work on the blanket structures to proceed during machine operation. Each of the hot bay areas will be equipped with overhead cranes for handling large components. Manipulator systems will be provided where required for disassembly operations, and specialized equipment and fixtures will be included.

Structural components removed from within the modules will be highly radioactive. These structures must be prepared for shipment off-site in shielded containers or for on-site storage. High level waste requiring off-site storage will probably be compacted to permit easier and more economical packaging. It may also be possible to dilute wastes for on-site low-level storage. To accomplish this, a waste preparation area has been included in the maintenance area. This area would be equipped to further disassemble and cut up the activated structures by totally remote means. A packaging and package decontamination area will also be provided.

4.3.6.2 Blanket Module Changeout Procedure

We have developed a preliminary removal and changeout procedure for the high temperature blanket. At this stage in the study, the purpose of outlining the procedure is to establish an overall approach and identify problem areas. Timelines are illustrated in Figs. 4.3-32 and 4.3-33.

Module removal will require remote shutdown and manual and remote disconnect operations. Initial remote shutdown operations include:

- Plasma off.
- Magnet current rampdown.
- Afterheat removal and structure anneal.
- System to atmospheric pressure.
- Install temporary shielding.

The first wall and adjacent structures will be sufficiently radioactive to produce significant afterheat for the first day after shutdown. Modules not scheduled for replacement will require 60 hr of annealing at 450°C (see Section 4.3.1.8) to lower the ductile-to-brittle transition temperature. Modules being replaced will also be annealed to ease handling. Slowly circulating LiPb will be used to remove afterheat and anneal the structure. Coolant lines will be disconnected remotely because of the tritium hazard. The joint areas may be bagged to reduce tritium exposure. Temporary shielding may be required in certain areas to further reduce radiation exposure.

At least two days after shutdown, activation levels in the outer shield materials will have decayed enough to allow maintenance teams to enter the reactor central cell area. Six work teams will begin preparing the six modules to be removed. Hands-on operations performed during this time are:

- Drain and purge main coolant systems.
- Disconnect structural attachments.
- Position transhaulers.
- Disconnect main coolant lines.
- Disconnect magnet and other service lines.

Fig. 4.3-32. Module removal time estimate.

Fig. 4.3-33. Module replacement time estimate.

The area will then be cleared of personnel so the activated blanket modules can be moved. Remote operations performed before the modules are rolled out include:

- Open vacuum seals.
- Remove seal shields.
- Remove temporary shielding as necessary.

The modules will then be removed on six transhaulers each and replacement modules will be brought into place. Installation and startup tasks are essentially the reverse of shutdown and removal as follows:

- Remote operations:
 - Install seal shielding;
 - Install temporary shielding.
- Manual operations:
 - Final positioning and clampdown;
 - Main coolant lines sealed;
 - Connect services;
 - Close vacuum seals (remote inside shielding);
 - Complete testing of seals and diagnostics.
- Startup:
 - Ramp up magnet current;
 - Warm up and fill modules;
 - Pumpdown;
 - Inject plasma.

Future work on central cell maintenance operations will focus on the details of the design and procedures, and on interfacing with end-cell and balance-of-plant maintenance procedures. For balance-of-plant maintenance, we will need

systems for removing tritium from the helium and LiPb loops and for removing tritium-contaminated equipment.

4.3.6.3 Equipment Requirements

The major equipment in the reactor compartment will be six manipulator/crane gantries for use on each of the modules to be removed. These will be sized to for the seal shielding, which at present is the heaviest object to be handled.

Transporter units for the modules must have approximately a 1500-ton capacity. In addition, the transporters must be remotely controllable. They must also be capable of negotiating a 90-deg turn with the module aboard. This is required to minimize building size. Finally, to precisely align the module in its opening, the transporter must be capable of lateral load movement.

Maintenance hot bays will be equipped with 200-ton bridge cranes to disassemble the components from the modules. Manipulators will be located at appropriate intervals along the bay walls for remote disassembly.

4.3.6.4 Transhauler

The modules will be transported to hot cells for maintenance on the small-rail transhaulers shown in Fig. 4.3-34. These transhaulers, capable of 250 tons of lift, are currently used by General Dynamics Electric Boat Division to move submarine components, and in Japan by Kawasaki Heavy Industries to move ship components.

Manufactured by Western Gear at a 1980 cost of \$75 K, the transhaulers are remotely controlled and are capable, along embedded rails, of speeds up to 2 ft/min and a 90-deg change in direction. When modified, the speed can increase to 20 ft/min; the orientation of the transhauler to allow the 90-deg change in direction can be accomplished remotely. The six units required for each module can be simultaneously remotely operated in all movements.

Current design

MFG — Western gear
 Capacity — 250 tons
 Drive — Electric (2 ft/min)
 Lift — Hydraulic ($\frac{1}{4}$ in./min)
 Direction orientation (90-deg rotation—manual)
 Cost — \$75K (1980 dollars—60 units)

Modification required

Automate direction orientation

Fig. 4.3-34. Transhauler.

4.3.7 Blanket Module Manufacturing

Manufacturing engineering has been incorporated in the design of the high temperature blanket. This is vital to ensure that the design can be constructed without resorting to high cost or undeveloped techniques. The following components make up the blanket module.

Primary Shield. The shield components can be fabricated with state-of-the-art manufacturing processes as illustrated in Fig. 4.3-35. The inner and outer skins will be plasma arc-cut to near-net dimensions and the oxide surface that is generated by burning will be removed by hand-grinding during fitting for welding. The 2-in.-thick skins will then be rolled to their respective diameters and trimmed to size. Before the inner skin is rolled, the LiPb access ports will be machined into the inboard edge of the skin. The pod attach-rail details are machined complete, including weld preparation.

Shield Subassembly. The manufacturing procedure for the shield subassembly is also shown in Fig. 4.3-35. The inner skin will be welded together to form a cylinder. On this skin the inner structural rings will be located and welded in place. The outer skin pieces will be attached and welded. The assembly will be complete when the pod rails have been welded in place. This operation will require a weld fixture to hold the rails in position during the tack and first pass weld. All other operations at this point will use shop and manufacturing aids for support during assembly.

A specialized subcontractor will construct the forged torus ring, which will be delivered complete with edge weld preparation. Two rings that provide room for welding at assembly will be welded to the inner and outer rims of the forged torus. The torus ring will be ultrasonically inspected before welding, and x-ray and dye-penetrant inspection will be performed after welding. A flanged structural ring will be made up of segmented plates and a rolled ring. This welded subassembly forms one of the LiPb walls.

First Wall. Figure 4.3-36 illustrates the components and assembly of the first wall. The first wall subassembly will contain a bore, LiPb flow diverter and a skin. The bore will be rolled and welded to size and then the flow diverter will be resistance-welded to its outer diameter. The flow diverter will be made in segments and the channels will be die-formed. The inner skin will be made up in segments and resistance-welded to the flow

Fig. 4.3-35. Shield support structure fabrication.

Fig. 4.3-36. First wall assembly.

diverter and then the longitudinal seams will be welded.

Shield Major Assemblies. Because of weight and size considerations, an optimum assembly site will be selected before of the subassemblies are further married. Up to this point in the fabrication sequence the components and assemblies will have been built "in-plant." The weld assembly for the structural shield ring will receive the torus ring and flanged ring weld assemblies for welding (see Fig. 4.3-35). One shield-half will then have the first wall assembly positioned and welded in place. This shield-half will then be ready to receive the pod assemblies (see Fig. 4.3-36).

Silicon Carbide Components. Cast silicon carbide is as easy to work with as metal castings. Dimensions are accurate, detail is precise, and complex shapes can be accomplished without difficulty. Generally, cast shapes are used as-produced. The material is far too hard to be machined conventionally. Sizes of individual parts being produced today may range up to 1.2 m in length, 1 m in diameter, and from 6 to 10 mm in wall thicknesses. Some parts can be made with wall thicknesses of up to 76 mm, depending on the part configuration. Larger parts can be made by temporarily bonding separate pieces. These bonds must maintain integrity during assembly only and are not required to withstand high temperatures. These parameters fit the MARS components.

The silicon carbide pod support panels will be bonded in place on the helium circulating tubes and this assembly will be bonded to the pod SiC skin. After the ends are bonded in place, the pebbles will be placed in the cavity. Bonding the top in place will complete the silicon carbide assembly (shown in Fig. 4.3-37).

HT-9 Pod Details and Assembly. The pod skin detail will be stretch-formed and the end pieces will be die-formed. The upper skin will also be die-formed, drilled, and machined to final size. The tubes and collars will be procured components. The pod interface panel will be a resistance-welded subassembly. The number of these parts required for MARS would qualify them for rate tooling.

The silicon carbide assembly will have ceramic pedestals and ceramic felt bonded to its skin before it is assembled into the HT-9 pod. All the HT-9 details will be fitted to the pod and welded in place. After the tubes,

Fig. 4.3-37. Silicon carbide components.

collars, and doublers are welded in place, the completed assembly will be heat treated and tested (see Fig. 4.3-38).

Blanket Assembly Major Mating. Each pod assembly will be aligned and lowered in place in the shield rail guides. The pods will be locked in place by inserting LiPb separation plates in the pod rail key slot. These plates will to be tack-welded in place with each pod. Pod separation panels will be inserted between each pod and tack-welded in place (see Fig. 4.3-39). After all pods are in place, the LiPb separation plates will be welded complete.

The second shield half will be brought to the assembled shield and lowered in place. To provide for "down-hand" welding, the mated assemblies will be rotated to horizontal and placed on a suitable weld positioner. The two halves will then be welded together at the LiPb separation plate (see Fig. 4.3-40). A close-out weld at the torus ring to first wall is also required.

The LiPb plenum chamber close-out plates, having been previously detailed, rolled, sized, and drilled, will be ready for installation over the helium tube roughouts. These plates will be welded to the LiPb separation plates and the wall of the flanged structure assembly. Weld collars will be placed over the helium tubes and welded to both the close-out plates and the tubes. This closes out the LiPb chamber. See Fig. 4.3-41 for illustration.

The details of the helium manifold consist of tubes, flanged collar assemblies and sleeves. A box assembly containing the helium disconnect is also a part of this assembly. The tubes will be rolled to shape, cut off to size, and ported with oversized holes. The collar assemblies will be made with large skirt doublers attached to a tube that is sized to slip-fit over the helium tubes. The collars will first be slipped over the tubes, which have excess length in a rough-out configuration. The detailed manifolds will be brought to the rough-out tubes and the assembly will be measured to determine the required length. The tubes will then be cut to length and welded in place both to the manifold and the tube. After all the ports of the manifolds have been welded, the sleeves will be located over the manifold joints and welded in place. The sleeves are used to provide for manifold alignment tolerances and size mismatch. Leak and pressure tests will be performed when the welding is completed. Details and installation procedures are illustrated

Fig. 4.3-38. Pod assembly.

Fig. 4.3-39. Pod installation.

**Mating shield
halves**

Fig. 4.3-40. Shield half assembly.

**LiPb plenum chamber
close-out plate installation
weld and test**

Fig. 4.3-41. Assembly rotated for downhand welding.

in Fig. 4.3-42. Two sump assemblies will be welded between the helium manifolds for the inlet and outlet of the LiPb.

The blanket and shield will be complete at this point and ready for installation on the module support structure.

Helium manifold
details

Helium manifold
installation and test

LiPb sump
installation

Fig. 4.3-42. Helium manifold detailing.

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4.4 SOLID BREEDER BLANKET

The solid breeder blanket designed for MARS is helium-cooled with a water-cooled shield external to the vacuum boundary. The use of a vanadium-base alloy for breeder-bed components and ferritic steel for the primary structure offers the most practical compromise in meeting operational requirements and achieving a relatively low residual radioactivity.

The central cell module, shown in Fig. 4.4-1, comprises two magnets, a shield assembly, and a blanket assembly. It can be installed and removed from the reactor as a unit except for two small shield assemblies that cover the weld joint between modules. We selected the module size and composition based on reasonable weight and fabricable size. The weight of the 6.32-m-long module is:

Shield weight	= 247 tons
Blanket weight	= 134 tons
<u>Magnet weight</u>	= <u>400 tons</u>
Module weight	= 781 tons

The central cell module geometry is shown in Fig. 4.4-2.

4.4.1 Blanket Design

4.4.1.1 Design Summary

The design principle of the solid breeder blanket is the tube-type heat exchanger. The coolant--helium--flows over vanadium alloy tubes containing lithium oxide pellets that have been heated by fusion neutrons. This heat exchange produces high-temperature helium that is directly used in a steam generator. The neutrons also react with the lithium to produce tritium in the form of T_2O , which migrates into the helium flow. The tritium is extracted from the helium loop by processing a slip stream.

Heat generation from neutron deposition in the blanket is largest closest to the plasma and falls off rapidly as the distance from the plasma increases. Therefore, it is necessary to bring the cool helium directly to the inner wall and direct it radially outwards over the vanadium alloy tubes to the outer wall, as shown in Fig. 4.4-3. The helium ducted to the inner wall is

Fig. 4.4-1. Isometric view of the central cell module.

Fig. 4.4-2. MARS central-cell module geometry.

Fig. 4.4-3. Helium coolant flow.

manifolded axially to feed a tapered circumferential plenum. The helium passes through radially outward-facing holes toward the tubes. The inner wall has circumferential vanes machined into it to control its temperature. The outer wall is protected from the outward-flowing hot gas by insulation and a separate helium coolant loop (see Section BB of Fig. 4.4-4). The radially flowing helium is directed circumferentially to flow parallel to the outer wall and exits the blanket via the exhaust duct. A full description of the thermal/hydraulics is given in Section 4.4.1.4.

4.4.1.2 Structural Design

The blanket is contained by an elongated, doughnut-shaped pressure vessel. The internal helium pressure is 30 atm. Figure 4.4-4 shows general cross sections of the blanket.

..... We have chosen lithium oxide (Li_2O) as the breeder material because it gives an acceptable TBR and energy multiplication M. The Li_2O pellets are fabricated under pressure at an elevated temperature. The pellets will, in all probability, tend to break up and will have to be contained. We considered two possible containment materials;

- Silicon carbide (slip cast tubes),
- Vanadium alloy tubes.

The SiC tubes required a wall thickness three times that of the vanadium alloy because of fabrication limitations. This lowered the Li_2O packing density, resulting in a marginal TBR. Also, the brittle SiC tubes were difficult to handle during fabrication of the blanket. Although the vanadium alloy would embrittle during reactor operations, its manufactured condition is not brittle; therefore, normal fabrication, assembly, and handling procedures can be used. Because the vanadium alloy offers these advantages over SiC, we decided to use it for the tubes that will contain Li_2O pellets.

The vanadium alloy tubes vary in size. The smallest diameter tubes are closest to the plasma with the size increasing radially away from the plasma. The variation in diameter is necessary to limit the maximum Li_2O temperature to preclude sintering. The tube layout is shown in Fig. 4.4-5.

The tubes are supported in loose-fitting holes in two vanadium alloy tube sheets, as shown in Fig. 4.4-6, and are restrained axially at one of the tube

Fig. 4.4-4. Blanket design.

Fig. 4.4-6. Tube sector assembly.

sheets. The trapezoidal-shaped tube sheets are connected by a vanadium alloy spacing structure. At each end of the spacing structure, the tube sheets contain slots that straddle rails attached to the outer wall. Vanadium alloy baffle plates are attached to the tube sheets to maintain the radial direction of the coolant flow. There are 12 tube assembly sectors arranged circumferentially around the plasma. One sector is shown in Fig. 4.4-6. The weight of the tube sector assemblies is supported via rails to support rings attached to the exterior of the outer wall. The blanket is supported at the inner support rings and longitudinal members are attached to the outer wall from the support points to the adjacent support rings. The design of the blanket supports allows for thermal growth between the support points. One support is rigidly fixed and will react loads in any direction; a second support will react vertical loads and horizontal loads in one direction; the remaining two supports will react vertical loads only.

To allow for thermal and radiation swelling and creep, the length of the blanket allows for a gap between adjacent modules at initial installation. This gap is almost taken up when the blanket is at its operational temperature. To provide a vacuum wall for plasma containment, a vacuum-tight joint is required, which can make allowance for the relative motion between adjacent modules (see Fig. 4.4-7). A double bellows design is required to contain the helium coolant. The welded vacuum joint has a configuration that permits it to be welded and cut using remotely operated tools and equipment.

Vanadium alloy V-15Cr-5Ti was chosen for the tubes to contain the Li_2O pellets and for the structure. It will be necessary to use protective surface coatings to overcome the alloy's susceptibility to surface reactions with tritium and gaseous impurities in the helium coolant (e.g. hydrogen, oxygen, nitrogen, and carbon compounds) and with the Li_2O . Because of the long-time high-temperature service conditions, some of these gases will diffuse through the protective surface causing the vanadium alloy substrate to become brittle. Fortunately, this embrittlement will only be significant at temperatures below the operational range of the blanket. Stress levels are minimal and we have accounted for the relative motions due to differences in thermal expansion of adjacent structures.

The material selected for the blanket pressure vessel and support structure is a ferritic steel having the physical, structural, and radiation damage

Fig. 4.4-7. Blanket module joint.

resistance properties of SANDVIK HT-9. The design of the vessel alleviates some of the welding problems of HT-9. However, we expect that development work will lead to improvements in HT-9 fabricability. The service temperature of the pressure vessel was limited to 450°C by thermal creep. We have met this temperature limitation by the using direct cooling and/or insulation.

We applied the following structural design criteria in the design of the blanket:

The allowable stress S_m (time-independent) is the lowest value of

$$\frac{S_u}{3} \quad \text{at room temperature,}$$

$$\frac{S_u}{3} \quad \text{at maximum service temperature,}$$

$$\frac{2S_y}{3} \quad \text{at room temperature, and}$$

$$0.9 S_y \quad \text{at maximum service temperature,}$$

where

S_u = ultimate strength of the material and

S_y = yield strength of the material.

The allowable stress S_t (time-dependent) is the lowest value of

$$\frac{2}{3} \quad \text{the maximum stress to cause creep rupture in time } t,$$

80% of the maximum stress to cause tertiary creep in time t , and the minimum stress to produce 1% strain in time t .

The primary allowable membrane stress S_{mt} is the lesser value of S_m and S_t .

For normal operating conditions, the allowable stress limits are

$$P_m < S_{mt} \quad \text{and}$$

$$P_1 + P_b < 1.5S_m \quad \text{or} \quad K_t S_t.$$

where $K_t = 1 + K_s \left(1 - \frac{P_1}{S}\right)$,
 $K_s = 0.25$ for shell-type structures,
 P_m = general primary membrane stress,
 P_1 = local primary membrane stress, and
 P_b = primary bending stress.

For shell buckling, the static factor of safety shall be not less than 5.

We designed the solid breeder blanket on the basis of known loads caused by pressure, temperature and dead weight. We have not quantitatively analyzed other design conditions caused by seismic and magnetic loads.

4.4.1.3 Neutronics

For the solid breeder blanket, the main choices for breeder materials are Li_2O and Li_7Pb_2 . Helium cooling also acts as the purging medium for the bred tritium. At present, the preferred breeding material is Li_2O , because more established database indicates a favorable upper temperature limit of 700°C. The attractive feature of Li_7Pb_2 is its neutronic properties, with lead providing neutron multiplication; however, the material properties of this alloy are not sufficiently known.

All neutronics analyses used the one-dimensional discrete ordinates code ANISN. They were performed with a 100-neutron/21-gamma group structure from the EPR cross section library (based on ENDF B-IV). The analyses used cylindrical geometry and a P_3S_8 approximation. A distributed isotropic source of 14.1-MeV neutrons was confined to a radius of 43 cm with the first wall at a 60-cm radius.

To help develop a blanket design with good tritium breeding and energy multiplication properties, we explored the sensitivity of neutronics performance to several design variables. These included blanket thickness, Li-6 enrichment (including variation in enrichment between blanket regions), SiC or graphite reflector, and the introduction of beryllium as a neutron multiplier. The analyses included the effects of the blanket structure and a shield. One general result was that the active blanket region containing the breeding compound and the reflector or neutron multiplier would have to be at least 50 cm thick (including 20 vol % He) to avoid significant energy leakage into the shield (recoverable only as low-grade heat).

For Li_7Pb_2 blankets, we investigated the effects of varying the Li-6 fraction from natural to 90% and the blanket thickness from 30 to 70 cm. We found that for each thickness there is an optimum enrichment that shifts from 45% at 30 cm (TBR = 1.24) to 35% at 50 cm (TBR = 1.44) to 25% at 70 cm (TBR = 1.57). Also, the TBR tends to become less sensitive to enrichment as the blanket thickness increases. The total energy deposition in the blanket was found to increase very little (from 16.21 to 16.47 MeV/neutron at 15% enrichment) as the blanket thickness was varied from 30 to 70 cm, and it showed even less sensitivity to variations in enrichment at a given thickness. These trends, summarized in Fig. 4.4-8, appear to be specific to Li_7Pb_2 and are caused by the neutron multiplication properties of lead.

For Li_2O blankets, we explored the effects of zone enrichment, inclusion of a SiC or graphite reflector, and the use of a beryllium multiplier. As a baseline, we considered a blanket with a 50-cm-thick breeding zone containing 80 vol % Li_2O with natural lithium. We obtained a TBR of 1.32 and an M of 1.18 with 0.64 MeV deposition in the shield. An increase in the Li_2O region thickness to 60 cm combined with grading of the Li-6 fraction produced no significant change in the energy deposition but lowered the TBR to 1.24. Replacement of the rear 15 cm of Li_2O in this configuration with SiC did not affect these results.

We also examined the partial replacement of Li_2O with SiC for different moderation characteristics or Be for neutron multiplication. The SiC replacement substantially lowered TBR, and slightly reduced M, as shown in Fig. 4.4-9(a). In contrast, the partial replacement of Be for Li_2O showed that the TBR as a function of Li_2O replacement rises from 1.26 for 10 vol % replacement to 2.0 at 60 vol % replacement. Over the same substitution range, the blanket energy deposition increased from 16.4 MeV to 21.4 MeV/neutron [see Fig. 4.4-9(b)]. We also explored the effect of varying the lithium enrichment (from 10 to 60%) at a given Li_2O replacement level (50 vol %). The energy deposition was unaffected; the TBR declined slightly, from 1.91 (10% Li-6) to 1.82 (60% Li-6). In spite of these improvements in performance, a beryllium multiplier was not carried forward because of community concerns over beryllium resource limitations. All of the above analyses include the effects of systematic cross section errors that overpredict the $^7\text{Li}(n,n')\text{T}$ and $\text{Be}(n,2n)$ reactions.

Fig. 4.4-8. Tritium breeding and energy multiplication in Li_7Pb_2 .

(a) Silicon carbide

(b) Beryllium

Fig. 4.4-9. Effect of silicon carbide and beryllium on Li_2O performance.

Based on these results, we configured a realistic blanket design using Li_2O with He cooling. The breeding/power conversion region consists of three zones of stacked, thin-walled vanadium alloy tubes filled with Li_2O pellets (natural lithium), oriented parallel to the plasma axis. The tube diameter and wall thickness are reduced toward the front to provide for more effective cooling in the region of higher power deposition. The blanket shell, including the first wall, is a stiffened HT-9 structure, also cooled by He. The one-dimensional neutronics representation includes a 60-cm shield and is shown in Fig. 4.4-10. Breeding performance was analyzed with both the EPR library and the LLNL ENDL library, which incorporates a downward correction of the $\text{Li-7 } (n,n')T$ cross section. As shown in Fig. 4.4-10, a total energy deposition of 15.64 MeV (excluding the shield) is predicted, together with a TBR of 1.309 based on the EPR library. Use of the ENDL library results in a 2% reduction in the TBR. For this configuration, we evaluated the effect of replacing the vanadium alloy with SiC and of replacing the Li_2O with Li_7Pb_2 , using either type of tube.

After additional refinements in the thermal design of the blanket, we partitioned the blanket into four zones of vanadium alloy tubes that contain the Li_2O (as shown in Fig. 4.4-11). The neutronics results show $M = 1.1$ (excluding the shield) and $\text{TBR} = 1.25$. The inclusion of a steel (Fe-1422) reflector region in the shield did not enhance M . Replacing the last breeding zone by 95 vol % Fe-1422 had the effect of increasing M to 1.15 while lowering TBR to 1.09.

In an attempt to improve M , we replaced Li_2O in the third and fourth breeding zones with 70% enriched Li_7Pb_2 . This produced $M = 1.10$ and $\text{TBR} = 1.25$. Finally, we examined the effect of replacing all Li_2O with Li_7Pb_2 , using natural Li-6 for the first two zones and 70% enrichment for the last two zones. This arrangement also does not produce an improvement in M (1.11), but offers TBR of 1.37.

In summary it appears that Li_2O blankets cannot achieve M greater than 1.1 to 1.15 without a neutron multiplier. The Li_7Pb_2 breeders may provide higher M ; however, further study and material characterization is required.

Plasma C.L. 0 cm		Region	Energy deposited (MeV)	TBR (EPR)	TBR (ENDL)
61					
First wall	12.3 vol % HT-9 87.7 vol % He 74	Breeding zone 1	8.385	0.703	0.681
Zone 1	5.7 vol % V 49.5 vol % Li ₂ O 44.8 vol % He *(1.91/0.051) 96				
Zone 2	7.1 vol % V 50.3 vol % Li ₂ O *(3.18/0.102) 123	Breeding zone 2	3.901	0.415	0.409
Zone 3	6.9 vol % V 52.3 vol % Li ₂ O 40.8 vol % He *(5.8/0.153) 173	Breeding zone 3	1.521	0.189	0.187
Backwall	37 vol % HT-9 63 vol % He 185	All breeding zones	13.807	1.309	1.277
Shield	60 vol % HT-9 15 vol % B ₄ C 15 vol % Pb 0.5 vol % H ₂ O 245	All zones excluding shield	15.641	—	—
*Vanadium tube dia/thickness (cm)					

Fig. 4.4-10. Li₂O blanket performance with three breeding zones.

0 cm	Source						
40	Void						
58.6	HT-9	100 vol %	Z1				
60.146	V-alloy	3.545 vol %	Z2				
77.40	HT-9	1.25	Z3				
	V-alloy	13.27					
	Li ₂ O	43.61					
82.30	HT-9	1.44					
	V-alloy	10.60	Z4				
	Li ₂ O	39.28					
95.20	HT-9	1.82					
	V-alloy	9.03	Z5				
	Li ₂ O	57.00					
118.20	HT-9	2.47					
	V-alloy	6.36	Z6				
	Li ₂ O	65.91					
156.90	ZrO ₂	100	Z7				
158.236	V-alloy	7.04	Z8				
166.014	HT-9	100	Z9				
171.60	Reflector						
201.60	Shield						
231.0	Void						
255.0	Dewar and magnet						
390.0							

Zone	Energy deposition (MeV/n)	M	TBR
Z1	1.808	0.128	
Z2	0.459	0.033	
Z3	2.588	0.1836	0.1874
Z4	4.268	0.3027	0.3522
Z5	4.637	0.3289	0.4921
Z6	1.680	0.1191	0.2150
Z7	0.001	0.0000	
Z8	0.007	0.0006	
Z9	0.056	0.0040	
Subtotal	15.504	1.0997	
Z10	0.075	0.0053	
Z11	0.002	0.0001	
Total	15.581	1.1051	1.2467

Fig. 4.4-11. Li₂O blanket performance with four breeding zones.

4.4.1.4. Thermal/Hydraulics

Functional Description. Each blanket module covers 6 m of the central cell and has a single set of helium inlet and outlet ducts. The inlet duct penetrates the blanket core in a radial direction to just behind the first wall, where the gas expands into the first wall channels and travels in a circumferential direction as it absorbs the heat generated in the first wall. As the helium flows around the first wall, it bleeds off through ports into the chambers holding the breeder. The first wall passages vary in area so that, as the gas heats up and is bled off, a constant gas flow velocity is maintained. The helium flow path is illustrated in Fig. 4.4-12.

The breeding material must be maintained in a temperature band. If the material is too cool, the diffusion coefficient for tritium will be too low and there will be a large tritium inventory; conversely, if it is too hot, the material will sinter, which also inhibits tritium diffusion. In many of the solid breeding materials, this operating temperature range may be quite narrow. For Li_2O , it is presently estimated as 410 to 700°C. We met this restriction in temperature range by varying the tube diameter in the blanket. With a two-order-of-magnitude variation in volumetric heating rate, the 1-cm-diameter inner tubes will be at a temperature close to that of the outer tubes, which are 3.175 cm in diameter.

As the gas leaves each breeding region, it flows around the module just inside of the outer wall and is directed to the outlet duct where it leaves the module.

Thermal Performance. Table 4.4-1 lists the thermal performance parameters of the blanket. Because the total energy deposition is fixed by neutronics, a direct relationship exists between the helium flow rate and outlet temperature. To maximize power conversion efficiency, the helium should be at the highest possible temperature. Because the energy is absorbed in solid materials and transferred to the coolant by conduction and convection, the temperature limits of the blanket materials will limit the maximum helium temperature. The Li_2O temperature limit is 700°C. At the rear of the breeding zone, the heating rate has dropped to a point where the temperature gradients, both in the material and across gas films, are fairly small; the coolant temperature is very close to the Li_2O limit. The difference in temperature between the breeding tube centerline and the surrounding helium is

Fig. 4.4-12. Helium coolant flow path.

Table 4.4-1. Blanket coolant parameters.

Parameter	Value
Helium flow per module (kg/s)	79.2 ^a
Helium inlet temperature (°C)	300
Helium outlet temperture (°C)	630
Helium inlet pressure (atm)	30
Helium pressure drop (atm)	2.1
Helium pumping power (MW)	264
Max first wall temperature (°C)	464
Min Li ₂ O temperature (°C)	393
Max Li ₂ O temperature (°C)	687

^aIncludes rear wall coolant.

only 14°C at the outer edge compared to 173°C at the inner edge. With a helium inlet temperature of 300°C, an average outlet temperature of 630°C may be achieved.

Because the helium flowing around the first wall passages is bled off gradually into the blanket zones, each zone will be receiving progressively higher inlet temperatures, ranging from 300°C near the inlet duct to 362°C on the opposite side. The mean helium temperature leaving the first wall and entering the breeding zone is 319°C. As a result, the gas temperature leaving the breeding zones will vary from 611 to 673°C, with a mean of 630°C.

The first wall temperatures are calculated assuming that the first wall is adiabatic on the plasma side. The first wall has a volume of 1.546 cm³/cm² of frontal area and neutronic heating of 41 W/cm³, resulting in an effective heat flux of 63.4 W/cm² of frontal area. Surface heating is caused by plasma radiation and charged-particle interactions and is small for MARS, about 6 W/cm². Thus, the error introduced by neglecting surface heating is about 10%.

The first-wall coolant channels vary in cross sectional area to maintain a constant flow velocity of 60 m/s (Mach no. = 0.04); the film coefficients are calculated by conventional means. For the purpose of the heat transfer calculations, we assume that the first wall structure, at any circumferential location, has a uniform temperature on all cooled surfaces; the difference in gas-to-metal temperature is then equal to the heat deposited at any area divided by the surface area and film coefficient. To minimize the wall temperatures and reduce the metal volume (and thus heat deposition) while still maintaining adequate stiffness, fins are incorporated in the coolant channels as shown in Section CC of Fig. 4.4-4.

With the metal surface temperatures known, the maximum metal temperature is calculated on the basis of an adiabatic wall on the plasma side and uniform internal heat generation. The wall is 0.48 cm thick, absorbs 41 W/cm³, and has a temperature difference from the front to rear face of 15.7°C. The maximum temperature gradient is double the overall gradient, or 65°C/cm, which is acceptable based on the thermal stress.

One of our design goals was to limit HT-9 to a maximum temperature of approximately 450°C. We adjusted the first-wall gas velocity and fin area to approach this goal. We did not feel that further increases in gas velocity were warranted based on increased pressure drop.

The helium coolant bleeds through ports in the aft portion of the first wall and into the breeding zones. It flows over tubes containing Li_2O , arranged in staggered arrays, parallel to the plasma axis. The film coefficients are calculated using relationships developed for cross-flow heat exchangers.

The portions of the blanket closer to the plasma have the highest heating and thus the tubes must be small (1-cm-diameter) to limit the Li_2O temperature to acceptable values. As the coolant progresses through the blanket, the heating rate falls off exponentially and the tube diameters may be increased. This design incorporates four zones containing tubes with diameters of 1.0, 1.27, 1.905, and 3.175 cm.

Figure 4.4-13 illustrates the predicted temperature profiles for the coolant gas, first wall, vanadium alloy tubes, and Li_2O .

Hydraulic Performance. The helium pressure drop through the first wall and blanket is 2.1 atm with an inlet pressure of 30 atm. This breaks down as follows:

<u>Item</u>	<u>Pressure drop (atm)</u>
Inlet duct	1.79
First wall	0.07
Blanket	0.01
Outlet duct	<u>0.23</u>
	2.10

The bulk of the pressure loss occurs in the inlet duct. The inlet duct penetrates the blanket and it is not practical to have a large duct diameter with gradual turns and expansion angles. Although in a detailed design every effort would be made to minimize the pressure loss by means of turning vanes, etc., it is still likely that a large portion of the inlet duct velocity head would be lost in the transition from the duct to the first wall. In this analysis we estimated that one full velocity head is lost as a result of the expansion process and another full velocity head is lost in frictional and turbulence effects accrued in distributing the flow uniformly. The pressure loss in the first wall passages is primarily caused by frictional losses and is relatively small at a velocity of 60 m/s.

Fig. 4.4-13. Blanket temperature profiles.

The pressure drop in the blanket is also very small. This is because the void fraction is high, which causes the velocity to be low. The highest gas velocity, at the minimum gaps between tubes, is less than 10 m/s. In fact, it would be desirable to increase this velocity to improve the heat transfer characteristics of the blanket. The main reason for adopting a lower limit on tube-to-tube spacing is that the tubes are supported in tube sheets that must maintain the structural integrity of the assembly. An alternate design, which has yet to be examined, would use tube bundles with the tubes spiral-wrapped with wire or some other form of spacers to maintain a closer tube spacing. It might also be beneficial to knurl or etch the cladding to increase heat transfer at a small expense in pressure drop.

The pressure drop associated with the outlet duct is also relatively small. There is little restriction on its diameter and thus the velocities may be kept somewhat lower than the inlet duct and, more importantly, we have a duct entrance loss that is easier to accommodate efficiently. In this analysis, a pressure loss of one velocity head (referenced to the exit duct velocity) is assumed for the losses associated with collecting the flow and directing it into the exit duct.

The total pumping power for coolant flow through the first wall and blanket is 265 MW, or 8.6% of the thermal energy liberated in the blanket. This loss is a strong function of the system operating pressure, as shown in Fig. 4.4-14. The loss could be cut approximately in half by increasing the system pressure to 40 atm. A similar study for a high-temperature gas-cooled reactor/gas turbine (HTGR-GT) system¹, which has similar components in the power conversion system, reported a baseline system pressure of 78 atm. Their sensitivity study showed little variation in power cost with changing pressure. It is likely that the optimum system pressure is higher than 30 atm; however, it is doubtful that it would be as high as 80 atm because of the large effect on the blanket structure.

Rear Wall Thermal Hydraulics. The rear wall is gas cooled by a separate helium stream tapped off the main coolant and reunited with it outside of the blanket. The neutronic energy deposition in the rear wall is 0.125 W/cm^3 (0.70 W/cm^2 of rear wall surface area) plus 0.44 W/cm^2 heat conduction from the breeding zone.

Fig. 4.4-14. Effect of helium pressure on pumping power.

The coolant is introduced in corrugated passages and extracted at a point 90 deg around the periphery of the rear wall. The manifolding is arranged so that the gas in the adjacent passages travels in opposite directions. In this manner the rear wall is maintained at as constant a temperature as possible. The gas enters at 300°C and exits at just under 320°C; the wall surface is 320°C. Assuming that the outer edges of the wall are adiabatic, the thermal gradient across the rear wall is 5.4°C. The pressure loss associated with this coolant flow is less than 0.1 atm and the pumping power is less than 0.7 MW.

4.4.1.5 Radiation Effects On Structural Materials

The three areas of concern with regard to radiation effects on structural materials are: change of tensile properties, radiation creep, and shift in ductile-to-brittle transition temperature. Unfortunately, the effects of D-T neutrons on the properties of structural materials have not been experimentally evaluated at this time. Therefore, it is necessary to use the experimental database developed with fission neutron, heavy ion, and electron irradiations.

The primary structural material for the solid breeder blanket is SANDVIK HT-9, a chromium-molybdenum ferritic steel. This alloy has been the subject of an extensive experimental evaluation effort by the Fast Breeder Reactor Program. Its irradiation response in a fission environment is very encouraging and we assume that this performance would be maintained in a D-T neutron environment.

An undesirable characteristic of HT-9 is the need for preheat during fusion welding and for a post-weld heat treatment. Heat treatment of assemblies of complex geometry is undesirable and often impractical. For the purpose of this study, the use of dissimilar metal weld lands made of Inco 625 eliminates the need for post-weld heat treatment of major assemblies. The Inco 625 alloy, however, suffers a greater loss of ductility than HT-9 under irradiation and does not offer a totally satisfactory solution to the problem.

The tube assembly structure is fabricated from the vanadium base alloy V-15Cr-5Ti. Although the general technology of vanadium base alloys is limited, their irradiation response has been investigated² and experimental results indicate an extremely high resistance to void swelling and no undesirable effects on mechanical properties.

4.4.1.6 Solid Breeder Performance

Our primary emphasis in selecting a solid breeder focused on the lithium-based ceramics such as Li_2O , LiAlO_2 , Li_2SiO_3 , Li_4SiO_4 , Li_2TiO_3 , and Li_2ZrO_3 . These materials are not well-characterized; work is in progress under DOE sponsorship to verify that their properties meet design requirements. The major uncertainties are: (1) the effect of high temperatures and irradiation on tritium release, and (2) the effect of impurities on system chemistry (phase change) and tritium release.

For the breeder, we selected Li_2O as the baseline material. It has a high lithium atom density, reasonably high operating temperature, does not require a neutron multiplier and is better characterized than the rest of the materials. The maximum operating temperature to preclude sintering is currently estimated to be 700°C based on initial experimental data of Yang, et al.³ The minimum operating temperature is set at 410°C because of LiOT formation.

We have also considered using Li_7Pb_2 as the breeder. It is an intermetallic compound that appears to act as an ionic salt. Preliminary indications are that tritium is much less soluble in Li_7Pb_2 than in Li_2O . Yang's initial experimental data indicated no microstructure change after irradiation at 600°C . The operating temperature range for Li_7Pb_2 appears to approach that of Li_2O . In addition, the material is compatible with the present mechanical design of the solid breeder module and could be directly substituted for Li_2O . At this time we are considering Li_7Pb_2 as a backup for the solid breeder while the characterization program for the lithium-based ceramics is being conducted.

4.4.2 Shield

4.4.2.1 Design Criteria

The main criterion in configuring the shield for the solid breeder blanket design was to provide adequate radiation protection for the central-cell superconducting magnets. The specific performance requirements imposed were to keep the radiation dose to the magnet insulation (Kapton)--accumulated over 24 FPY--below a generally accepted damage threshold of 5×10^9 rad, to limit the peak displacement damage in the copper stabilizer to 2.2×10^{-5} dpa/FPY,

and to reduce the peak radiation heating in the magnet winding below 0.06 mW/cm^3 . Another important consideration was to achieve the required attenuation performance with the minimum shield thickness to minimize magnet bore. We also sought to limit the generation of long-lived radioactivity in the shield by selection of the shield materials. Because the blanket design allows only about 0.5% of the energy to leak into the shield, the required shield performance was relatively modest. We chose water as the coolant.

4.4.2.2 Structural Design

The shield is situated radially between the blanket and the magnets to absorb neutron and gamma energy to limit deposition in the magnet. As we stated before, the design of the shield is governed by neutronics and remote handling procedures adopted for removing and replacing the central cell module.

The shield has a radial thickness of 41.5 cm and a neutronics analysis shows that a volumetric composition of 60% HT-9, 15% lead, 15% B_4C , and 10% H_2O gives a satisfactory performance.

The shield, shown in Fig. 4.4-15, covers two areas. The largest part of the shield, or blanket shield, covers the major portion of the blanket and is made in two pieces with the joint on the horizontal plane parallel to the plasma axis. The lower half of this part of the shield, attached to the lower carriage structure, supports the upper half and can remain in position during blanket changeout. The upper half is constructed to permit crane hoisting. Both the upper and lower shield halves include penetrations for the blanket outer wall coolant ducts. The lower half has one support point (fixed point) for the blanket and penetrations to allow the other three support links to be attached to the lower carriage structure.

The smaller part of the shield covers the module joint regions. The smaller shields are also made in two parts, upper and lower, and are split on the horizontal plane similar to the blanket shield. The module joint shields are supported by the blanket shield through radial attachment bolts. Each half has penetrations for the module-joint bellows coolant duct. When access is required to the module joint, the lower half of the shield is lowered to a storage cradle on the floor and the upper half is crane hoisted.

Fig. 4.4-15. Remote joint and blanket shields.

The shield is cooled by water flowing in channels near the surface closest to the plasma. In the module joint shields, the water inlet opens up into a manifold that feeds channels running circumferentially around the shield half into an exit manifold and outlet pipe. The blanket shield halves are cooled by water piped to a manifold at the center of the blanket. This manifold feeds channels running axially to both ends of the blanket where they are turned 180 deg back to an exit manifold and outlet pipe at the center of the blanket.

The shield is constructed so that the steel forms an annular box to contain rolled slabs of lead held in place by a minimum number of studs and steel enclosures that hold the boron carbide powder. A steel cover bolted in place closes off the annular box.

4.4.2.3 Neutronics

We conducted the shield analysis as an integral part of the blanket design using the ANISN code with a 100-neutron/21-gamma group structure. We used the EPR cross section library, augmented by data sets from MACKLIB (displacement and activation cross sections). For the radiation transport analysis, we treated this shield as a homogeneous mixture. In one design variant, we introduced a 30-cm steel reflector (Fe-1422 with 5 vol % H₂O) between the blanket and the homogenized shield region to increase the energy deposition; however, this was marginally effective and therefore abandoned.

To determine the shield thickness required to satisfy magnet exposure limits, we generated parametric results for the various magnet effects vs shield thickness. For these calculations, we used the fluxes exiting from the shield and therefore neglected the attenuation provided by the magnet dewar. Based on these analyses we concluded that a 45-cm-thick shield would satisfy the magnet exposure requirements with adequate margin, as shown in Fig. 4.4-16.

To assess the external dose levels after shutdown, we analyzed the activity produced in the shield over 2 FPY. All gamma-emitting radioisotopes with half-lives between 1 hr and 100 yr were included. We considered two options for the 60 vol % structural material: HT-9 and Fe-1422. For the HT-9 shield, we found a shutdown activity per unit of central cell length of 1.754×10^5 Ci/m dominated by Mn-56. This excludes an Fe-55 activity of 6.24×10^4 Ci/m. One day after shutdown, the activity reaches 6.82×10^4 Ci/m

0 cm	Source				
40	Void				
58.6	Blanket				
171.60	Shield				
	HT-9	60			
	B ₄ C	15			
	Pb	15			
	H ₂ O	10			
216.60	Void		Z12		
255	Dewar				
	304SS	37.62 vol %	Z13		
	LN ₂	3.95			
272	Polyimide 100		Z14		
273	(Magnet pack)				
	304 SS	35 vol %			
	Cu	31.5	Z15		
	Polyimide	15			
	LHe	15			
	NbTi	3.5			
390					

Constraint	Requirement	Performance
Magnet insulation dose (kapton)	$< 5 \times 10^8$ rad/24 FPY	4.09×10^8 rad in 24 FPY
Copper stabilizer displacement damage	$< 2.2 \times 10^{-5}$ displacement/atom	4.5×10^{-6} displacement/atom
Magnet heating	< 0.06 mW/cm ³	0.014 mW/cm ³
Shield activation		Ci/m
Shutdown		1.8×10^5
		$(1.6 \times 10^5)^*$
24 hr		6.8×10^4
		(3.4×10^4)
48 hr		5.2×10^4
		(3.2×10^4)

*Fe-1422 replaced HT-9 in shield

Fig. 4.4-16. Summary of shield design results.

(now dominated by W-187); after 2 days it is 5.16×10^4 Ci/m. With an Fe-1422 structure, the shield activity is 1.59×10^6 Ci/m at shutdown and is driven by Mn-56 for the first 16 hr. After 1 day, the activity level declines to 3.43×10^4 Ci/m (now mainly because of Mn-54) and to 3.17×10^4 Ci/m after 2 days. These results do not suggest a clear-cut choice between the 2 materials. At present, we have chosen HT-9 as the baseline for the shield, but Fe-1422 is an alternate.

To provide a lower activity alternative to the above shields, we are exploring the use of an aluminum alloy for the shield structure. The magnet protection requirements can be met with a 50-cm shield, although the safety margin is reduced. For an aluminum alloy shield, the shutdown activity will be mostly due to the 15-hr Na-24, though alloy additions such as Zn may also contribute. We are currently assessing the post-shutdown radiation doses that these shield options and the shield-transmitted decay radiation from the blanket would present during maintenance operations.

We are also in the process of analyzing the design of the shielding plugs needed to suppress streaming between the blanket modules. We are giving special attention to establishing the special shielding problems related to the helium cooling duct and manifold.

4.4.2.4 Thermal/Hydraulics

The total energy deposited in the shield is 15.2 MW. The shield will be water-cooled; if we assume a 50°C temperature rise, the flow required will be 72.7 liter/s for the entire reactor. Although we did not conduct a pressure drop analysis, if we assume a 2-atm pressure drop, then the pumping power will be only 15 kW.

We analyzed thermal profiles in the shield to determine the maximum possible temperature in the lead layer. The analysis was greatly simplified and conservative to ensure that we did not exceed the lead temperature limits. We assumed that all of the shield energy from the shield was deposited in the steel and lead zones and that the boron carbide layer was a perfect insulator. On this basis, the maximum lead temperature would be 31°C hotter than the inner (water-cooled) face of the shield. If the maximum inner-face temperatures were 80°C, then the maximum lead temperature would be 111°C, which is well under the allowable limit.

4.4.3 Vacuum Seals

In the solid breeder blanket, the vacuum boundary is not penetrated at any point by coolant pipes. It is a continuous wall containing only weld joints. The critical weld joint is located between the blanket modules; it is designed to take into account the relative growth between adjacent modules (see Fig. 4.4-7). The module joint has been designed to allow modules to be replaced remotely. The joint can be cut and rewelded by a remotely operated tool similar to that developed for TFTR.

We looked at various mechanical seals but none seemed to offer the vacuum integrity and remote maintenance applicability of the welded joint. A brazed alternative to the welded joint that removes the need for a cutting machine seems practical but it is not yet developed to a point where we can include it in this design.

4.4.4 Energy Conversion System

4.4.4.1 System Analysis

The most critical parameter in the energy conversion system is the maximum working fluid temperature, which largely determines the type of thermal-to-electrical conversion cycle and, ultimately, the energy conversion efficiency. For this design, helium is available from the blanket at a mean temperature of 630°C. This source temperature allows steam to be produced at about 550°C; thus, we are operating in a region similar to that of conventional fossil and HTGR plants. Because of the temperature limits imposed on the blanket by the high temperature sintering characteristics of the Li_2O , we have selected a conventional steam cycle for the power conversion system.

Figure 4.4-17 illustrates the steam cycle efficiencies of various existing and conceptual power plants and a standard steam cycle efficiency curve for a single reheat system. This gives a 42.5% cycle efficiency at a steam temperature of 550°C.

Figure 4.4-18 is the flow diagram of the basic power cycle which identifies the major system components. The cycle is quite conventional, with a single reheat and multiple stages of regenerative feedwater heaters (shown grouped for simplicity as HP and LP heaters). In this case, steam is

Fig. 4.4-17. Power plant steam cycle efficiencies.

Fig. 4.4-18. Basic power cycle.

extracted from the intermediate pressure (IPT) and low pressure (LPT) turbines to supply the feedwater heaters, which is conventional steam cycle practice, and we can select the 42.5% conversion efficiency noted above.

The MARS system, in addition to the power extracted from the blanket, has 371 MW_t of power that must be removed from the direct converter (12% of the blanket heat). As described in Section 3.6, this energy is extracted with pressurized water at a temperature of 215°C. The question is whether it is economically feasible to convert this heat to electricity.

The feedwater must be heated from a temperature of 38 to 245°C before entering the steam generator (see Fig. 4.4-18), which is an opportunity to introduce a 215°C heat source. The required power cycle modifications are: (1) substituting water-to-water heat exchangers for steam-to-water heat exchangers in the feedwater heating system, which should not be complex or expensive, and (2) modifying the IP and LP turbines to handle increased flow resulting from an absence of extraction steam. This modification is considered to be more critical and its extent is not yet known; however, conversations with turbine manufacturers indicated that it is feasible without having a significant effect on turbine efficiency.

The effects of this change were scoped based on the results from a similar study for LWRs.⁴ For MARS, there is energy available to perform 78% of the feedwater heating. At this level of feedwater heating, 18% of the added energy appeared as electricity. In other words, the efficiency of converting the energy in 215°C water was 18%.

The cycle shown in Fig. 4.4-19 converts 3067 MW_t from the blanket at 42.5% and 371 MW_t from the direct converter at 18%; this results in a total output of 1370 MW_e and an effective cycle efficiency of 39.8%.

Recovery of the 371 MW_t from the direct converter results in an additional output of 67 MW_e ; the question that we must address is whether this recovery is worthwhile. If we use a rough direct-cost figure of \$1500 to 1800/kWe, the modifications required to implement recovery of this direct converter heat could cost between \$100 and \$120 million. On this basis it would seem that recovery of this direct converter heat is economic; however, more study will be required.

Fig. 4.4-19. Power cycle using direct converter heat.

4.4.4.2 Component Description

Because the power cycle is conventional, we will not discuss the standard components, such as turbines, condensers and feedwater heaters. However, the boiler/superheater and reheater are unique and deserve discussion.

Figure 4.4-20 shows the temperature profiles on the helium and steam sides for both the boiler/superheater and the reheater. The heat exchangers are in parallel with a 3000-psig steam supply pressure and a 500-psig reheat pressure.

The multiple-unit boiler/superheater transfers 2637 MW and the reheater transfers 430 MW. Reference 5 describes conceptual designs for He/He heat exchangers up to 900 MW. Because the primary helium in each unit will be tritiated, both units must provide adequate barriers to prevent tritium migration into the steam system. We envision that the design will have a concentric tube configuration with extensive metal-to-metal contact to promote good heat transfer, but it will not have a metallurgical bond so that a crack will not propagate from one wall to the other. The gap between tubes will either be vented to a tritium leak detection device or will contain an oxidizing gas to convert the tritium to T_2O and thus inhibit its migration through the second wall. Table 4.4-2 lists the basic specifications for the heat exchangers.

4.4.5 Blanket Maintenance

4.4.5.1 Maintenance Philosophy

Any maintenance required inside the reactor building while the reactor is operating will be performed remotely. A few hours after the reactor has been shut down, personnel will be allowed into the reactor building to begin dismantling equipment outside the shield. The cooling system for the blanket and shield must be kept operating until the afterheat level is reduced. About 3 days after reactor shut down, the cooling systems can be deactivated and dismantled. From this point on, all operations must be done remotely because the first operation involves removing shield parts.

Fig. 4.4-20. Heat exchanger temperature profiles.

Table 4.4-2. Heat exchanger specification.

Parameter	Boiler/superheater	Reheater
Helium inlet temperature ($^{\circ}\text{C}$)	630	630
Helium outlet temperature ($^{\circ}\text{C}$)	300	300
Helium flow rate (kg/s)	1609	371
Helium pressure (atm)	30	30
Feedwater temperature ($^{\circ}\text{C}$)	245	-
Feedwater pressure (atm)	204	-
Steam inlet temperature ($^{\circ}\text{C}$)	-	293
Steam outlet temperature ($^{\circ}\text{C}$)	550	550
Steam flow rate (kg/s)	1070	963
Total rating (MW_T)	2492	575

4.4.5.2 Module Changeout Procedure

As we stated in Section 4.4, the module is defined as two magnets, a shield assembly and a blanket assembly with associated support structure (see Fig. 4.4-1). The total weight of the module is 781 tons.

After all electrical, fluid, and instrumentation lines have been disconnected, the joint shield segments are removed as shown in Fig. 4.4-21. The lower segments are moved to cradles on the floor and the upper segments crane hoisted to a suitable area adjacent to the machine.

The remote welder/cutter, located on the joint rails, is used to cut the module joint. The bellows in the joint are designed to spring back a small amount when the cut is made to provide clearance for module removal. One set of anchor brackets is removed and the module is moved on its rails to the maintenance location. Here the module is held in position by another set of anchor brackets.

A hoisting fixture is attached to one magnet (weighing 200 tons); this assembly is removed by gantry crane to a storage position inside the reactor enclosure. The second magnet is similarly removed and stored. The upper carriage structure is removed as shown in Fig. 4.4-22. The bolts attaching the upper shield to the lower shield are removed and the upper shield lifted off and stored. This now reveals the blanket assembly as shown in Fig. 4.4-23. The four bolts that attach the blanket assembly to its support are removed; the blanket is now ready to be hoisted and lowered onto a cradle trolley on rails adjacent to the door connecting the reactor enclosure with the hot cell. This completes the blanket module removal.

When reassembling the central cell module, the reverse procedure is followed until the replacement module is in the reactor. Before the weld joint is made, the module must be properly aligned by adjusting the anchor brackets, which have remote-controlled vertical and lateral adjusting features. When the module joint has been aligned, the welder cutter is positioned for a trimming cut to be made followed by the weld pass. The welder cutter is removed and stored and the joint shield segments bolted in place. Hands-on assembly of the remaining equipment can then proceed.

Fig. 4.4-21. Removal of central cell module.

Fig. 4.4-22. Removal of magnet and structure.

Fig. 4.4-23. Removal of shield and blanket.

4.4.6 Blanket Module Manufacturability

The materials chosen in the construction of the blanket have known fabrication processes and any potential problems are well-understood. The ferritic steel can be machined, formed, and welded. The weldments require preheating and post-weld heat treatment. The vanadium alloy can be machined and welded in a manner similar to that of titanium.

The ferritic alloy inner cylinders are shear-formed, have the grooves machined, the sections welded together, and the short joining rings of Inconel 625 welded to each end. The circumferential plenum sections are welded in place and then the inlet axial manifold is installed. The tube assembly sector rails are welded in position to complete the assembly of the inner cylinder.

The outer cylinder is also ferritic steel; the cylinder sections are rolled and welded and the sections are welded together to form the outer cylinders. Joining rings of Inconel 625 are added to the inner and outer cylinders and the end domes to enable final closing welds to be made without pre-weld heating or post-weld heat treatment. Cutouts are made for the inlet and outlet ducts; the inlet manifold reinforcing ring is welded in position. The support rings are now welded in place, and the outer wall cooling channels and ducts are welded to the inside of the wall and the inlet duct connecting piece. The exhaust duct nozzle is fabricated and welded to the outer cylinder. The tube sector assembly rails are welded in place to complete the outer cylinder assembly.

The end domes are cut from flat ferritic alloy plate and are then taper-machined and spun-formed to shape. Inconel 625 joining rings are welded to both the inner and outer cylindrical flanges. The SiC-filled antistreaming blocks are attached to the outside and the wall cooling channels and ducts are attached to the outside to complete the assembly.

The outer and inner wall assemblies are positioned with the inlet-duct connecting piece in place and cylinder axis vertical. The two pieces are fixtured rigidly together so that one layer of the tube assembly sectors can be inserted from the top, located, and pinned. Now the end dome can be positioned on top, located, and welded to both cylinders. The assembly is now inverted and the remaining layer of tube assembly sectors are inserted,

located, and pinned in position. The blanket can now be closed by positioning and welding the second end dome.

All welds can be x-rayed except the final end dome welds. These can be liquid-penetrant inspected after the root pass and at completion.

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