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Lawrence Livermore National Laboratory

TRW Energy Development Group

University of Wisconsin-Madison

General Dynamics/Convair Division

University of Washington

Grumman Aerospace Corporation

EBASCO Services, Inc.

Science Applications, Inc.

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Energy Technology Engineering Center (ETEC)

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12. Blanket and Reflector

12.1 DESIGN SUMMARY

12.1.1 Objectives

The main objective of the MARS central cell blanket and shield is to produce electricity in the most cost effective manner. This must be done in the safest manner possible and with the least long-term impact on the environment.

12.1.2 Capital Costs

One of our primary objectives was to minimize the cost of the product. We found that this could best be done 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 for the overall power cycle. In fact, the final figure of merit ($\$/kW_e$) strongly depends on the product of $M\eta$, which was maximized.

12.1.3 Ease of Fabrication

Many of the past designs for fusion power reactors have 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 Li-Pb 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.

12.1.4 Maintenance

The MARS Li-Pb 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 required between modules.
5. No movement of superconducting coils required for the removal of damaged modules.
6. "Hands on" maintenance at the back of the shield within two days after reactor shutdown.

The present design incorporates all of the above features.

12.1.5 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, the blanket components last only a few years), it was necessary to choose a material that is highly resistant to radiation damage and also contains the fewest possible elements that yield long-lived isotopes. The ferritic alloy, HT-9, satisfies these requirements and can be in fact considered as Class C waste for near-surface burial.

12.1.6 General Features of MARS Lithium-Lead Blanket

Figure 12-1 shows an isometric view of the blanket and Fig. 12-2 shows a vertical cross section of the central cell containing the $\text{Li}_{17}\text{Pb}_{83}$ blanket modules. Figure 12-3 gives the horizontal cross section, and Table 12-1 lists the main features of the blanket design.

Fig. 12-1. Isometric view of the Li-Pb blanket.

Fig. 12-2. Vertical cross section of the central cell of the Li-Pb blanket.

Fig. 12-3. Horizontal cross section of the Li-Pb blanket.

Table 12-1. Major Li-Pb blanket parameters.

Total central cell power (MW_{th})	2811
Power deposited in the Li-Pb blanket (MW_{th})	2081
Structural material	HT-9
Blanket coolant and breeder	$Li_{17}Pb_{83}$
First wall radius (cm)	60
Neutron wall loading (MW/m^2)	4.3
Blanket/reflector multiplication (M)	1.362
Tritium breeding ratio	1.15
Blanket T_2 inventory (kg)	0.007
Net conversion efficiency (%)	42.1

The basic design of the blanket is quite simple. The $Li_{17}Pb_{83}$ alloy (Li-Pb) is fed at 350°C to the HT-9 blanket module through a large coolant header at the top. From there, it flows (in parallel) through 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 the blanket multiplication factor M. At 500°C, the Li-Pb 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 and they also lower the liquid metal velocity, which in turn lowers the corrosion rate.

The central-cell neutron-produced power is 2811 MW_{th} . This energy is deposited in a 38.2-cm-thick blanket (2081 MW_{th}) module and a 43-cm-thick reflector (730 MW_{th}). 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 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 1.15 from the Li-Pb, which is enriched to 90% Li-6. An important feature of the Li-Pb system is its low tritium solubility; only 7 g of tritium are 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.

Finally the net conversion efficiency, including the pumping power for the Li-Pb, is 42.1%. 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.

12.2 DESIGN ALTERNATIVES

The main choice to be made in any blanket design is the form of the tritium breeding material (e.g., solid or liquid). In MARS, one solid (Li_2O) and two liquids (Li and the $\text{Li}_{17}\text{Pb}_{83}$ eutectic) were originally considered.

The choice between solid and liquid breeders was made on the following basis. First, the energy multiplication of solid breeder blankets is generally lower than for liquids. Second, the solid breeders quite often require neutron multipliers, which increases the complexity of the blanket design. The third reason had to do with the overall complexity of a blanket that requires a high pressure coolant to be separated from the breeder. Such a configuration necessitates numerous welds and very precise machining. The extra structural material required in solid breeder blankets to withstand the pressure also reduces the tritium breeding ratio. Fourth, solid breeders have high tritium inventories. Values ranging from 3 to 100 kg per 1000 MW_{th} have been predicted, but the uncertainty in the tritium diffusivity and retention data makes any conclusive statement impossible at this time. Finally, the radiation stability of solid breeder ceramics is unknown at this time and useful lifetimes at high temperatures may be such as to cause premature replacement of blankets.

Of all the liquid breeder systems, we chose the Li-Pb system for one major reason: safety. Liquid lithium/water reactions are notoriously violent and we felt that with a reactor system that contains $\sim 10^5$ Ci, we could not afford to incorporate all the safety features required to ensure that no $\text{Li}/\text{H}_2\text{O}$, air, or concrete reactions would take place during the lifetime of the plant.

We also chose a Li-Pb system because of tritium inventory. The present MARS system has only 1 g of tritium in the blanket Li-Pb at any one time. A similar Li system would have an inventory on the order of 1 kg or more. Finally central-cell solenoidal magnets could be made smaller with a Li-Pb system compared with a Li system, that is, Li-Pb is a better shield material.

An indication of the attractiveness of the Li-Pb system is given in Fig. 12-4 where the tritium breeding ratios quoted in the literature are plotted vs the quoted blanket energy multiplication value M . The best blanket (given equal safety, design and fabrication ease, and cost) is one that has the highest M and still achieves a satisfactory (i.e., >1.10) tritium breeding ratio. Note that STARFIRE and UWMAC-II are solid breeder concepts (UWMAC-II also used Be), UWMAC-I and UWMAC-III are Li systems, and NUWMAC, UWTOR-M, MARS, and WITAMIR are Li-Pb systems. The reason NUWMAC has a low M is because it incorporates an H_2O -cooled titanium alloy structure with a $\text{Li}_{38}\text{Pb}_{62}$ composition. UWTOR-M has a low M because of high neutron leakage through large diverter slots. The "best" blankets are MARS and WITAMIR, both of which use HT-9 as a structural material along with $\text{Li}_{17}\text{Pb}_{83}$.

Fig. 12-4. Nuclear performance of fusion reactor blankets.

The usefulness of the MARS blanket system is shown even better in Fig. 12-5. Here the energy multiplication times the net system efficiency is plotted vs the TBR. Again, MARS has the best characteristics if the minimum tritium breeding ratio is considered to be 1.1.

12.3 BLANKET CONFIGURATION SELECTION

Midway through the MARS study, the decision was made to use the liquid Li-Pb blanket as the reference concept. A brief description of the design configuration is given below. The Li-Pb 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 12-2 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. The 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. Table 12-2 contains the pertinent mechanical parameters of the blanket.

Fig. 12-5. Nuclear performance of fusion reactor blankets.

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 72 blanket modules of identical axial length, is 129 m (cold). 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 thermal stress problems. The vacuum boundary is defined as the inner surface of the reflector. Bellows and O-rings ensure vacuum tightness at the penetrations in the shield and reflector, which are required for coolant supply pipes.

Table 2-2. Mechanical parameters for the blanket module.

Structural material	HT-9
Axial length, room temperature (cm)	179.2
Axial length, operating temperature (cm)	180.25
Inner radius (cm)	60
Outer radius (cm)	99
Tube zone thickness (cm)	19.2
Beam zone thickness (cm)	19
Tube outer diameter (cm)	10.14
Tube wall thickness (cm)	0.23
Axial tube spacing (cm)	10.53
Radial tube spacing (cm)	9.05
Li ₁₇ Pb ₈₃ weight contained in a module (ton)	29.5
Structural material weight of a module, drained (ton)	2.7

The attractive features of this design are:

1. Both the blanket module configuration and fabrication are simple.
2. The coolant channels in the blanket module and the connected pipe headers are drainable.
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.

12.4 STRUCTURAL MATERIAL FOR MARS

12.4.1 General Considerations

Because MARS is to be considered as a tenth-of-a-kind fusion power plant, the structural materials to be used for its design should be those with a broad enough data base in high radiation fields and high temperature environments to make a reasonable estimate of each material's lifetime. Accordingly, the two alloy classes considered as potential candidates were the austenitic stainless steels and the ferritic (or martensitic) steels. The major disadvantage of the austenitic stainless steels is their propensity for swelling. This is clearly demonstrated by considering the swelling behavior of both austenitic type 316 stainless steel and ferritic steels, based on breeder reactor irradiation at about 500°C. As seen in Fig. 12-6, austenitic steels exhibit a rapid rate of swelling after an incubation dose varying

Fig. 12-6. Summary of recent swelling results for stainless steels irradiated with neutrons. Both austenitic and ferritic alloys show an incubation period before swelling starts, but the steady-state swelling rate is $\sim 1\%$ per year for austenitic alloys and 0.1% per dpa for ferritic alloys.

between 30 and 60 dpa, depending on the particular heat treatment. Even more advanced alloys such as PCA (not shown in Fig. 12-6) are expected to reach the 1% per dpa value at damage levels exceeding 70 to 100 dpa. In contrast, ferritic steels have not exhibited more than 1% swelling in experiments conducted up to 80 dpa. When ferritics do pass beyond the transition fluence, they are not expected to swell faster than 0.1% dpa. Based on breeder reactor experience, it appears that the blanket components exposed to the highest neutron flux for 2 years in MARS would swell about 20 to 30% if made of 20% CW type 316 stainless steel. Although the blanket designs contemplated for MARS could accommodate a linear expansion of a few percent due to swelling, we doubt that a significantly larger expansion could be accommodated in a power-producing tandem mirror fusion reactor. Therefore, we chose the ferritic stainless steels for MARS.

In the course of this design study, we thought that ferritic steels used as structural materials for the blankets may significantly perturb and lower the magnetic field and its distribution in the central cell region, particularly at either end of the central cell. The amount of field perturbation and reduction that can be tolerated in a mirror reactor is not known at the present time. It appears, however, that these issues can be resolved by appropriate designs of the blanket modules and by a modest increase in the magnetic field in the central cell region.

Our study has shown that ferritic steels are preferable to austenitic steels for the following reasons:

1. Much greater resistance to neutron-induced void formation and metal swelling.
2. A greater resistance to in-reactor creep below 550°C.
3. Better ductility retained after high temperature fission neutron irradiation.
4. Better thermal stress resistance, at least by a factor of two.
5. Better compatibility with the $\text{Li}_{17}\text{Pb}_{83}$ breeder/coolant material.
6. Considerably more experience in heat exchanger design and aqueous environments.
7. Lower material cost per unit weight.
8. Less demand on critical resources such as Cr (12 vs 18%) and Ni (0.5 vs ~8%) which generally have to be imported.

The specific choice of HT-9 over other ferritic steels should not be taken too seriously. This alloy system has been identified as one particularly attractive to the fast breeder and, as such, gives us a convenient data base from which to extrapolate its performance in a fusion environment. It is quite possible that other ferritic steels might be developed to give even better properties in a fusion environment and future studies ought to be open to new ideas in this area.

12.4.2 Physical and Thermal Properties of HT-9

The general class of alloys that we considered are the ferromagnetic steels that contain roughly 9 to 13% Cr with some small addition of various strengthening and stabilizing elements such as Mo. These steels are conventionally used in the normalized and tempered condition for high temperature applications. Although heat treatment can result in either a tempered martensite or bainite structure, depending on the alloy and thermal treatment parameters, this class of materials is usually referred to as "martensitic" stainless steels for simplicity.

Because of the encouraging and rapidly expanding data base from the U.S. Fast Breeder Reactor Program and the significant operational history in Europe, the alloy Sandvick HT-9 (DIMX20CrMoWV121) was selected for MARS. The nominal composition of this alloy is given in Table 12-3, and Fig. 12-7 gives the binary section of the Fe-Cr-C phase diagram at 12%.¹ For high temperatures and extended time service, the alloy is usually heat treated at 1050°C for 0.5 hours in the austenite zone (Fig. 12-8), air cooled, and then tempered at 780°C.

Table 12-3. Nominal composition
of HT-0 alloy.

Element	Wt%
Iron	Balance
Chromium	11.5
Molybdenum	1.0
Nickel	0.5
Tungsten	0.5
Manganese	0.5
Vanadium	0.3
Silicon	0.25
Carbon	0.20

12.4.3 Use of Martensitic Steels

It is fortunate that there are so many applications for martensitic steels throughout the U.S. and the world. A recent summary by scientists at GA Technologies revealed that there are major applications for high temperature martensitic stainless steels in the following areas:²

- Fossile power plants--superheaters, reheaters, evaporators, steam piping;
- Nuclear power plants--superheaters, reheaters, [U.K. Hinkley Point "B" Advanced Gas-Cooled Reactor (AGR)], steam generator: [U.K. Prototype Fast Reactor (PFR)], He compressors, circulators, [U.S. Fort St. Vrain High Temperature Gas Reactor (HTGR)];
- Other Applications--gas and steam turbine components, aircraft and missile thin-wall pressure vessels, petroleum industry pressure vessel linings.

Fig. 12-7. Binary section of the Fe-Cr-C phase diagram at 12% chromium.

Fig. 12-8. Typical heat treatment profile for a martensitic stainless steel.

The past and current conditions under which 9-12 Cr steels are used in foreign fossil power plants are reported in Table 12-4. Table 12-5 indicates future uses that are being considered for 9-12 Cr steels. From the GA Technologies summary documenting the extensive experience in using 9-12 Cr steels, we obtained a great deal of the mechanical property data that we summarize in the next section.

12.4.4 Mechanical Properties of HT-9

Figure 12-9 compares the tensile strength of HT-9 with the strengths of several other candidate CTR materials. It shows that HT-9 is superior to annealed 316 SS up to ~600°C and that it compares favorably to Ti-6Al-4V in

Table 12-4. Location and use of 9-12 Cr steels in fossil power plants outside the U.S.

Location	Usage	Alloy	Temperature (°C)	Service status (hours)
U.K.	13 plants	9 CrMo(V)	450-660	<110,000
	12 plants	12 CrMo	430-850	<45,000
Austria	--	--	--	--
Belgium	--	--	--	--
Czechoslovakia	96 tons	9 CrMo	<610	≤110,000
Germany	--	--	--	--
Switzerland	--	--	--	--
Turkey	>250 tons	12 CrMo(V,W)	<610	≤130,000
Yugoslavia	--	--	--	--

Table 12-5. Possible future uses of 9-12 Cr steels.

System	Application	Alloys
US-LMFBR	Ducts (cladding) Steam generator	HT-9 MOD 9 Cr-1 Mo
US-GCFR	Ducts, cladding, steam generator, shielding	HT-9/MOD 9 Cr-1 Mo
French Super	Ducts	9-17 Cr
Phenix	Steam generator	9 Cr-1 Mo
Japanese FBR	Steam generator	9 Cr-2 Mo
US-HTGR	Steam generator and pumping, He-He heat exchangers, He circulators	HT-9/MOD 9 Cr-1 Mo
US-LWR	Steam generator	HT-9/MOD 9 Cr-1 Mo

Fig. 12-9. (a) Ultimate tensile strength vs temperature curves for candidate CTR alloys. (b) Tensile strength vs temperature curves for candidate CTR alloys.

the 400 to 600°C range. However, above 500°C the yield strength begins to drop, and it is best to limit its operating range to below 500 to 550°C.

The effect of long term exposure at 600°C on the tensile properties of HT-9 is shown in Fig. 12-10.³ Exposure to 600°C temperatures for 80,000 hours (~10 years) reveals that there is very little degradation of ultimate or 0.2% yield strength. The percentage elongation also stays between a comfortable 10 and 30% even after long term exposure. A recent study⁴ also reports that HT-9 specimens subjected to 1 atm of hydrogen gas during testing at 200 to 400°C had no effect on ductility.

Fig. 12-10. Effect of 80,000 hour, 600°C service exposure on HT-9 tensile properties (Ref. 3).

The ductility of HT-9 is compared with other CTR alloys in Fig. 12-11 and reveals that HT-9 retains a ductility in the 400 to 600°C range, which is comparable to 316 SS.^{5,6}

Long-term stress rupture data for HT-9 and other alloys at 500°C are shown in Fig. 12-12. This information reveals that for up to 10 years of exposure, HT-9 can withstand stresses as high as ~200 MPa.

The effect of 80,000-hour 600°C service exposure on HT-9 stress-rupture properties is shown in Fig. 12-13. There is a slight degradation of the properties, but it appears that stresses of 20 ksi (~130 MPa) or more are quite reasonable for 100,000-hour exposures.

Fig. 12-11. Total tensile elongation test temperature for candidate CTR alloys (Ref. 6).

Fig. 12-12. Stress rupture data at 500°C for candidate CTR alloys (Ref. 6).

Fig. 12-13. Effect of 80,000 hour, 600°C service exposure on HT-9 stress-rupture properties (Ref. 2).

12.4.5 Thermal Stress Factor

When rigidly held plates are subjected to high heat fluxes, the resulting differential thermal expansion generates large stresses and in extreme circumstances can even cause failure. This can be a particularly difficult problem for tokamaks.

A typical figure of merit that has been used to indicate the resistance of metals to failure is the ratio of the yield stress to the thermally induced stress. If we assume equal thickness and nuclear heat generation rates, then the figure of merit M (a high value is desired) is

$$M = \frac{2\sigma_y K(1 - \nu)}{\alpha E}$$

where σ_y is the yield strength, K is the thermal conductivity, ν is Poisson's ratio, α is the thermal expansion coefficient, and E is Young's modulus. Typical values of M have been calculated at 500°C for 316 SS, HT-9, Ti alloys, Inconel 718, and V-20 Ti. These values are given in Table 12-6.

Table 12-6. Thermal stress resistance of potential fusion materials at 500°C.

Alloy	Thermal stress resistance (W/m x 10 ⁻³)
Annealed 316 SS	1.3
20% CW 316 SS	4.9
HT-9	8.5
Ti-6Al-4V	10.2
Ti-6Al-2Sn-4Zr-2Mo	10.4
Inconel 718	10.7
V-20 Ti	12.2

Note that the HT-9 alloy has a figure of merit of at least twice that of cold-worked 316 SS and slightly less than Ti and Ni alloys at 500°C. The HT-9 is clearly inferior to refractory metals, but from a lack of extensive irradiative experience, we will not consider those systems in MARS.

12.4.6 Irradiation Effects on HT-9

There are two major areas in which irradiation data is important to the lifetime analysis of HT-9 in MARS: void swelling and irradiation induced shift in the ductile-to-brittle transition temperature (DBTT). A complete review of the available data will not be given here, but a summary of the important features is included in the following sections.

12.4.6.1 Void Swelling in HT-9 Alloys. A summary of the data on swelling in HT-9 (or similar alloys) is given in Table 12-7.⁷⁻¹⁰ There have been at least four different studies, mostly conducted in fast fission reactors. These investigations cover the temperature range from 300°C to 640°C. The fast neutron fluence studied ranged from 2.3×10^{22} to 5.7×10^{22} n/cm², or from ~ 10 to 36 dpa. The important point to note from Table 12-7 is that

Table 12-7. Recent swelling data of 12 Cr-1 Mo steels.

Material	Temp (°C)	Fluence, >0.1 MeV (n/m ²)	Reactor Type	$\Delta V/V$ (%)	Ave. void density (No./m ³)	Ave. void diameter (nm)	Reference
HT-9	300	4.9 x 10 ²⁶ (~36 dpa)	HFIR	Negligible	Few	~4	(6)
	400	4.9 x 10 ²⁶ (~36 dpa)	HFIR	0.07	3 x 10 ²⁰	~16 (Large, ~25) (Small, ~5)	
	500	4.9 x 10 ²⁶ (~36 dpa)	HFIR	Negligible	Few	~3	
	600	4.9 x 10 ²⁶ (~36 dpa)	HFIR	Negligible	Few	~5	
	400	2.3 x 10 ²⁶	EBR-II	None	---	---	(7)
	450	2.3 x 10 ²⁶	EBR-II	None	---	---	
	500	2.3 x 10 ²⁶	EBR-II	None	---	---	
	550	2.5 x 10 ²⁶	EBR-II	None	---	---	
FV-448 and CRM-12	460	4.6 x 10 ²⁶ to 5.7 x 10 ²⁶ (~23 dpa)	DFR	<0.1 ^a			(8)
	530			<0.1 ^a			
	535			<0.1 ^a			
	625			<0.1 ^a			
	640			<0.1 ^a			
FV-448 and CRM-12	425	6.15 ~ 7.1 x 10 ²⁶ (~30 dpa)	DFR	<0.1 ^a	---	---	(8)
	450						
	510						
	630						
FV-448	380	30 dpa (N/2)	DFR	<0.1 ^a			(9)
	420	9900 hrs		None			
	460	in Rx		None			
	615			None			

^aBelow detection limit.

even at 36 dpa, the irradiation-induced swelling is less than 0.1% at 400°C and is negligible at the other temperatures. With such low swelling values, it is very difficult to construct a swelling design equation. However, Ghoniem¹¹ has attempted to model the swelling of ferritic steels as a function of Cr content, such that

$$\frac{\Delta V}{V} (\%) = \exp \left[- \left(\frac{T - T_p}{W} \right)^2 \right] (0.36 \delta - 0.074) [\phi(\text{Cr})] , \quad (12-1)$$

where

$$\phi(\text{Cr}) = \begin{cases} 0.067 \text{ Cr}^2 - 0.457 \text{ Cr} + 1.0 & (\text{Cr} < 5\%) \\ 0.37 \text{ Cr} + 0.237 & (\text{Cr} > 5\%) \end{cases} , \quad (12-2)$$

Cr = chromium concentration, % .

In Eq. (12-1) W ($= 59^\circ\text{C}$) is the full width of the assumed Gaussian distribution, δ is the displacement dose (dpa), T is the irradiation temperature ($^\circ\text{C}$), and T_p is the peak swelling temperature given by

$$T_p (^\circ\text{C}) = 420 + 727 \ln \left[\frac{P (\text{dpa/s})}{10^{-6}} \right] .$$

For MARS, $\text{Cr} = 12\%$ so $\phi(\text{Cr}) = 0.681$, $T_p = 420$ to 425°C , and $W = 59^\circ\text{C}$. Hence the swelling equation becomes

$$\frac{\Delta V}{V} (\%) = (0.68) \exp \left[- \left(\frac{T - 425}{59} \right)^2 \right] [0.036 \delta - 0.074] .$$

For 60 dpa per full power year (FPY) the maximum swelling rate at 425°C (after the transient period has been exceeded) is 1.42% per FPY. Since the transient period for HT-9 is certainly more than 36 dpa and probably more than 100 dpa, one would expect less than 2% swelling after 3 FPY of MARS operation. Thereafter, one might obtain ~1.4% swelling per FPY.

12.4.6.2 Radiation Embrittlement. Brittleness is a normal property of most solids, including metals and alloys at low temperature.¹² Only face-centered-cubic (FCC) metals are commonly ductile at low temperature. One of the most serious concerns is the embrittling effect of neutron irradiation because it produces brittle fracture even at relatively high temperatures. A measure of brittle fracture is the transition temperature between brittle behavior at low temperature and ductile behavior at high temperature. The DBTT can be defined as the intersection of the fracture (σ_f) and yield (σ_y) stresses. At low temperature, the material undergoes brittle fracture before reaching the yield point. The effect of irradiation is to harden the alloy by introducing vacancy and interstitial loop obstacles to dislocation motion. The effect is a significant increase in the yield stress ($\Delta\sigma_y$) and only a small elevation of the fracture strength ($\Delta\sigma_f$). The shift in DBTT can then be geometrically defined as (see Fig. 12-14)

Fig. 12-14. A schematic illustration of the concept of the shift in the DBTT. σ_y = yield stress, σ_1 = fraction stress, ϕ for fluence, and ΔT is the DBTT shift.

$$\Delta \text{DBTT} = \Delta T_\phi = \frac{\Delta \sigma_{1\phi} - \Delta \sigma_{y\phi}}{\partial \sigma_y / \partial T} \quad (12-3)$$

A rather consistent trend-band for increases in DBTT with integrated neutron fluence has been observed for several irradiated steels. However, the rate of embrittlement decreases with each increment of neutron dosage. Data from the light water reactor (LWR) surveillance programs show a saturation effect in the increase in DBTT with integrated fluence.^{13,14} The data indicate that saturation effects are real and that saturation in DBTT occurs at a lower fluence for a higher temperature. It must be remembered, however, that the effects of hydrogen and helium gas produced under typical fusion reactor conditions are not included in such studies. Recent neutron results on the embrittlement behavior of the high chromium alloy, HT-9, show a tendency toward temper embrittlement at 538°C.¹⁵ A DBTT shift of 108°C is measured at an irradiation temperature of 419°C.

Composition, heat treatment, and type of steel are known to influence the neutron irradiation embrittlement response of steels. For the purpose of the present analysis, such effects will be ignored, and the validity of one design equation will be assumed regardless of compositional variations. Furthermore, the DBTT will be assumed to be about 0°C before irradiation. Again, composition and heat treatment affect the initial DBTT (within $\pm 50^\circ\text{C}$). A design equation for the shift in DBTT, that is based on hardening theories,¹⁶ can be then written in the form

$$\Delta\text{DBTT} = \frac{1.87 \times 10^4}{T-288} \left\{ 1 - \exp \left[- \left(\frac{4T - 350}{T} \right) \delta \right] \right\}^{1/2} \quad (12-4)$$

This equation is valid above approximately 250°C. The data, together with the fitting equation, are shown in Fig. 12-15. It can be seen that in the 350 to 500°C range for MARS, the DBTT should never exceed 200°C, well below the melting point of $\text{Li}_7\text{Pb}_{83}$. Therefore we do not expect the shift in the DBTT to be a problem for MARS.

Fig. 12-15. The shift in the DBTT as a function of irradiation dose.

12.5 MECHANICAL DESIGN

12.5.1 Overall Layout

The design of the blanket in MARS takes maximum advantage of the linear geometry of the central cell. Thus, the blanket, reflector, and shield consist of concentric cylinders, one within the other. Coolant connections for the blanket penetrate both shield and reflector, whereas the coolant connections for the reflector penetrate the shield only.

We designed the segmentation of the blanket in the central cell with the following considerations:

1. A blanket module should be of a reasonable size and weight to facilitate handling by remote control.
2. A blanket module must be able to be extracted from the central cell without displacing the coils.
3. Segmentation must be such as to allow proper clearance for making coolant connections between the central cell coils.

Figure 12-16 is a lengthwise cross section of a portion of the central cell showing two service stations. It can be seen that blanket modules line up between coils only in the service station and nowhere else. The six blanket modules that do not line up between coils have coolant connections at different axial locations, the first three modules being mirror images of the second three modules. The modules in the service stations have connections in the center. Thus, although in all other respects the blanket modules are identical, they differ in the axial location of the coolant connection. This should not present any difficulty, since the welding of the manifold cover with the coolant connection already attached to it will be the last step in the production of a blanket module. All the previous operations leading up to the last step are performed on a mass production basis.

There are 72 blanket modules in the central cell, each 179.2 cm long (the expanded length at operating temperature is 180.3 cm). Ten service stations are equally spaced along the central cell, each servicing seven blanket modules with the exception of the two stations on the extreme ends, which service eight modules each. Blanket modules are translated axially within the reflector for removal through the service station. Maintenance of the blanket module is discussed in Sec. 18.2 of this volume.

The reflector and shield are also segmented so that they and the central cell coils will be easier to maintain. Figure 12-16 shows that one sector consists of two central cell coils with their associated reflector and shield mounted on a support structure and rails to allow a displacement perpendicular to the axis of the reactor. Every other sector contains a service station. Note that the blanket modules must be removed prior to sector removal, because they do not line up with the parting lines between sectors. These sectors will only be removed from the reactor in the event of a failure of a central cell coil, reflector, or shield. The probability of such a failure is extremely low.

Fig. 12-16. Lengthwise cross section of central cell showing two service stations.

12.5.2 Blanket Modules

The MARS blanket, is composed of two distinct zones, a tubular zone 19.2 cm thick and a beam zone 19 cm thick constructed from the ferritic steel HT-9. (Figure 12-17 shows a plan view of the blanket.) The tubular zone consists of two rows of tubes, each having a 10.14-cm outer diameter and a wall thickness of 0.23 cm, spaced on a triangular pitch. Radial center-to-center spacing is 9.05 cm and axial spacing is 10.53 cm. The radius of curvature to the center of the front row is 65.7 cm, putting the effective first wall at a radius of 60 cm. The first row comprises 34 tubes, 17 on each side, and the second row comprises 32 tubes, 16 on each side, for a total 66 tubes in each module. Table 12-8 gives the actual material fractions in each of the zones.

The second zone consists of a beam-like structure with rectangular passages for the flow of $\text{Li}_7\text{Pb}_{33}$. This zone has a higher steel fraction and is designed to be the primary load-carrying component in the blanket. Figure 12-18 shows the edge of a blanket module with details of both zones. The beam zone is fabricated from 12- by 19-cm I beams that are bent to the proper radius of curvature and then welded flange-to-flange. The end closure is ellipsoidal and can be readily fabricated by the spinning technique.

Fig. 12-17. Plan view of blanket module.

Table 12-8. Material fractions in the blanket zones.

Tubular zone	
Inner radius (cm)	60
Outer radius (cm)	79.2
Material fractions in vol%	
Li ₁₇ Pb ₈₃	73.7
HT-9	7.1
Void	19.2
Beam structural zone	
Inner radius (cm)	80
Outer radius (cm)	99
Material fractions in vol%	
Li ₁₇ Pb ₈₃	85
HT-9	14.4
Void	0.6

Fig. 12-18. Edge of blanket module showing details of the tubular and beam zones.

As shown in Fig. 12-17, the upper and lower manifolds are stepped at the interface between the manifolds and the two zones. This is done to facilitate the assembly of the modules where the tubes and beams will be welded to the manifolds from inside the manifold. Obviously, the manifold cover with the header connections in place will be welded to the manifolds after the tubes and beams have been welded and the weldments tested with x-ray and other techniques.

The joining of the tubes to the manifold is critical from several standpoints. It is imperative that the weldment be of excellent quality. At the same time, a certain amount of flexibility is desirable at the joint. For assembly purposes, the tubes are swaged down to ~80% of the original diameter to allow more space between tubes at the interface with the manifold. Figure 12-19 shows the detail of a tube end and Fig. 12-20 shows a proposed weld configuration. We feel that such a combination, namely making the tube more flexible by reducing the moment of inertia at the end and welding it to a lip on the manifold side, will give the needed flexibility while providing a good weldment. The liquid metal side will have a smooth transition with no nucleating points for corrosion. The vacuum side will have no corrosive materials to initiate problems at the interface between the tubes and the manifold. Although such a configuration is not the best from the standpoint of good high-vacuum practice, we believe that with adequate preparation and bake-out, the outgassing problem will not be prohibitive, especially in view of the high pumping capacities provided within the system.

Fig. 12-19. Detail of tube end.

Outside diameter = 10.14 cm

Inside Diameter = 9.69 cm

$t = 0.0225$ cm

Outside diameter = 8.0 cm

Inside diameter = 7.7 cm

$t = 0.15$ cm

Fig. 12-20. Proposed tube-manifold weld configuration.

The blanket modules are supported on the reflector, which in turn is supported by the shield. Thus, the load path goes from the blanket to the reflector, to the shield, and finally through the supports to the reactor floor. Figure 12-17 shows a support plate on either side of a blanket module welded to the outer surface of the beam zone at an angle of 45° . The support plates bear against ledges machined into the reflector for an even load distribution over the entire length of the module. The compressive stress at the interface is <3 MPa.

Figures 12-21 and 12-22 contain additional views of the blanket module. Figure 12-21 is a side view with the upper quarter removed to show the details within the manifold. Note the ample space provided between the tubes for welding. On the beam zone side, the web material is machined away to make a smooth lip for joining to the manifold. In this figure, the header connection is in the center of the manifold, which is consistent with the requirement for the modules in the service station slot. The two other blanket module varieties have the header connection displaced 65 and 18 cm from the center,

Fig. 12-21. Side view of blanket module with upper quarter removed.

respectively. Figure 12-22 is a view of the blanket module from below, with the lower left quarter removed. It shows the cross section of the two zones at midplane and the joints between the tubes and the upper manifold. Part of the tube manifold interface on the inside of the lower manifold can be seen through the coolant header connection.

Fabrication of the modules appears to be fairly straightforward. Only two tube shapes are needed, 2448 for the first row and 2304 for the second. These tubes lend themselves to easy mass production. Furthermore, the beam zone structures, of which there are 144 units, are identical in all respects and can also be mass produced.

The manifolds are hot-formed from 0.7-cm-thick HT-9 sheets. The holes are drilled undersize and the lips are drawn out toward the inside of the manifold. Another drilling operation is needed to size the holes properly and reduce the lips to the desired thickness. The openings for the beam zone structures are then machined.

The tubes and the beam structures can now be assembled onto the manifolds. This involves extensive set up and alignment before welding. However, with proper fixtures this step can be accelerated substantially. The welding will be performed by automatic welding devices that provide very uniform and reproducible welds.

Fig. 12-22. View of blanket module from below with lower left quarter removed.

It is very likely that more time and effort will be spent in verifying welds than in actually fabricating the modules. Fabricators have indicated that more than half of the cost of producing the blanket will be in weld verification, testing and quality assurance.

Adding the beam zone structure and finally the manifold covers completes the assembly of the modules. These welds will also have to be x-rayed. The remaining steps involve heat treating, cleaning, and packaging the modules for shipment to the reactor site.

Table 12-9 gives the structural parameters of the blanket module.

12.5.3 Header Connections

Each blanket module is connected to supply and return headers at the top and bottom. The $\text{Li}_7\text{Pb}_{83}$ breeding/cooling material enters the module at the top, is distributed axially in the manifold, and then flows circumferentially through the tubes and beams, collecting in the lower manifold and exiting through the return header. The joints between the headers and the module must be easy to operate by remote control and must be highly reliable.

Table 12-9. Blanket module structural parameters.

Number of modules	72
Outer diameter (cm)	198
Axial length, cold (cm)	179.2
Axial length, hot (cm)	180.25
Number of tubes in first row	34
Number of tubes in second row	32
Tube outer diameter (cm)	10.14
Tube wall thickness (cm)	0.23
Tube axial spacing (cm)	10.53
Tube radial spacing (cm)	9.05
Web thickness in beam zone (cm)	0.5
Average flange thickness in beam zone (cm)	0.7
Number of rectangular passages	30
Dimension of rectangular passage (cm x cm)	17.6 x 11.5
Thickness of manifold material (cm)	0.7
Mass of empty blanket module (kg)	2734
Mass of module filled with $\text{Li}_{17}\text{Pb}_{83}$ (tonnes)	29.47
Compressive stress at support (MPa)	2.7

Figure 12-23 is a cross section of the MARS central cell at a service station showing the supply and return lines. The coolant supply line enters the service station cover through a sealed flexible penetration that allows relative motion of the header and the service station cover. Furthermore, the header itself has a flexible joint in the form of an omega bellows, shown in Fig. 12-23 at the 2.5-m mark from the plasma center. This flexible joint makes possible minor angular adjustment of the header flange for easier alignment at the connection to the blanket module. The connection itself can be seen just above the one meter mark on the figure.

Although the service station is an evacuated enclosure, a seal is needed where the coolant line enters the removable reflector/shield segment. The

Fig. 12-23. Cross section of central cell at a service station.

same seal exists at each penetration of a coolant line into the shield. Thus, there are 144 such seals (as in the bottom of Fig. 12-23), which form the vacuum barrier for the central cell at this point.

Figure 12-24 depicts an expansion of this region showing details of the penetration and the seal. The double bellows are needed to remotely operate the clamping mechanism at the header connection to the blanket module.

The seal between the header and the blanket module is affected by a frozen layer of $\text{Li}_{17}\text{Pb}_{83}$, which happens to be a good solder. The expanded view of the flanges at the joint in Fig. 12-24 and the detailed drawing in Fig. 12-25 show how this seal works. The flange attached to the blanket module contains two rectangular grooves for placement of the $\text{Li}_{17}\text{Pb}_{83}$ rings. The mating flange, namely that attached to the coolant line, has two cooling channels machined into it that align with the $\text{Li}_{17}\text{Pb}_{83}$ grooves

Fig. 12-24. Overall view of the header connection.

Fig. 12-25. Details of the flanges at the header connection.

when the flanges are brought together. The clamping mechanism is operated from outside the double bellows by means of pneumatic drivers that actuate a floating flange located between the bellows. Levers mounted on the floating flange operate hinged brackets that can clamp the seal flanges with a locking grip. This is accomplished by outwardly displacing the floating flange (i.e., away from the seal) which causes the hinged brackets to lock the seal flanges together. Conversely, an inward motion of the floating flange releases the grip by swinging the hinged brackets away.

The $\text{Li}_{17}\text{Pb}_{83}$ rings are melted by circulating hot helium gas through the coolant channels. Once the sealing material is molten and spreads between the flange surfaces, the heaters on the cooling helium lines are turned off and the cooling gas freezes the solder. The solder is maintained in the frozen state by continuously circulating the cold helium gas through the cooling channels in the flange.

This seal is also equipped with an automatic leak detector, a means of seal verification and redundancy. Figure 12-25 shows a groove in the header flange located midway between the coolant channels. This groove contains two concentric copper rings embedded in a ceramic insulator. The rings come close to, but do not touch, the surface of the opposite flange. In the event of a leak, the groove will fill up with $\text{Li}_{17}\text{Pb}_{83}$, shorting the two rings and tripping an alarm. Such an alarm not only indicates a leak, but immediately identifies the leaky joint. In addition, a small vacuum line attached to the groove can be used to initially verify the seal by simply evacuating the space between the two concentric solder pools. The coolant lines, the vacuum line, and the alarm circuit are attached to the seal flange from the backside and penetrate the vacuum barrier through the stationary flange on which the pneumatic drivers are located.

To provide redundancy, each coolant line is supplied with coolant gas from independent sources. Thus, if one coolant circuit fails, the other will continue to ensure that the seal will remain frozen. There are 18 independent coolant circuits for the whole reactor, each circuit feeding a single line in 16 seals. This is intended to prevent a common mode failure.

Heat transfer from the flowing $\text{Li}_{17}\text{Pb}_{83}$ in the reactor coolant line to the seal flange is substantially reduced by a sealed layer of insulation in the header wall. This insulation is needed to reduce the magnetohydrodynamic (MHD) pressure drop due to a liquid metal flowing across magnetic field lines. Instead of inducing eddy currents in the 0.5-cm-thick header wall, they are induced in the 0.125-cm-thick sheath that seals the insulation. Thus, the eddy currents are much lower in magnitude, reducing the MHD pressure drop. This insulation, which can either be mica or graphite, also reduces the thermal stress on the seal flanges. The thermal hydraulic parameters of the seal are given in Table 12-10.

Table 12-10. Thermal hydraulic parameters of the $\text{Li}_{17}\text{Pb}_{83}$ -sealed header connection.

Insulating material	
Mica (cm)	0.38
Graphite (cm)	0.15
Heat conducted to first coolant line (kW)	1.10
Nuclear heat to first coolant line (kW)	0.85
Total heat to first coolant line (kW)	1.95
Total heat to second coolant line (kW)	0.80
Helium coolant flow rate/line (kg/s)	4.1×10^{-3}
Helium velocity (m/s)	15
Helium coolant pressure (MPa)	0.2
Inlet temperature (°C)	54
Outlet temperature (°C)	146
Pressure drop (MPa)	0.03
Hoop stress in pipe (MPa)	82
Thermal stress in flange (MPa)	139

A major advantage of this type of seal has to do with blanket maintenance. During routine blanket maintenance, the headers for all the blanket modules being replaced must be disconnected. Once the $\text{Li}_{17}\text{Pb}_{83}$ has been drained from the blanket and the coolant lines have been valved off, the headers are disconnected by simply melting the frozen layer of $\text{Li}_{17}\text{Pb}_{83}$ with hot helium gas and then releasing the grip provided by the hinged bracket. In principle, both functions can be accomplished at the touch of two buttons or switches. Retracting the headers between the service stations provides clearance to move the blanket modules axially within the reflector. All of these operations are performed without disturbing the vacuum seal between the coolant header and the shield. Table 12-11 summarizes the salient features of this seal.

Table 12-11. Features of the $\text{Li}_{17}\text{Pb}_{83}$ -sealed header connection.

- Actuated remotely with relative ease
- Provides leak detection
- Provides initial verification
- Provides redundancy
- Is self healing
- Compatible with the chosen blanket maintenance scheme
- Leaves the vacuum barrier undisturbed

12.5.4 Service Station

The central cell has 10 equally spaced service stations. Figure 12-16 shows two of the stations. These service stations provide access to the inside of the central cell for routine and unexpected maintenance.

Figure 12-23 is a cross section of the central cell through a service station and shows the shape of the reflector/shield at this location. Note that the upper half of the reflector/shield in the service station has a flat horizontal surface on the top. This is done purposely to provide a flat sealing surface between the removable reflector/shield segment and the stationary one. Preliminary indications are that elastomer seals can be used here. Service connections, such as cooling lines and diagnostic channels, to the removable segment are made inside the service station.

Each service station is 3.7 x 1.88 m. The cover is located to barely clear the upper surface of the central cell coil dewars, namely at 3.75 m from the center of the plasma. As shown in the Fig. 12-23, the box is welded to the outer surface of the shield but is actually sealed to the reflector, which constitutes the vacuum boundary for the central cell. This is accomplished by extending the reflector surface over the upper edge of the shield in the

service station, where it is welded to the side of the station box. The box itself is fabricated from 2-cm-thick Fe 1422 (NONMAGNE-30)¹⁷ reinforced with girders on the outside in order to withstand the vacuum load. The service station cover is sealed to the box with a conventional elastomer o-ring. The maximum dose rate for an organic material at this point is ~ 100 rad/hour, which gives a silicon rubber compound o-ring, for example, a useful life of 15 years based on a limit of 10^7 rad. These seals will be replaced on a schedule equal to the lifetime of a blanket module, namely every 4 to 5 years. The station cover will also be reinforced on the inside and will have a single penetration for the coolant header. The seal at the penetration is also elastomeric, with the flange attached to the header on the underside of the cover, to permit removal of the cover before disconnecting and removing the header.

The service station is evacuated to provide a secondary vacuum buffer against leaks at the removable reflector/shield interface with the stationary reflector. A roughing vacuum on the order of 10^{-3} Torr will suffice, and is achieved using small pumps attached to the wall of the service station.

12.5.5 Reflector Design

The reflector is a cylindrical structure with an inner radius of 101 cm and an outer radius of 144 cm. There are 19 sectors that are 6.32 m long and 2 sectors that are 5.08 m long. The short sectors are located at either end of the central cell. The reflector is made from Fe 1422 (Nonmagne-30)¹⁷ and is water-cooled, with a volumetric fraction of 95% steel and 5% water. The water inlet temperature is 280°C, the outlet temperature is 320°C, and the pressure is 13.8 MPa. The reflector has a notch on the top and bottom, which is needed to allow the axial translation of the blanket modules, since the coolant connection on the blanket module manifolds extends 7.4 cm above the manifold, or to a radius of 104.5 cm from the plasma center.

The relatively high water pressure in the reflector, combined with thermal stresses in the inner reflector surface that are induced by the drop-off in nuclear heating and one side cooling, necessitated a somewhat unconventional reflector design. Figure 12-26 shows the overall shape of the reflector in plan view, with an expanded cross section at A-A. The reflector essentially consists of concentric cylinders of Fe 1422. The first cylinder has a minor radius of 101 cm and a major radius of 114.5 cm. The outer surface of the cylinder is deeply grooved, as shown in the figure. These grooves, spaced at 5-cm intervals from center to center, are 12.5 cm deep and 1.0 cm wide. They come to within 1 cm of the inner surface for expressly one reason, to minimize the thermal stresses. This is discussed further in Sec. 12.9 of this volume. However, the hoop stress caused by the water pressure is carried by all the material in the cylinder minus the grooves. The filler rings in the grooves are there only to fill the space with steel and provide a controlled-thickness gap for the cooling water. They are segmented and thus can be laid into the grooves and fastened with ease.

The second, third, and fourth cylinders only need to be continuous in the longitudinal direction. These cylinder segments have a dual purpose--to resist the axial force of the water pressure and to fill the space with steel needed for the reflector. The end plates are fastened to these longitudinally

Fig. 12-26. Details of the reflector design.

continuous segments of the cylinders, providing the structure needed to react the axial force within the reflector as a result of the water pressure. Recessed bolts with welded covers can be used for fastening. The segments are spaced with shims to maintain the desired gaps for the coolant.

The last cylinder has a function similar to the first and is designed in the same way. The thermal stress is somewhat lower because the backside of the reflector can radiate to the cooler shield that surrounds it. Thus, the temperature gradient across the last cylinder is lower.

Although the reflector components are massive, there is nothing complicated about their construction. Lathes and milling machines exist today that are capable of fabricating cylinders of this size. Perhaps the most difficult aspect of the reflector to fabricate is the notch that accommodates the coolant connection in the blanket module. The notch itself is only 5.5 cm deep at the maximum point, which is 40% of the cylinder thickness. It may be possible to machine the notches in the material stock before rolling it into a

cylinder. Another way is to fabricate the cylinder out of two halves and then weld them with the notches already machined. The support ledges for the blanket modules are machined at the same time. In any event, the grooves on the outer side are machined after the cylinder is rolled and welded. A rectangular cover plate will then be needed to seal the grooves that have broken through the notch. This plate will be welded to the continuous inner surface of the cylinder and then tack-welded to the back structure through the machined grooves.

In comparison with the first and last cylinders, fabricating the "guts" of the reflector is trivial because the "guts" are simply plates rolled into cylindrical segments. Fabricating the last cylinder is somewhat difficult because the grooves are located on the inside. Again, it may be possible to machine the grooves into the material stock before rolling it. If this is not possible, then it may be possible to machine them from inside the cylinder. This brings up the question of how long the cylinders should be. Although the reflector module is 6.32 m long, there is no reason why it could not be fabricated from several cylindrical assemblies and then welded together. Obviously, axial alignment has to be maintained and the joining weld must be vacuum tight because the reflector is the primary vacuum barrier in the central cell. Furthermore, penetrations for the coolant headers must be provided. These penetrations are located at the notches and there will be six or eight such penetrations in each reflector module, depending on its location. These penetrations are machined into the fabricated cylinders. A sleeve is then inserted into the hole and welded to both the inner and outer cylinders. The final step in fabricating a reflector assembly is to weld the end plates, shown in Fig. 12-26, and bolt them to the intermediate cylinder segments. Each bolt is then sealed with a welded disk over it.

The reflector modules are joined together by expandable bellows, to accommodate the increase in length during operation. Such an omega bellows, located at the back surface of the reflector, would have a major radius of 1.5 m and be capable of a deflection of 3.2 cm. Omega bellows of this size have been fabricated for bubble chambers and are well within present day technology. Table 12-12 gives the structural parameters of the reflector.

12.6 NEUTRONICS

The primary goal of this neutronics analysis is to determine the blanket design that yields the largest energy multiplication (M) with an adequate tritium breeding ratio (T). This will have an impact on reducing the overall cost of produced electricity. A value of $T \approx 1.1$ is required in MARS to account for any deficiencies and uncertainties in the calculational models and cross section data used and to allow for tritium losses and radioactive decay plus supplying fuel for startup of other fusion reactors.

The liquid metal eutectic $\text{Li}_{17}\text{Pb}_{83}$ is used in MARS as the breeder and coolant. The ferritic steel alloy HT-9 is used for the structural material in the blanket. The blanket includes a front tube zone composed of several rows of closely packed 10.14-cm-diameter tubes 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 provides structural support for the blanket. The blanket is located next to a water-cooled reflector. The energy deposited in the blanket and reflector is

Table 12-12. Structural parameters of reflector.

Material of construction	Fe 1422 (Nonmagne -30)
Inner radius (cm)	101
Outer radius (cm)	144
Length of modules	
19 modules (m)	6.32
2 modules (m)	5.08
Maximum structure temperature ($^{\circ}\text{C}$)	340
Design coolant pressure (MPa)	13.8
Yield strength of Fe 1422 at 340°C (MPa)	245
Maximum design stress intensity (MPa)	147
Mass of reflector module	
6.32 m long (tonnes)	149
5.08 m long (tonnes)	119.9
Total mass of reflector (tonnes)	3070.6

recovered and represents the reactor's thermal power. A set of one-dimensional neutronics calculations has been performed to determine the appropriate composition and thickness for the different blanket and reflector zones. We have performed three-dimensional calculations for the final blanket design to determine its neutronics characteristics and have taken into account the detailed geometrical configuration in these calculations.

12.6.1 Choice of Calculational Method and Cross Section Data

The discrete ordinates and Monte Carlo methods are the main calculational methods used in neutronics analyses. Different codes based on these methods are available. To compare the accuracy of these methods, a benchmark calculation of tritium breeding in a liquid lithium blanket with a niobium structure was performed using 100-group cross section data based on the ENDF/B-III evaluation.¹⁸ We obtained excellent agreement between the results of Monte Carlo and discrete ordinates codes. Later results showed that good agreement can still be obtained for this benchmark problem with

fewer energy groups and different cross section processing codes. As a result, discrete ordinates codes such as ANISN¹⁹ and ONEDANT,²⁰ with 25-, 30-, and 46- group cross section libraries based on ENDF/B-IV and ENDF/B-V have been used for neutronics analyses of fusion reactor designs.

For fusion blankets that use Li₇Pb₈₃ and steel, a much softer spectrum is obtained and large atom fractions of iron and lead exist. We performed calculations for the WITAMIR blanket design²¹ using different codes and cross section libraries. Deviations up to 20% were obtained as indicated by the results in Table 12-13. However, the different codes gave similar results when the same cross section data were used, implying that the differences are related to using different group structures, data processing codes, and weighting spectra. This discrepancy motivated us to set a benchmark problem for a Li-Pb blanket using HT-9 as structure.²² Extensive calculations were performed to investigate the effects of blanket thickness, lithium enrichment, group structure, and weighting spectra on the accuracy of the calculated tritium breeding ratio. The results of this study revealed that T is very sensitive to the group structure and weighting spectra with the effect being more pronounced for thin blankets and low lithium enrichments. Therefore, in this work the coupled neutron-gamma continuous-energy MCNP²³ Monte Carlo code is used to eliminate the energy group structure effects. MCNP is used for both one- and three- dimensional calculations. Monte Carlo codes run quite fast for simple one-dimensional geometries. Since we are interested only in integral quantities such as T and M, low statistical uncertainties can be achieved with a reasonable number of histories. Four thousand histories were used in the calculations, resulting in statistical uncertainties of less than 1% for the quantities of interest.

Cross section data libraries based on the ENDF/B-IV and ENDF/B-V evaluations are available for use with MCNP. Our results show that using ENDF/B-IV data tends to overestimate T and underestimate M, when compared to ENDF/B-V data, for a Li-Pb system. Therefore, the cross section data library based on the most recent ENDF/B-V evaluation is used in this work.

Since the Monte Carlo method is not efficient in handling deep penetration problems and for accurately predicting local radiation effects,

Table 12-13. Tritium breeding in WITAMIR using different codes and data.

Method	Code	Number of groups	Source of data	Tritium breeding ratio
Monte Carlo	MCNP	Continuous energy	ENDF/B-IV	0.9858
P ₃ S ₈	ANISN	25	ENDF/B-IV	1.0682
P ₃ S ₈	ONEDANT	30	ENDF/B-IV	0.8719
Monte Carlo	TARTNP	175	ENDL	0.9226
Monte Carlo	MCNP	Continuous energy	ENDF/B-V	0.9636
P ₃ S ₈	ONEDANT	30	ENDF/B-V	0.8562

the one-dimensional discrete ordinates code ONEDANT was used to analyze the shield and to determine the spatial variation of the different radiation effects in the blanket. We used the P_3S_8 approximation with a coupled 46-neutron, 21 gamma-group cross-section library based on the VITAMIN-C data library²⁴ and the MACKLIB-IV-82 response library.²⁵

12.6.2 Neutron Multiplication and Choice of Blanket Thickness

Neutron multiplication via the $Pb(n,2n)$ reaction results in attractive neutronics performance in Li-Pb blankets. A neutron multiplication (M_n) of up to 1.8 can be achieved in a $Li_{17}Pb_{83}$ blanket, which has an impact on increasing both T and M. The neutron multiplication increases as the blanket thickness increases, reaching a saturation level at a blanket thickness of approximately 30 cm, as shown in Fig. 12-27. No further increase in M_n is obtained beyond this thickness because most of the neutrons would have been slowed down below the threshold energy for the $Pb(n,2n)$ reaction. This implies that for neutron multiplication, the lead in the back region of the blanket is ineffective. Hence, a thin blanket design was considered. However, lithium enrichment in 6Li is essential to achieve adequate tritium breeding in such a design.

Fig. 12-27. Variation of neutron multiplication with $Li_{17}Pb_{83}$ blanket thickness.

Neutronics calculations were performed for a thin blanket design in which the lithium is enriched to 90% 6Li and compared with a thick blanket design in which natural lithium is used. Both designs use HT-9 for structure in the blanket and reflector. A 28-cm-thick reflector is used in both designs. Table 12-14 lists the characteristics of the thin and thick blankets. A saving of 49 cm in blanket thickness was achieved for approximately the same tritium breeding ratio. This translates to a savings of 37 cm in the inner radius of the magnet, which reduced the central cell cost. Furthermore, the blanket weighs less and can be easily changed out with a reduced amount of activated waste. However, the thin blanket with the HT-9 reflector produces less energy multiplication than the thick blanket.

Table 12-14. Characteristics of thin and thick blankets.

	Thin blanket	Thick blanket
Blanket thickness (cm)	38.2	87.2
Reflector thickness (cm)	28	28
Shield thickness (cm)	56	44
Inner radius of magnet winding pack (cm)	210	247
Li enrichment (% ^6Li)	90	7.42
Tritium breeding ratio	1.15	1.10
Energy multiplication	1.26	1.35

12.6.3 Enhancement of Energy Multiplication for the Thin Blanket

Analyzing the breakdown of the capital cost of a mirror reactor plant, we notice that the cost of the central cell constitutes only a small fraction of the total cost. As a result, the significant savings in central cell cost obtained by using a thin blanket design implies only a small savings in the total capital cost of the reactor. On the other hand, the net electrical power of the system is almost directly proportional to M . Therefore, it is vital to maximize the energy multiplication to enhance the overall economic figure of merit.²⁶ We made several attempts to enhance M while preserving the attractive features associated with the thin blanket design. Two such ways were considered and are discussed below.

12.6.3.1 Structural Material Content in the Blanket. From structural, thermal-hydraulics, and stress considerations, the volumetric fractions in the blanket tube zone have been chosen to be 73.7 vol% $\text{Li}_{17}\text{Pb}_{83}$, 7.1 vol% HT-9, and 19.2 vol% void. The thickness of this zone is 19.2 cm corresponding to two rows of tubes. The beam zone for the thin blanket design has, therefore, a thickness of 19 cm.

Decreasing the structure content in the beam zone was found to increase M . Figure 12-28 shows this trend for different Li-Pb blanket thicknesses.²⁷ For a given blanket thickness, both T and M increase as the structure content decreases. For certain required value of T , larger M is obtained when a thinner blanket is used with a smaller structure content. We have chosen a beam zone consisting of 85 vol% $\text{Li}_{17}\text{Pb}_{83}$ and 15 vol% HT-9 to achieve high M while providing adequate support for the blanket.

Fig. 12-28. The T-M plot showing effects of blanket thickness and structure content.

12.6.3.2 Choice of Reflector Material and Thickness. The reflector is water-cooled and the energy deposited in it constitutes a part of the recoverable energy. The energy multiplication is defined as the ratio of the total recoverable energy deposited in both the blanket and reflector to the fusion neutron energy of 14.1 MeV per DT fusion. Therefore, M can be further enhanced by the appropriate choice of reflector material and thickness. 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. Materials considered in this analysis include 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 62 wt% Fe), yielded the largest energy multiplication. This is primarily due to the large gamma production in manganese. Furthermore, β decay of ^{56}Mn , which is a product of the $^{55}\text{Mn}(n,\alpha)$ reaction, contributes to local neutron heating.

Increasing the reflector thickness was found to increase M further reaching a saturation value after a thickness of approximately 43 cm. Figure 12-29 shows the effect of reflector thickness and material on energy multiplication for a thin blanket using 90% enriched lithium and a thick

Fig. 12-29. Effect of reflector material and thickness on energy multiplication.

blanket with natural lithium. Both blanket designs give similar values of T . The conditions for the designs compared in Table 12-14 are represented by the triangles. Increasing the Fe-1422 reflector thickness from 28 to 43 cm for the thin blanket increases M from 1.35 to 1.39. The fraction of energy deposited in the reflector increased from 24.5 to 27.6%, whereas the energy deposited as low grade heat in the shield decreased from 3.85 to 0.76%. It is clear that the improvement in M is more pronounced for the thin blanket design. According to this analysis, a 43-cm-thick Fe-1422 reflector was chosen for use with the thin blanket design. Table 12-15 gives the compositions and thicknesses chosen for the different zones of the MARS blanket and reflector. The main disadvantage of the thin blanket design is the large fraction of heat deposited in the water-cooled reflector, which reduces the overall thermal efficiency. This was taken into account in a detailed cost analysis, which revealed that the thin blanket design has a better overall figure of merit than the thick blanket design.²⁶

Table 12-15. Dimensions and composition of blanket and reflector.

	Thickness (cm)	Composition
Blanket tube zone	19.2	73.7 vol% $\text{Li}_{17}\text{Pb}_{83}$, 7.1 vol% HT-9
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

12.6.4 Neutronics Performance of the Li-Pb Blanket Based on the One-Dimensional Model

A schematic of the MARS Li-Pb blanket, reflector, shield, and magnet is given in Fig. 12-30. A summary of the tritium production results obtained for this one-dimensional model is given in Table 12-16. 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 12-17. The total recoverable energy is 19.6 MeV/fusion, which corresponds to an overall energy multiplication of 1.39. About 60% of the total heating comes from gamma heating; this is a direct result of using a metallic reflector.

Figure 12-31 shows the spatial variation of power density in the reactor. The contributions from neutron and gamma heating are also shown. The results are normalized to a neutron wall loading of 4.3 MW/m^2 at the first wall radius of 60 cm. The peak power densities in the blanket, reflector, and shield are 30, 4, and 0.1 W/cm^3 , respectively. The power density increases as one moves from the tube to the beam zone at the interface between the two zones as a result of the 19.2% void fraction in the tube zone. The peaking at the back of the beam zone is attributed to the absorption in lead of the gammas generated in the metallic reflector. It is this strong gamma absorption in lead that results in shifting the peak power density in the reflector away from the interface with the blanket.

Figure 12-32 gives the spatial variation of displacement damage (dpa) and helium production rates. Because of the relatively large threshold energy for the (n,α) reaction, the helium production rate has a steeper spatial variation than the dpa rate. While the dpa rate drops by a factor of $\sqrt{15}$ from the front to the back of the blanket, the helium production rate drops by

0			
44	Plasma		44
60	Void		16
79.2	Blanket I 73.7 vol % $\text{Li}_{17}\text{PB}_{23}$, 7.1 vol % HT-9		19.2
80.0	Void		0.8
99	Blanket II 85 vol % $\text{Li}_{17}\text{PB}_{23}$, 15 vol % HT-9		19.0
101	Void		2
	Reflector 95 vol % Fe 1422, 5 vol % H_2O		43
144	Shield A 95 vol % Fe 1422, 5 vol % H_2O	10	
154	Shield B 86 vol % B_4C , 10 vol % Fe 1422, 4 vol % H_2O	23	
177	Shield C 90 vol % Pb, 5 vol % Fe 1422, 5 vol % H_2O	8	
185	Void		7
192	Dewar 304 SS		1
193	5 vol % Kapton		3
196	LN_2 radiation shield (1/8-in. 304 SS)		1
197	5 vol % Kapton		7
204	Casing 304 SS		5
209	Polyimide		1
210	Winding pack		
	35 vol % SS 304 15 vol % polyimide 31.5 vol % Cu 3.6 vol % NbTi 15 vol % liq. He		117.3
327.3			
Radius (cm)		Thickness (cm)	

Fig. 12-30. Schematic of blanket, reflector, shield, and magnet.

Table 12-16. Tritium production (tritons/fusion).

	${}^6\text{Li}(n,\alpha)\text{t}$	${}^7\text{Li}(n,n'\alpha)\text{t}$	Total
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 12-17. Nuclear heating (MeV/fusion).

	Neutron	Gamma	Total
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

a factor of ~ 100 . The peak dpa and helium production rates at the first wall are 60.7 dpa/FPY and 492.1 He appm/FPY, respectively.

12.6.5 Three-Dimensional Analysis

To investigate the effects of the detailed blanket structural design on the neutronics performance, we carried out a three-dimensional calculation. The geometrical configuration of the tubes, beams, and Li-Pb manifolds was included in the calculational model. The two rows of tubes are arranged with axial and radial spacings of 10.45 and 9.05 cm, respectively. Because of the repeated nature of the tubular zone, only a 5.225 cm wide unit cell was

Fig. 12-31. Spatial variation of power density.

modeled surrounded with two reflecting boundaries. A horizontal cross section of the unit cell used in the calculation is shown in Fig. 12-33. One-half of one of the I beams used to fabricate the beam zone is shown. The reflector and shield zones were included as homogenized regions in the calculational model. A vertical cross section of the model at the midplane of the unit cell is shown in Fig. 12-34. Because of symmetry, only one quarter of the unit cell was modeled with reflecting boundaries. Eight thousand histories were used in the MCNP calculation, which yielded statistical uncertainties of less than 1% in the overall tritium breeding ratio and energy multiplication.

Fig. 12-32. Spatial variation of dpa and helium production rates.

Fig. 12-33. Horizontal cross section of the unit cell used in the three-dimensional calculation.

Fig. 12-34. Vertical cross section of the calculational model.

The calculated values for tritium production and nuclear heating in the different zones are given in Table 12-18. Comparing these results with the results of the one-dimensional calculation in which the blanket was represented by two homogenized zones, we notice that consideration of the blanket heterogeneity increases the tritium breeding ratio by 1.6% and decreases the energy multiplication by 1%. The increased tritium production is attributed to the fact that the secondary neutrons produced by the $\text{Pb}(n,2n)$ reaction in Li-Pb have a greater chance to breed tritium in the surrounding Li than in the homogenized case where the structure is distributed uniformly throughout the blanket. Because more neutrons are absorbed in ${}^6\text{Li}$, thus producing less energy than if they were parasitically absorbed in the structure, a smaller energy multiplication of 1.377 is obtained.

This three-dimensional analysis is also aimed at determining the spatial distribution of the dpa rate in the blanket structure, which is an important input for the stress analysis discussed in Sec. 12.9. For this purpose, the walls of the front and back tubes in the model were divided into several zones. Table 12-19 gives the dpa rate variation along the circumference of the tubes and at the front of the beam zone. The results are normalized to a neutron wall loading of 4.3 MW/m^2 at a wall radius of 60 cm and are accurate to within $\pm 1.5\%$. We did not observe any significant poloidal variation of damage other than the variation within the statistical uncertainty.

The final mechanical design of the blanket and reflector includes two penetrations, 52 cm in diameter, in the reflector for each blanket module. These penetrations are required for the Li-Pb header pipes. A 10-cm-high and 52-cm-wide notch in the reflector is used to allow space for the header flange

Table 12-18. Three-dimensional results for tritium production and nuclear heating.

	Tritium production (tritons/fusion)	Nuclear heating (MeV/fusion)
Front tubes	0.248	4.71
Back tubes	0.230	2.88
Beam zone	0.461	4.02
Manifolds	0.213	2.53
Reflector	--	5.27
Total	1.152	19.41

Table 12-19. Spatial variation of dpa rate (dpa/FPY) in HT-9 structure of the blanket.

Front tube		Back tube		Front of beam	
Front	60.0	Front	31.9	Behind front tube	18.5
Side	43.2	Side	24.2	Center	17.7
Back	30.9	Back	18.8	Behind back tube	17.9

during axial translation of the module. Two of them are needed, one at the top and bottom and they extend along the whole central cell. These geometrical perturbations to the reflector were not included in the three-dimensional calculational model. Including them will result in extremely large computing time and memory because the unit cell cannot be used and the whole module with 66 tubes has to be modeled. Instead of performing this expensive calculation, we have estimated the impact of these geometrical variations on the overall neutronics performance of the blanket.

The header penetrations and notches in the reflector represent ~5.5% of its volume. The energy deposited in the reflector is, therefore, estimated to reduce to 4.98 MeV/fusion. The extra Li-Pb in the header pipes occupies 1.74% of the reflector volume. We estimated the extra nuclear heating deposited in these pipes to be 0.06 MeV/fusion. By modifying the values in Table 12-18, we get a total recoverable energy of 19.18 MeV/fusion, implying that the overall energy multiplication is 1.36. Based on a central-cell

fusion power (14.1 MeV n + 3.5 MeV α) of 2580 MW, we calculated the total thermal power to be 2811 MW with 2081 MW from the blanket and 730 MW from the reflector. The geometrical perturbations in the reflector are not expected to change the calculated local tritium breeding ratio of 1.152. The reactor overall tritium breeding ratio can be obtained by multiplying the local value by the ratio of the blanket fusion power to the total reactor fusion power. This yields an overall reactor tritium breeding ratio of 1.12.

12.7 THERMAL HYDRAULICS

The flow of a liquid metal perpendicular to a strong magnetic field is called Hartmann flow. The flow is characterized by laminar flow with a constant velocity profile and very a thin boundary layer. The heat transfer is thus dominated by conduction and is predictable. Because of the lack of turbulence, the effect of MHD is to suppress heat transfer, which makes a liquid metal a poor heat transfer medium in an MHD environment. However, 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, the tube flow suggested here can provide enough cooling to the first wall.

For most two-dimensional heat conduction problems, the governing equation can be written as

$$\rho C_p \left(v_x \frac{\partial T}{\partial x} + v_y \frac{\partial T}{\partial y} \right) = k \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} \right) + q \quad (12-5)$$

where

- x = direction along first wall,
- y = direction perpendicular to first wall,
- ρ = density,
- C_p = specific heat,
- v = velocity,
- T = temperature,
- k = thermal conductivity,
- q = heat generation rate.

Because the neutronics calculation assumes a concentric cylinder geometry, the heat transfer calculation assumes the same geometry. Thus, the first bank of tubes is assumed to be a concentric cylinder rather than individual tubes.

Since $v_y = 0$, and assuming that $\partial^2 T / \partial x^2 \ll \partial^2 T / \partial y^2$, Eq. (12-5) can be written as

$$\rho C_p v_x \frac{\partial T}{\partial x} = k \left(\frac{\partial^2 T}{\partial y^2} \right) + q \quad (12-6)$$

The quantity x/v_x is the time required for coolant to reach location x from entrance and can be designed as t. Since v_x is a constant, Eq. (12-6) can be rewritten as

$$\rho C_p \frac{\partial T}{\partial t} = k \left(\frac{\partial^2 T}{\partial y^2} \right) + q \quad (12-7)$$

where

$$t = x/v_x \quad (12-8)$$

Equation (12-7) is the same equation as conduction in a stationary system. Therefore, by allowing coordinate x to travel at the same velocity as the coolant, the heat transfer problem is reduced to a conduction problem in a stationary system. The boundary conditions of the problem are

$$T = T_0 \text{ at } t = 0 \quad (12-9)$$

$$\frac{\partial T}{\partial y} = -q''/k \text{ at } y = 0 \quad (12-10)$$

$$\frac{\partial T}{\partial y} = -0 \text{ at } y = \delta \quad (12-11)$$

in which q'' is the surface heating load from the plasma and δ is the thickness of the first tube. The nuclear heating rate in the blanket, q , is obtained from the neutronic calculations and is shown in Fig. 12-35. This curve, together with Eqs. (12-7 and 12-9 through 12-11), completely define the problem of the heat transfer.

An exact solution for a conduction heat transfer problem with both surface heating and nonuniform volumetric heating can be obtained only if the volumetric heating is in a certain form, such as an exponential. However, a numerical solution is easily obtainable by using finite difference techniques. The energy balance equations for each control volume can be written as

$$T_i' = T_i + [\sum C_{ij}(T_j - T_i)/(\rho C_p)_i + Q_i/\rho C_p] \Delta \theta \quad (12-12)$$

where C_{ij} is the conductance between zones i and j with $\Delta \theta$ defined by

$$\Delta \theta \leq (\rho C_p)_i / \sum_j C_{ij} \quad (12-13)$$

In this case, $\Delta \theta$ is the maximum time step to ensure convergence of the iteration process. The resulting temperature distribution of the first tube at the coolant exit is shown in Fig. 12-36. The maximum coolant/structural interface temperature is at this point and is 545°C.

The reflector is cooled by high pressure water to be fed to a separate saturated water cycle. To obtain a power cycle with good efficiency, we selected a temperature range of 280 to 320°C. The reflector design is shown in Fig. 12-37 and a summary of the parameters are shown in Table 12-20.

Fig. 12-35. Nuclear heating rate of the MARS blanket.

Fig. 12-36. Temperature of the first tube at the exit end.

Table 12-20. Thermal hydraulic parameters for each reflector module.

	Zone I	Zone II	Zone III	Zone IV	Reflector module
Inner/outer, radius (cm)	101/116	116/120	120/130	130/144	101/144
Nuclear heating (MW)	22.03	4.03	4.03	2.1	32.2
Nuclear heating (%)	68.5	12.5	12.5	6.5	100
Water temperature in/out(°C)	280/307.4	307.4/320	307.4/320	307.4/320	280/320
Coolant passage (mm)	2	2.5	2.5	1.35	---
Mass flow (kg/s)	179	71	71	37	179
Velocity (m/s)	0.94	1.03	1.03	0.64	--
Pressure drop (k/Pa)	1.94	1.37	1.37	1.40	3.31

The heat deposition in the reflector has been shown in Fig. 12-35. The maximum volumetric heating rate is 4.5 W/cm^3 . Since the distance between coolant channels is 4 cm, the maximum coolant surface heat flux is 9 W/cm^2 . The heat transfer coefficient of the coolant can be calculated by

$$\frac{hD}{k} = 0.023 \left(\frac{Dv\rho}{\mu} \right)^{0.8} \left(\frac{C_p \rho \mu}{k} \right)^{0.4},$$

where

- h = the heat transfer coefficient,
- D = the diameter (0.2 cm),
- k = thermal conductivity ($6 \times 10^{-3} \text{ W/cm} \cdot ^\circ\text{C}$),
- v = velocity (94 cm/s),
- ρ = density (0.78 g/cm^3),
- μ = viscosity ($1 \times 10^{-3} \text{ g/s} \cdot \text{cm}$),
- C_p = specific heat $5 \text{ W/s} \cdot ^\circ\text{C}$.

The heat transfer coefficient is calculated to be $1.4 \text{ W/(cm}^2 \cdot ^\circ\text{C)}$. Therefore, the temperature difference between the structural surface and the bulk coolant is only 6.5°C . The velocity used above is, therefore, sufficient for heat removal. Because the maximum water temperature is 326.5°C and the pressure

of the water should be higher than the saturation pressure, which is 12.2 MPa, we use a coolant pressure of 13.8 MPa. The maximum structural temperature is calculated to be 340°C.

Table 12-21 summarizes the more important parameters of the blanket and reflector.

Table 12-21. Thermal-hydraulic parameters for the MARS blanket.

Fusion power (MW)	2580
Blanket power (MW)	2081
Reflector power (MW)	730
Blanket coolant	Li ₁₇ Pb ₈₃
Coolant temperature (°C)	350/500
Structural temperature (°C)	350/550
Maximum coolant velocity in blanket (m/s)	0.17
Coolant velocity in feed/discharge pipe (m/s)	1.34
Coolant flow rate (kg/hr)	3.2 x 10 ⁸
Reflector coolant	H ₂ O
Coolant temperature (°C)	280/320
Structural temperature (°C)	280/340
Maximum coolant velocity (m/s)	1.03
Coolant pressure (MPa)	13.8
Coolant flow rate (kg/hr)	1.26 x 10 ⁷

12.8 MAGNETOHYDRODYNAMIC EFFECTS

A detailed discussion of MHD effects is available in Ref. 28. A brief discussion and introduction to the working equation is given here. The most important effects of the magnetic field on the reactor coolant design are the suppression of turbulence and the increase in flow resistance. These effects are discussed separately below.

12.8.1 Turbulence Suppression

The flow regime in a tube or duct depends mainly on the following dimensionless groups:

$$\begin{aligned}\text{Reynolds number } Re &= 4 R_H v \rho / \mu, \\ \text{Transverse Hartmann number } \bar{H} &= R_H B \sqrt{\sigma / \mu}, \\ \text{Parallel Hartmann number } H &= R_H B \sqrt{\sigma / \mu},\end{aligned}$$

where

R_H = hydraulic radius (cross section area divided by wetted perimeter),
 v = bulk velocity,
 ρ = density,
 μ = viscosity,
 B = component of the magnetic field perpendicular to the bulk velocity,
 B = component of the magnetic field parallel to the bulk velocity,
 σ = electrical conductivity of the coolant.

The Reynolds number is the main criterion for the onset of turbulence in the absence of magnetic effects. The transition Reynolds number is around 2300 for fully developed flow in a smooth-walled tube.

When a magnetic field is applied to an electrically conducting fluid, the magnetic force $[\mathbf{v} \times \mathbf{B}]$ resists the development of the turbulent eddies. This damping effect delays the laminar-turbulent transition to a higher Reynolds number, as shown in Fig. 12-38.

For MARS, the Hartmann number is 10^4 and the Reynolds number is 10^5 . Therefore, it is realistic (as well as conservative) to assume laminar flow through the reactor. The heat transfer is then dominated by conduction.

12.8.2 Magnetohydrodynamic Pressure Drop

It is well known that whenever an electrically conducting fluid flows across field lines, eddy currents will appear wherever $\text{curl } [\mathbf{v} \times \mathbf{B}]$ is non-zero. This produces a retarding force that causes extra pressure drops in the fluid. We have classified these pressure drops into two categories: Hartmann and end-of-loop effects.

The Hartmann pressure gradient arises in fully developed laminar flow with a uniform transverse magnetic field. For such a flow between parallel plates in a uniform magnetic field normal to the plates, the pressure gradient is given by²⁹

$$-\frac{dP}{dx} = \frac{\mu v}{a^2} \left(\frac{\bar{H}^2 \tanh \bar{H}}{\bar{H} - \tanh \bar{H}} + \frac{\bar{H}^2 C}{1 + C} \right), \quad (12-14)$$

where

C = wall conductance ratio $\sigma_w t_w / a \sigma$,
 a = channel half-width,
 σ_w = electrical conductivity of the wall material,
 t_w = wall thickness.

Fig. 12-38. Transition Reynolds numbers for flows of conducting fluids in circular pipes in parallel and transverse magnetic fields.

When the fluid is in a channel with a conducting wall and under large magnetic fields ($H \gg 1 + 1/C$), the last term of Eq. (12-14) dominates, giving

$$-\frac{dP_H}{dx} = \frac{\bar{v} B^2 \sigma_w t_w}{a(1+C)} \quad (12-15)$$

If the wall is strong enough to withstand the pressure and the corrosion, the value of C is usually on the order of 10^{-2} or larger; hence, Eq. (12-15) is an excellent approximation in the present range of Hartmann numbers ($H \approx 10^4$).

The end-of-loop effects are caused by gradients of $[\underline{v} \times \underline{B}]$ in the flow direction. The resulting pressure drops are given approximately by

$$\Delta P_E = K_{PIE} \frac{\sigma_b}{v_{av}} \Delta(v^2 B^2) \quad (12-16)$$

Here b is the mean half-width of the local cross section in the direction normal to $\underline{B}$ and v_{av} is the arithmetic mean of values for initial and final cross sections. The coefficient K_{PIE} has been presented by Hoffman and Carlson²⁸ for flows in tubes and ducts with varying $\bar{B}$; the results are summarized in Figs. 12-39 and 12-40. We will use the same plots here to estimate K_{PIE} for changes of velocity and channel width; in this estimation L_F is taken as the downstream length of the region of varying $v\bar{B}$.

The calculation of the MHD pressure drop is summarized in Table 12-22. The total pressure drop is 1.57 MPa. Because part of the pressure drop occurs before the coolant reaches the first wall, the maximum pressure on the first wall is 0.73 MPa. The pumping power of a coolant can be calculated by

$$P = \frac{1}{\epsilon} V \Delta P \quad ,$$

where

- P = the required pumping power,
- ϵ = the pump efficiency,
- V = the coolant volumetric flow rate,
- ΔP = the coolant pressure drop.

We have calculated the pumping power of the $Li_{17}Pb_{83}$ necessary to overcome the MHD pressure drop to be 20 MW by assuming a pump efficiency of 80%. The major parameters of MHD calculations are summarized in Table 12-23.

12.9 STRESS ANALYSIS

12.9.1 Blanket Stresses

The stresses in the blanket can be divided into three classes: thermal stresses, coolant pressure stresses, and mechanical stresses due to the supports and body forces. Thermal stresses are induced in structures that are subjected to temperature gradients and are constrained from deforming. The front row of tubes in the blanket experiences such stresses. The coolant pressure induces stresses in all the blanket components. Mechanical stresses are induced by the weight of the components and the materials in them, as well as by the localized forces at blanket support points.

In this section the beam and tube zones will be analyzed separately, since they are essentially independent of each other.

Fig. 12-39. Pressure drops due to entrance and exit eddy current in linear fringe magnetic fields that are separated by a long region of uniform magnetic field.

Fig. 12-40. Pressure drop due to entrance or exit eddy currents.

Table 12-22. Summary of MHD calculations.

	V (m/s)	a (m)	t_w (mm)	L (m)	B (T)	k	ΔP_E	ΔP_H (MPa)	$\Sigma \Delta P$
Feed pipe	1.34	0.18	1	3	4.7	0.05	0.16	0.16	0.32
Feed pipe turn to supply header	1.34	0.18	1	--	4.7	0.10	0.32	--	0.32
Supply header turn to blanket tube	0.17	0.05	2	--	4.7	0.10	0.01	--	0.01
Blanket tube	0.17	0.05	2	1.88	4.7	--	--	0.27	0.27
Blanket tube turn to return header	0.17	0.05	2	--	4.7	0.10	0.01	--	0.01
Return header turn to discharge pipe	1.34	0.18	1	--	4.7	0.10	0.32	--	0.32
Discharge pipe	1.34	0.18	1	8	4.7	0.05	0.16	0.16	0.32
Total									1.57

Table 12-23. MHD parameters.

Blanket power (MW)	2071
Blanket coolant flow rate (m^3/hr)	3.4×10^4
Blanket velocity (m/s)	0.17
Magnetic field (T)	4.7
MHD pressure drop (MPa)	1.57
Maximum blanket pressure (MPa)	0.73
Li-Pb pumping power, 80% efficiency (MW)	20

12.9.1.1 Beam Zone. Figure 12-41 is a plan view of a blanket module, which consists of two rows of tubes and a beam zone in the back. The beam zone is the blanket's structural component. The blanket is supported within the reflector by two support plates running the whole axial length of the blanket. These support plates are part of the beam zone and can be seen in Fig. 12-41.

We have modeled the beam zone as a circular ring loaded in the center at the top and the bottom and supported at the locations indicated in Fig. 12-41. This model is quite accurate because the manifolds actually provide the ring continuity and because the loads come from two sources--(1) the mass of the breeding material in the manifolds and tubes and (2) the distributed load in the ring due to the mass of the breeding material in the beam. Figure 12-42 is a diagram of the assumed model, where P is the force from the manifold and tubes (P is equal at the top and bottom), w is the distributed load in the beam, R is the reaction at the supports, and r is the average radius of curvature. The angle ϕ is measured from the south pole clockwise. Since the beams are attached to the manifolds at $\phi = 15^\circ$ and $\phi = 165^\circ$, the moments and reactions will be calculated between these angles at intervals of 15° .

Fig. 12-41. Plan view of a blanket module.

Fig. 12-42. Assumed model of the blanket beam structure.

Figures 12-43a and b are free-body diagrams for the beam spaces $0 < \phi < \pi/4$ and $\pi/4 < \phi < \pi$, respectively. In Fig. 12-43a, F is the vertical force from the distributed load w and d is the distance from F to the point of analysis. Thus,

$$F = wr\phi ,$$

$$d = r \sin \phi - r/\phi (1 - \cos \phi) ,$$

$$Fd = wr^2(\phi \sin \phi + \cos \phi - 1) .$$

Fig. 12-43. (a) Free-body diagram for the beam for $0 < \phi < \pi/4$;
(b) free-body diagram for the beam for $\pi/4 < \phi < \pi$.

The moment at the south pole is designated M_1 , and is assumed to be positive. The vertical reaction is $P/2$ and the horizontal reaction is N_1 . Summing the moments around the point of analysis yields the moment at that point equal to

$$M = M_1 + N_1 r (1 - \cos \phi) - O/2 r \sin \phi - w r^2 (\phi \sin \phi + \cos \phi - 1) \quad (12-17)$$

As ϕ goes beyond $\pi/4$ we move to the free-body diagram in Fig. 12-43b which includes the reaction R . The same summing of moments yields

$$M = M_1 + N_1 r (1 - \cos \phi) - O/2 r \sin \phi - w r^2 (\phi \sin \phi + \cos \phi - 1) \\ + R r (\sin \phi - 1/\sqrt{2}) \quad (12-18)$$

To obtain M_1 and N_1 we use the expression for the flexural energy

$$U = \int \frac{M^2 r}{2 EI} d\phi \quad ,$$

from which we obtain the boundary conditions

$$\frac{\partial U}{\partial M_1} = 0 \quad , \quad \text{therefore} \quad \int_M \frac{\partial M}{\partial M_1} d\phi = 0 \quad , \quad (12-19)$$

and

$$\frac{\partial U}{\partial N_1} = 0 \quad , \quad \text{therefore} \quad \int_M \frac{\partial M}{\partial N_1} \partial \phi = 0 \quad . \quad (12-20)$$

To solve for M_1 , we integrate Eq. (12-18). Obviously the limits of integration are $0 < \phi < \pi$ for the part in Eq. (12-17) and $\pi/4 < \phi < \pi$ for the added terms in Eq. (12-18). Setting the result equal to zero, we obtain

$$0 = [M_1 \pi + N_1 r \pi - Pr + Rr(1 + 1/\sqrt{2} - 3/4 \pi/\sqrt{2})] \quad . \quad (12-21)$$

To solve for N_1 we use the boundary condition in Eq. (12-20). Because $\partial M / \partial N_1 = r(1 - \cos \phi)$, we get

$$\int_M \frac{\partial M}{\partial N_1} \partial \phi = \int_M d\phi + \int_M (-\cos \phi) d\phi = 0 \quad .$$

Having set $\int M d\phi = 0$ in Eq. (12-21), we integrate the second term $\int M(-\cos \phi) d\phi = 0$

$$N_1 = \frac{R}{2\pi} - \frac{wr}{2}$$

or

$$N_1 = \frac{P}{2\pi} \quad .$$

(12-22)

Substituting $P/2\pi$ for N_1 in Eq. (12-21) gives the value for M_1

$$M_1 = 0.146 Pr - 0.041 wr^2 \quad . \quad (12-23)$$

These values for N_1 and M_1 can now be used in Eqs. (12-17) and (12-18) to calculate the moments at 15 deg intervals from $\phi = 15^\circ$ to $\phi = 165^\circ$. Furthermore, the summation of forces in each free-body diagram gives the vertical and horizontal reactions at the points of analyses. These forces are resolved into normal forces (tangent to the radius of curvature) and shear forces (perpendicular to the radius of curvature). The adopted convention is the following:

- Normal forces: compressive forces are negative (-), tensile forces are positive (+);
- Shear forces: directed away from center, negative (-), directed toward center, positive (+).

Table 12-24 gives the loads on a blanket module due to the weights of the structure and the breeding material. From these loads, the values of the forces P and w and the reaction R can be obtained as follows:

$$\frac{P}{2} = \frac{2242 + 5140}{2} = 3691 \text{ kg} = 36,209 \text{ N} \quad ,$$

$$w = \frac{7415}{(r)\frac{5\pi}{6}} = 3163 \text{ kg/m} = 31,029 \text{ N/m} \quad ,$$

$$R = 7415 + 2242 + 5140 = 14,797 \text{ kg} = 145,160 \text{ N} \quad .$$

Table 12-24. Loads on a blanket module.

Component	Structure (kg)	Li ₁₇ Pb ₈₃ (kg)	Total (kg)
Beam (one side)	755	6660	7415
Manifold (one)	13	2109	2242
Tubes (one side)	378	4762	5140

The average radius of curvature of the beam zone is 0.895 m. These values are then used in Eqs. (12-17, -18, -22, and -23), to obtain the bending moments. Table 12-25 gives the normal forces F_n , the shear forces F_s , and the bending moments as a function of the angle ϕ .

Figure 12-44 shows a cross section of the beam zone at the edge. The computed moment of inertia I about an axis perpendicular to the beam webs is $2.37 \times 10^4 \text{ cm}^4$ and the cross sectional area is 413 cm^2 .

The maximum normal stress occurs at $\phi = 75^\circ$ and is equal to

$$\sigma_{N\max} = - \frac{74,152 \text{ N}}{413 \times 10^{-4} \text{ m}^2} = -1.79 \text{ MPa} .$$

The maximum shear stress occurs at the support, at $\phi = 45^\circ$ and is equal to

$$\sigma_{S\max} = \frac{74,894 \text{ N}}{413 \times 10^{-4} \text{ m}^2} = 1.81 \text{ MPa} .$$

The maximum bending stress occurs at the support, as might be expected and is equal to

$$\sigma_{B\max} = \frac{17.955 \text{ Nm} (9.5/100)\text{m}}{2.37 \times 10^{-4} \text{ m}^4} = \pm 7.2 \text{ MPa} .$$

The stress is compressive on the outer beam surface and tensile on the inner surface. As it turns out, these stresses are small compared with the pressure stresses acting on the flat elements of the beam flanges.

The maximum operating pressure in the blanket is 1.35 MPa, although the system pressure is higher than that. Since the blanket is effectively at the end of the line, most of the pressure drop occurs in the steam generator and the pipes. The MID pressure drops in the blanket are discussed in Sec. 12.8.

From Fig. 12-44 it is obvious that the beam flanges can be modeled as flat plates built in on opposite sides having an infinitely large aspect ratio of length to width. The bending moments in the center and at the edge are given by

$$M_c = \beta q b^2 \text{ and } M_e = \gamma q b^2 ,$$

Table 12-25. Moments and forces on beam.

Angle θ (degrees)	Normal force, F_n^a (N)	Shear force, F_s^b (N)	Moment, M^c (Nm)
15	+20,494	-31,995	-436
30	+31,713	-31,901	-9,555
45 ^d	+44,030	-27,749	-17,955
45 ^e	-58,610	+74,894	-17,955
60	-69,697	+53,535	-3,978
75	-74,152	+31,789	+5,418
105	-66,060	-5,780	+11,042
120	-56,013	-19,045	+8,711
135	-44,945	-28,661	+4,156
150	-32,464	-33,199	-1,573
165	-20,494	-31,995	-7,560

^a F_n (-) tensile;
 F_n (+) compressive.

^b F_s (-) away from center;
 F_s (+) toward center.

^c M (-) produces tension on inner surface;
 M (+) produces compression on inner surface.

^dJust before support.

^eJust after support.

Fig. 12-44. Edge of blanket module.

where β and γ are coefficients for the existing boundary conditions, q is the pressure, and b is the width of the plate (distance between webs in the beam structure). The coefficients are $\beta = 0.0417$ and $\gamma = -0.0833$.³⁰ The moments are

$$M_c = 681 \frac{\text{Nm}}{\text{m}}$$

and

$$M_e = 1360 \frac{\text{Nm}}{\text{m}}.$$

The stresses in the flange are obtained from the equation $6M/t^2$, where t is the plate thickness (which is 0.7 cm in the center and 1.0 cm at the edge). The stresses are

$$\sigma_{\text{center}} = 83.4 \text{ MPa},$$

$$\sigma_{\text{edge}} = 81.3 \text{ MPa}.$$

The stress in the ellipsoidal end cap is ~ 70 MPa for a radius of curvature of 35 cm and the tensile stress in the webs is very low, < 0.4 MPa.

It is obvious that the pressure stresses in the flanges dominate, being on the order of 85 MPa. The bending stresses at the support, of the order $+7.2$ MPa, are perpendicular to the pressure stresses and therefore do not add algebraically. The maximum shear stress is < 2 MPa and is insignificant. It appears that at no point does a combination of these stresses exceed 90 MPa.

12.9.1.2 Tube Zone. The tubes can be also modeled as a continuous ring supported in the middle and loaded uniformly by their own mass and the mass of the breeding material. Figure 12-45a gives a diagram of the model and Fig. 12-45b is a free-body diagram. For a unit mass of ω kg/m, the force $F = \omega r \phi$ and as in the case of the beam, the moment about the point of analysis, $Fd = \omega r^2 (\phi \sin \phi + \cos \phi - 1)$. The vertical reactions are $\omega r \pi / 2$, and are equal at the top and bottom.

Fig. 12-45. (a) Assumed model of the front tube; (b) free-body diagram for the tube for $0 < \phi < \pi$.

Using the same method as in the beam, the moment equation is

$$M = -M_1 - N_1 r (1 - \cos \phi) \frac{\omega r^2 \pi}{2} (\sin \phi) - \omega r^2 (\phi \sin \phi + \cos \phi - 1). \quad (12-24)$$

Solving for M_1 and N_1 , as in the case of the beam, we get

$$M_1 = 1.5 \omega r^2 \quad (12-25)$$

and

$$N_1 = \frac{\omega r}{2} \quad (12-26)$$

The tube is supported at $\phi = 10^\circ$ and $\phi = 170^\circ$ at the bottom and top, respectively. For the front tube the radius of curvature r is 0.65 m and the unit loading ω is 839 N/m. This makes $M_1 = -532$ Nm and $N_1 = -273$ N. Table 12-26 gives the moments, and the normal and shear forces on the front tube, using the same sign convention as for the beam.

Table 12-26. Moments and forces on front tube.

Angle, ϕ (degrees)	Normal force, F_N^a (N)	Shear force, F_S^b (N)	Moment ^c (Nm)
10	+402	+705	-440
30	+523	+360	-322
45	+497	+111	-284
60	+384	-93	-283
75	+209	-227	-311
90	0	-273	-334
105	-209	-227	-398
120	-384	-93	-426
135	-497	+111	-427
150	-523	+360	-387
170	-402	+705	-266

^a F_N (-) tensile;
 F_N (+) compressive.

^b F_S (-) away from center;
 F_S (+) toward center.

^cM (-) produces tension in inner surface;
M (+) produces compression in inner surface.

The maximum bending moment is at the connection point at $\phi = 10^\circ$ and is equal to -440 Nm. At the 8-cm-outside-diameter connection the bending stress is +58 MPa. The stresses due to the normal and shear forces are very small, <2 MPa.

Figure 12-46 gives the temperature gradient across the front tube, which is 545°C on the front and 476°C in the back. Thermal stress is a totally

constrained beam is $\pm 1/2 \alpha E \Delta T$, where α is the coefficient of expansion, E the modulus of elasticity, and ΔT is the temperature difference. For a beam that is constrained in bending but allowed to expand, the stress is reduced to 60% of this value. For HT-9 at 550°C, α is 12.4×10^{-6} per °C, and E is 1.5×10^5 MPa, giving a thermal stress of ± 38.5 MPa.

Fig. 12-46. Temperature gradient in the front tube at exit.

The hoop stress resulting from the coolant pressure in the tubes is equal to 29.7 MPa for the 10-cm-diameter tube and 35 MPa for the 8-cm-diameter transition. It is interesting to note that the thermal stresses are induced by a positive moment, namely the inner surface is in compression and the outer surface in tension. The load moments, however, are negative and produce the opposite effect. For the front tube, the resulting bending stresses during reactor operation are

- Upper transition tube: $\sigma_{\text{bend}} = -35 + 38.5 = +3.5$ MPa;
- Lower transition tube: $\sigma_{\text{bend}} = -58 + 38.5 = -19.5$ MPa.

Hoop stresses due to the coolant pressure are perpendicular to the bending stresses, but longitudinal pressure stresses are parallel to them. The longitudinal stress, which is one-half of the hoop stress, must therefore be added to the bending stresses. Thus, the maximum stress on the front row of transition tubes during reactor operation is

$$\sigma_{\text{max}} = -19.5 - \frac{35}{2} = -37 \text{ MPa}.$$

It is tensile and is on the inner surface of the tube. Immediately after the plasma is turned off, namely when the thermal stress is no longer there, the maximum stress is actually higher:

$$\sigma_{\max} = -58 - \frac{35}{2} = -75.5 \text{ MPa} .$$

This stress is also tensile on the inner surface.

The second row of tubes does not have any appreciable thermal gradient and thus, the maximum stress in these tubes will be on the order of -75 MPa during and after plasma shutdown. Table 12-27 summarizes the stress in the tubes.

Table 12-27. Summary of blanket stresses.

Maximum design stress intensity for HT-9 at 550°C MPa	110
Beam stresses	
Max bending stress at support (MPa)	+7.2
Max longitudinal stress in beam flange (MPa)	83.4
Max normal stress (MPa)	-1.79
Max shear stress (MPa)	1.81
Tube Stresses	
With plasma on	
first row, max stress (MPa)	-37
second row, max stress (MPa)	-75.5
With plasma off	
first row, max stress (MPa)	-75.5
second row, max stress (MPa)	-75.5

12.9.2 Stresses in the Reflector

The reflector is fabricated from Fe-1422 (Nonmagne-30), a steel developed in Japan specifically for JT-60. Table 12-28 and Fig. 12-47 give the mechanical properties of Fe-1422 as supplied by the manufacturer. At the maximum operating temperature of 340°C, the yield strength is 246 MPa. We assume a maximum design stress intensity of 150 MPa, or ~60% of the yield strength.

Table 12-28. Tensile properties of Fe-1422 at low and high temperatures.¹⁷

Test temperature (°C)	Yield kgf/mm ² (MPa)	Tensile kgf/mm ² (MPa)	Elongation (%)	Reduction of area (%)
-130	62 (613)	83.3 (817)	8	10
-100	46.3 (454)	85.2 (836)	14	15
-50	43.6 (428)	94.0 (922)	30	35
20	35.5 (348)	84.5 (829)	60	54
100	32.6 (320)	79.5 (780)	73	59
200	28.6 (280)	73.0 (716)	70	68
300	26.1 (256)	63.4 (622)	62	76
400	23.5 (230)	56.7 (556)	60	78
500	17.6 (173)	46.2 (453)	50	76
600	13.5 (132)	35.8 (351)	45	72

Figure 12-37 shows a cross section of the reflector and Fig. 12-35 gives the nuclear heating in the blanket and reflector. The reflector consists of several concentric cylinders as shown in Fig. 12-37. Only the innermost and outermost cylinders are continuous circumferentially. The other so-called filler cylinders are segmented such that they are only continuous in the longitudinal direction. The material in these filler cylinders will be used to carry the longitudinal forces in the reflector.

The maximum structural temperature in the reflector is 340°C, with the water inlet at 280°C and outlet at 320°C. The interface temperature is 326°C for which the saturation pressure is 12.3 MPa. We are using a pressure of 13.5 MPa (1960 psi) for the reflector design.

A brief look at the thermal stresses in the inner and outer cylinders makes it obvious that a wall thick enough to withstand the hoop stress will have thermal stresses that are entirely too high. This will be demonstrated in the next several paragraphs.

Thermal circumferential (hoop) stress in a hollow cylinder with a temperature gradient in the wall can be expressed as

$$\sigma_{\theta th} = \frac{E\alpha}{(1-\nu)} \left[\frac{1}{(b^2-a^2)} \left(1 + \frac{a^2}{r^2} \right) \int_a^b T r dr + \frac{1}{r^2} \int_a^r T r dr - T_i \right], \quad (12-27)$$

Fig. 12-47. Tensile properties of Fe-1422 as a function of temperature.

where E is the modulus of elasticity, α the coefficient of expansion, ν the Poisson's ratio, a is the inner radius, b is the outer radius, and T_i is the initial temperature. For $r = a$, namely at the inner surface, the expression becomes

$$\sigma_{\theta ch} = \frac{E\alpha}{(1-\nu)} \left[\frac{2}{(b^2 - a^2)} \int_a^b T r dr - T_i \right] . \quad (12-28)$$

For the first 10 cm of the inner cylinder, the nuclear heating can be approximated as

$$q'''(r) = C_0 (r - a) + C_1 , \quad (12-29)$$

for which the coefficients $C_0 = 0.258 \text{ W/cm}^4$ and $C_1 = 2.58 \text{ W/cm}^3$.

By integrating twice and dividing by the thermal conductivity k , we obtain the temperature as a function of radius, such that

$$T(r) = -\frac{C_0 (r-a)^3}{6k} - \frac{C_1 (r-a)^2}{2k} + T_i, \quad (12-30)$$

where in this case, T_i is the initial temperature at the inner surface, which is assumed adiabatic, and cooling occurs at the rear surface of the cylinder. The temperature difference is the only parameter determining the thermal stress and is independent of the temperature of the coolant. By integrating $T(r)$ for substitution in Eq. (12-28), we obtain

$$\begin{aligned} \int_a^b T r dr &= \int_a^b [T(r-a) + aT_i] dr \\ &= -\frac{C_0}{6k} \left[\frac{(b-a)^5}{5} + \frac{a(b-a)^4}{4} \right] - \frac{C_1}{2k} \left[\frac{(b-a)^4}{4} + \frac{a(b-a)^3}{3} \right] \\ &\quad + T_i \frac{(b^2 - a^2)}{2}. \end{aligned}$$

Combining terms and substituting in Eq. (12-28), we obtain the expression for the thermal hoop stress:

$$\begin{aligned} \sigma_{\theta th} &= -\frac{2 E \alpha}{k (1-\nu) (b+a)} \left[\frac{C_0}{30} (b-a)^4 + \left(\frac{C_1}{8} + \frac{C_0 a}{24} \right) (b-a)^3 \right. \\ &\quad \left. + \frac{C_1 a}{6} (b-a)^2 \right]. \end{aligned} \quad (12-31)$$

The properties of Fe-1422 at 340°C used in Eq. (12-31) are $\nu = 0.289$; $k = 0.18 \text{ W/(cm/}^\circ\text{C)}$; $\alpha = 17.3 \times 10^{-6}/^\circ\text{C}$; and $E = 1.73 \times 10^5 \text{ MPa}$. Thermal stresses are calculated for a wall thickness range from 0.5 cm to 10 cm and are listed in Table 12-29.

The pressure hoop stress in a thin walled pressure vessel where $t \leq r_{avg}/10$ is

$$\sigma_{\theta p} = \frac{P(b+a)}{2(b-a)},$$

where P is the pressure. This expression can be used for internal and external pressure. Hoop stresses for the indicated thickness range, with a pressure P of 13.5 MPa and an inner radius of 101 cm, are calculated and listed in Table 12-29. Also listed is the total stress and the temperature difference ΔT across the cylinder for the first 5 cm, after which ΔT gets too high.

Figure 12-48 shows the stresses graphically where the design stress intensity of 150 MPa is indicated with a straight line. Clearly, a simple cylindrical design with cooling in the back does not even come close to satisfying this criterion. An obvious solution is to have a thin wall across

Table 12-29. Thermal, pressure, and total stress for the reflector as a function of wall thickness.

Wall thickness (cm)	$\sigma_{\theta th}$ (MPa)	$\sigma_{\theta p}$ (MPa)	$\sigma_{\theta total}$ (MPa)	ΔT , across cylinder ($^{\circ}C$)
0.5	2.98	2734	2737	1.8
1.0	10.33	1371	1381	7.4
2.0	42.39	689	731	30.5
3.0	97.95	461	559	70.9
4.0	178.7	347	526	129.7
5.0	286.4	280	566	209
6.0	424.6	234	659	--
8.0	799	177	976	--
10.0	1319	143	1462	--

Fig. 12-48. Thermal, pressure, and total stresses of a solid inner cylinder for the reflector condition as a function of wall thickness.

which the thermal stresses occur and a thick wall to react pressure stresses. The solution that we adopted (shown in Fig 12-37) does just that.

The figure shows that the grooves in the inner cylinder allow the coolant to come within 1 cm of the inner surface. Furthermore, cooling of the fins occurs on three sides and thus the dT/dr in them will be close to zero with very little thermal stress in the hoop direction. Some thermal stress will exist in the longitudinal direction, but will be small and will not add appreciably to the hoop stress.

When we consider the area lost to the grooves, the surface area that reacts the pressure load is equal to $0.225 \text{ m}^2/\text{m}$, using the dimensions indicated in Fig. 12-37. The hoop stress is then equal to the total force divided by this area and is given by

$$\sigma_{\theta p} = \frac{(13.5 \text{ MPa})(1.01 + 0.135)(2)}{0.225} = 137.4 \text{ MPa}.$$

Using $\sigma_{\theta th}$ for a 1.0-cm wall (from Table 12-29), the total hoop stress in the inner cylinder is

$$\sigma_{\theta total} = \sigma_{\theta p} + \sigma_{\theta th} = 137.4 + 10.3 = 147.7 \text{ MPa}.$$

The outer cylinder wall has the same problem but is not as severe. Here we assume that the outer surface is adiabatic, a worst-case assumption, since the outer surface can radiate to the cooler shield. The temperature as a function of radius is

$$T(r) = -\frac{q'''}{2k}(r-a)^2 + \frac{q'''}{k}(b-a)(r-a) + T_i, \quad (12-32)$$

where q''' is again the nuclear heating. The thermal hoop stress in the outer cylinder is

$$\sigma_{\theta th} = \frac{Eqq'''}{12k(1-\nu)(b+a)}(5b+3a)(b-a)^2.$$

Using an upper bound for $q''' = 0.17 \text{ W/cm}^3$, the thermal stress for a 3-cm-thick wall is 11.93 MPa. The pressure stress for the outer cylinder dimensions shown in Fig. 12-37 is computed equal to 138 MPa. Thus, the total stress for the outer cylinder is 150 MPa. This value is an upper bound since we used a high nuclear heating and assumed an adiabatic outer surface.

The longitudinal force on the reflector is

$$F_{long.} = 13.5 \text{ MPa} (\pi)(1.41^2 - 1.02^2) = 40.2 \text{ MN}.$$

The cross sectional area needed to carry this force at 150 MPa is only 0.27 m^2 . The area of the intermediate, longitudinally segmented cylinders is almost five times greater than the required area. Thus, we conclude that the longitudinal force can be adequately reacted. Enough space is available for bolting the end plates to the cylinders.

Finally, we might inquire about the stresses at the notch provided at the top and bottom of the reflector to allow axial translation of blanket modules. These notches penetrate the inner cylinder to a depth of 5.5 cm. Note, however, that the penetrations at the notches have sleeves that tie the inner and outer cylinders together. These sleeves more than compensate for the strength lost when the material is removed. A careful analysis of this region is beyond the scope of this study.

12.10 LITHIUM-LEAD CORROSION

The areas of MARS most likely to be affected by corrosion are the following:

1. Thinning of the blanket structure or heat exchanger, and therefore, exceeding the allowable stress in the tube walls.
2. Transport of radioactive corrosion products around the primary coolant loop making maintenance difficult.
3. Deposition of corrosion products in valves, or flow constrictions such that blockage may eventually occur.

We will present an analysis of each of the above areas at the end of this section.

Experimental data should be the basis for determining corrosion effects. Unfortunately there are no experiments that duplicate the MARS conditions with respect to temperature, flow velocity, flow profile, magnetic field effects, or purity of the coolant. Lacking such data, we were forced to use information that approximates the MARS conditions as close as possible. An experiment conducted at ANL³¹ measured the corrosion rate of HT-9 in contact with Li₁₇Pb₈₃ but because of the loop design the velocity of the coolant was very low, approximately 3 cm/s³². This compares with velocities of 17 cm/s in the MARS blanket, 70 cm/s in the heat exchanger, and 150 cm/s in the distribution pipes.

The actual data from the ANL experiment is given below and the corrosion rate is displayed in Fig. 12-49.

Temperature (°C)	Steady-state corrosion rate (mg/m ² ·h)
427	4.76
454	8.75

The corrosion rate above and below the 427 to 454°C temperature range is obtained by straight line extrapolation through the only HT-9/flowing Li₁₇Pb₈₃ data points available to us at this time. The corrosion rate $\dot{C}$ at temperature T can be calculated by the following expression.

$$\ln \dot{C} = 11.8 - \frac{4371.2}{T} \quad (\text{mg/m}^2 \cdot \text{h}) \quad (12-33)$$

Because the velocity in the MARS system is higher than the velocity at which the data were obtained, some scaling to higher velocities must be made. We have chosen to use the ANL velocity scaling based on Na/316 SS data, but would caution that the HT-9/Li₁₇Pb₈₃ system may act differently. The expression used to scale the corrosion rate from 0.03 m/s to a velocity v is

$$F(\text{rel}) = 0.973 + 0.885 v \quad (12-34)$$

where v is in m/s.

Fig. 12-49. Extrapolation of ANL corrosion data for the HT-9/ $\text{Li}_{17}\text{Pb}_{83}$ system,

Table 12-30 lists the steady-state corrosion rates anticipated for the various MARS components as calculated from Eqs. (12-33) and (12-34). The minimum temperature in MARS is 350°C and the maximum interface temperature is 545°C at the outlet to the exit header in the blanket. The maximum dissolution rate at the highest temperature is 47 mg/m²·h or approximately 53 microns per year. At the low temperature (350°C) the corrosion rate is less than 1 micron per year.

Table 12-30. Calculated steady-state corrosion rates for MARS components.

Temperature (°C)	mg/m ² ·h			
	ANL experiment (0.03 m/s)	Blanket/tubes (0.17 m/s)	Heat exchangers (0.7 m/s)	Outlet pipes (1.4 m/s)
350	0.50	0.56	0.80	1.11
362.5	0.77	0.87	1.23	1.71
375	1.15	1.29	1.83	2.55
387.5	1.68	1.89	2.68	3.72
400	2.39	2.69	3.81	5.30
412.5	3.33	3.74	5.31	7.38
425	4.55	5.11	7.26	10.08
427	4.76	5.35	7.59	10.55
437.5	6.10	6.86	9.73	13.52
450	8.05	9.05	12.84	17.84
457	8.75	9.84	13.96	19.39
462.5	10.47	11.77	16.70	23.20
475	13.43	15.10	21.42	29.76
487.5	17.00	19.11	27.12	37.67
500	21.28	23.92	33.94	47.16
512.5	26.33	29.59	42.00	58.35
525	32.26	36.26	51.45	71.50
537.5	39.16	44.02	62.46	86.78
550	47.10	52.94	75.12	104.37

The next problem to be addressed is determining the amount of surface area that exists at a given temperature. There are over 50,000 m² of HT-9/Li₁₇Pb₈₃ surface area in MARS to consider and the location of these areas is given in Table 12-31. Because the heat exchanger represents over 75% of the surface area, we might expect it to be dominant in total corrosion product generation. A more detailed analysis of the total surface area at a given temperature is listed in Table 12-32 and is shown in Fig. 12-50. These data indicate that only 85 m² are at the highest temperature range of 525-550°C. There are also about 5700 m² in the 475-500°C range, which we find very important from the total corrosion product standpoint. The total corrosion product generation is tabulated in Table 12-33 for each system and is displayed in Fig. 12-50. There are five important features of this information.

1. A large fraction (~40%) of corrosion products are generated in the outlet pipes at 500°C.
2. While the heat exchanger provides over 75% of the corrosion surface, it generates less than 50% of corrosion products.
3. The amount of corrosion product generated in the high temperature (500-550°C) region of the blankets is negligible (<2%) compared with the rest of the system.
4. The total amount of corrosion product generated per FPY is approximately 3200 kg.
5. Only 390 kg/y of corrosion product is radioactive.

With the above information, we can make the following observations on the effects of this corrosion (see Secs. 12.10.1 to 12.10.7).

Table 12-31. The location and areas of the HT-9/Li₁₇Pb₈₃ contact surfaces in MARS at specified temperatures.

Location	Area (m ²)
Blanket tubes	
First row (350-545°C)	1,355
Second row (350-500°C)	1,449
Beam section (350-500°C)	2,898
Manifolds (350-500°C)	240
Inlet pipes (350°C)	2,588
Outlet pipes (500°C)	2,960
Heat exchangers (350-500°C)	<u>39,000</u>
Total	50,490

Table 12-32. Total area (m^2) in MARS Li_7Pb_{83} system.

Average or temperature range (°C)	Blanket					Inlet or outlet pipe	Heat exchanger
	Total	1st Tubes	2nd Tubes	Beams	Headers		
350	2,708				120	2,588	
350-375	23,085	197	241	483			22,164
375-400	7,231	198	242	483			6,308
400-425	4,757	197	241	483			3,836
425-450	3,715	198	242	483			2,792
450-475	3,117	197	241	483			2,195
475-500	2,628	198	242	483			1,705
500	3,080				120	2,960	
500-525	85	85					
525-550	85	85					
Total	50,490	1,355	1,449	2,898	240	5,548	39,000

Fig. 12-50. The HT-9/Li₁₇Pb₈₃ contact surfaces in MARS at specified temperatures.

Table 12-33. Total corrosion rate in MARS $\text{Li}_{17}\text{Pb}_{83}$ system.

Temperature (°C)	kg/FPY					
	Blanket			Inlet or outlet pipe	Heat exchanger	Total
	1st tubes	2nd tubes	Beams Headers			
350				0.5	25.2	25.7
350-375	1.3	1.6	3.2			244.1
375-400	2.9	3.5	7.1			161.6
400-425	5.7	7.0	14.0			205.1
425-450	10.6	13.0	26.0			287.6
450-475	18.1	22.1	44.4			405.7
475-500	29.5	36.1	72.3			543.0
500				22.4	1223	1245.4
500-525	19.6					19.6
525-550	29.2					29.2
Total	116.9	83.3	167	22.9	1248.2	3167

12.10.1 Total Volume

The 3167 kg corresponds to a cube 74 cm on an edge or 0.41 m^3 per year, but the radioactive portion occupies only 0.05 m^3 (a cube of 37 cm on an edge).

12.10.2 Comparison With the Solubility Limit

The solubility of Fe in Pb at 350°C ranges from 0.01 to 20 appm.³³⁻³⁴ From this we predict that the 14,607 tonnes of $\text{Li}_{17}\text{Pb}_{83}$ in MARS can dissolve from 0.1 to 290 kg of Fe. Solubility data for Ni at 350°C indicates that approximately 1000 appm of Ni can be dissolved in Pb.³⁵ These data indicate that over 14,000 kg of Ni can dissolve before reaching the saturation level at the lowest temperature in the system. By comparing these numbers with those from the corrosion product generation we find the following:

	Corrosion product generated per year (kg)	Solubility limit (at 350°C •kg)
Fe	2360	290 (max)
Ni	16	14,600

We will never saturate the coolant with Ni over the entire lifetime of the reactor, but we will exceed the solubility limit of Fe within a relatively short time and, therefore, methods to remove that corrosion product need to be examined. (See Sec. 12.10.4 on cold trapping.)

12.10.3 Thinning Rate

Using the corrosion rates determined from the ANL data,³¹ we find that, except for the heat exchanger, less than 10% of the tubes and/or structural wall thickness will be eroded during the lifetime of any component. The 19% erosion of the superheater outer tube wall is acceptable because it is not a structural component. A comparison of the important numbers is given in Table 12-34.

Table 12-34. Effect of corrosion on thinning of the primary structures.

System	Initial thickness (mm)	Expected life (FPY)	Maximum thinning rate (mm/FPY)	Maximum wall erosion over lifetime (%)
Front tubes	2.3	4	0.053	9.2
Back tubes	2.3	4	0.024	4.1
Beams	4	4	0.024	2.4
Headers	2.3	4	0.024	4.1
Outlet pipe	30	30	0.053	5.3
Heat exchanger	2	10 ^a	0.038	19

^aSuperheater section only.

12.10.4 Design of the Cold Trap

To minimize any problems caused by the corrosion product deposition, we decided to install a cold trap in MARS. The object of the cold trap is to keep the primary coolant from becoming supersaturated in regions where flow blockage might be difficult to repair.

A schematic of the corrosion product cleanup system is shown in Fig. 12-51. For the ideal case, the concentration of the corrosion products in the main loop remains between C_s and $C_s + \Delta C$. The value of C_s is the saturation concentration at the lowest temperature in the cold trap and ΔC is the concentration increase per pass. Because the cleanup system must remove ΔC for every pass, the side stream flow rate to the cleanup system is

Fig. 12-51. Cold trap of corrosion product.

$$M_s = M_n \frac{\Delta C}{\eta(C_s - C_{ct})} \quad , \quad (12-35)$$

where

ΔC is the corrosion rate divided by the main flow rate,
 M_s is cleanup flow rate,
 M_n is the main flow rate,
 C_{ct} is the saturation concentration at the cold trap temperature,
 η is the cold trap efficiency.

The operating parameters for the cleanup system are listed in Table 12-35.

A regenerator heat exchanger and cooler reduces the side stream temperature to 310°C. Because the liquid is then supersaturated (for example with iron) the corrosion product can be deposited either on the surface of the heat exchanger tubes, or on a "sacrificial" surface associated with something like steel wool.³² In MARS, we use the latter approach, i.e., roughly 15 tonnes of 0.1 mm² cross section wire to present some 19,000 m² of cold (310°C) surface area to the Li₁₇Pb₈₃ coolant. The flow fraction required to accomplish this reduction is approximately 28% if the efficiency of the cold trap is 75%. After passing through the steel wool, the liquid is then heated back up to approximately 350°C where it is reunited with the main flow stream. The use of a recuperative heat exchanger in this way only loses about 37 MW (41% of the total power).

The above calculations show that with the above method, we should be able to remove the 0.3 m³ of corrosion product generated per calendar year of operation. Periodic replacement of the steel wool and disposal of that material should be a relatively straight-forward procedure.

Table 12-35. Parameters for the MARS cold trap

Parameter	Value
M_n (kg/s)	8.8×10^4
C_s (350°C) ^a (g Fe/g Pb)	6×10^{-9}
Corrosion rate (g/s)	0.075
Cold trap temperature (°C)	310
C_{st} (310°C) ^a (g Fe/g Pb)	1.3×10^{-9}
M_s (kg/s)	2.15×10^4
Assumed cold trap efficiency (%)	75
Fraction total flow to cold trap (%)	27.5
Economizer area (m ²)	144
Steel wool required (tonnes at 0.1 mm ² cross section)	15

^aThis assumes that data from Refs. 33 and 34 are correct and the addition of a third element, in solution, i.e., Mn, does not change the solubility limit.

12.10.5 Radioactivity Buildup in the Steam Generator

A recent calculation³⁶ of the dose rate around the MARS steam generator is shown in Fig. 12-52.* The dose rate (calculated after the Li_7Pb_{83} coolant is removed) can be substantial but not prohibitive. Working in 100 mrem/hour radiation fields is not uncommon in today's fission plants and with proper monitoring should present no particular problem if maintenance is infrequent (once a year).

*Note that the assumptions used in Ref. 36 refer to an earlier version of MARS but the general conclusions are still valid.

Fig. 12-52. MARS steam generator dose rates.

12.10.6 Potential Plugging of the MARS Coolant Loop

With the generation of approximately 0.3 m^3 of corrosion product per calendar year (at 75% availability), there might be a potential problem of plugging in valves, flow headers or heat exchangers. At the present time, the smallest flow path ($\sim 1.5 \text{ cm}$) for the $\text{Li}_{17}\text{Pb}_{83}$ coolant occurs in the heat exchangers. Assuming that the cold trap does not work as anticipated, we could ask how long will it take before the flow passages in the heat exchanger become plugged?

If we assume that a 50% flow blockage is the tolerable limit, then we would like to keep the deposition rate to less than $1.5 \text{ cm} \times 1/2 \times 1/2$ per 30 FPYs or 125 microns per FPY. This means that the area of deposition should be over 2400 m^2 . Referring to Table 12-32, we see that there is approximately $23,000 \text{ m}^2$ of surface area in the heat exchanger between 350 and 375°C . If the area is linearly apportioned in this temperature range, then the deposition will reach the limit (if the cold trap does not work) in the last 2 to 3°C temperature drop, but only if all the corrosion product precipitates in that temperature range. It is highly unlikely that this could occur, therefore, plugging is not considered a major problem in the heat exchanger.

12.10.7 Use of Inhibitors

At the present time, we have not proposed the use of corrosion inhibitors in the MARS system. However, we have been known for some time that the addition of Ti or Zr to Pb will inhibit corrosion in steels by forming adhering TiC and ZrC films on the surface. For example, when a 2-1/4 Cr - 1 Mo alloy was exposed to lead for 300 hours, it suffered severe corrosion.³⁷ However, the addition of Ti to the Pb completely eliminated the corrosion under the same conditions. We are also exploring the suggestion that Al or Mg may also act as inhibitors; these ideas are scheduled for testing at ORNL in 1984-85.³⁸

12.10.8 Conclusions

Unless some unforeseen data appears or some design detail that we have overlooked is discovered, there should be no problem with flow blockage in the heat exchanger of MARS. This, coupled with the low thinning rates (<10% of initial thicknesses for structural components), and reasonably low radiation levels in heat exchangers without a cold trap (hundreds of mrem/hr) leads us to the conclusion that operating the MARS reactor blanket at a peak (hot spot) temperature of 545°C and a bulk outlet temperature of 500°C is a reasonable procedure.

12.11 STRUCTURAL LIFETIME ASSESSMENT

The major factors that determine the lifetime of the HT-9 tubes in the MARS blanket are the stresses induced by the void swelling and the strain introduced by irradiation creep. A detailed analysis of the preliminary blanket design³⁹ revealed that even after 4.3 FPY, the stresses induced in the tubes are below the allowable values.⁴⁰ However, that analysis did not include the incubation period for void swelling to occur (approximately 2 FPYs), nor did it include design modifications to reduce the stresses in the tubes once swelling or creep-induced strains begin. Including these features would push the lifetime to 4 or 5 FPY, thus in this study we will use a value of 5 calendar years (3.6 FPY) until a more refined analysis can be performed.

12.12 MAGNETIC INTERACTIONS OF HT-9

12.12.1 Introduction

The use of the ferromagnetic alloy HT-9 in the MARS blanket was mainly determined because of its excellent resistance to neutron irradiation damage when compared with the other candidate alloys for fusion reactors. The presence of a magnetic material in the blanket and its interaction with the MARS magnetic field can have two effects: First, there will be an induced magnetic field from the blanket that will be superimposed on the magnetic field of MARS. Second, the ferromagnetic components will experience magnetic loading, i.e. forces and moments, in the presence of the magnetic field of MARS. The different aspects of both effects are described in the following subsection. We used the computer program GFUN3D in these calculations.⁴¹

12.12.2 Magnetic Field Perturbations

The induced field from the blanket in the plasma region is of primary concern. This field, if high enough, could affect the stability of the plasma. Reference 42 demonstrates that this field is an end effect that depends on the length of the blanket.

The ferromagnetic regions in the MARS blanket could be classified as the upper and lower manifolds: the tube section, which starts at radius 0.6 m and has a radial thickness of 0.192 m, and the beam section, which starts at radius 0.8 m and has a thickness of 0.19 m, respectively. The volumetric fraction of HT-9 is 7.1% in the tube section and 15% in the beam section.

We neglected the blanket manifolds in modeling the blanket because the volume concentration of HT-9 in the manifolds is very small, and because the field they produce will be a dipole-type field, which decays very quickly with distance. To account for the temperature variation in the blanket and the effect of the temperature on the magnetization, the blanket is divided azimuthally into five sections; each is assumed to have average constant temperature. Figure 12-53 shows a cross section of the blanket and the temperatures assumed for each azimuthal section. Four of the blanket modules are simulated, and the induced field along three different lines parallel to and including the z-axis has been calculated. The locations of these lines are shown in Fig. 12-53. The induced fields along these lines are shown in Figs. 12-54 through 12-56 for the assumed temperature distribution described above and for the average constant temperature throughout the blanket.

As shown in these figures, the maximum field occurs at the end of the blanket. Figure 12-57 shows the azimuthal variation of the induced field at the end of the blanket and at a radius of 0.44 m. The maximum magnitude of the induced field is ~ 0.016 T. This is less than 0.4% of the MARS central cell magnetic field of 4.7 T. We do not believe that these perturbations affect the confinement of the plasma.

12.12.3 Magnetic Loading

The forces on a magnetized body depend on the magnetization of the body, the field gradient within the body, and the shape of the body. The gradient of the field is substantial in the regions occupied by the coolant pipes. Figure 12-58 shows a cross section of the blanket and the coordinate system used. Note that all the coolant pipes, except for those located at servicing stations, are adjacent to the magnets, whereas the coolant pipes at the servicing stations are midway between the magnets. In addition, the discharge pipes are closer to the magnets than the feed pipes because the allowable space under the machine is limited. Thus, all these pipes will experience different magnetic forces that vary in magnitude and direction according to their locations.

Consider first the magnetic forces on the feed pipe located midway between two central cell magnets, i.e., the forces corresponding to the feed pipe at any servicing station. To save computer time, the pipe was actually simulated in GFUN as a square-cross-section solid beam that has the same volume of HT-9 as the pipe. The beam location, dimensions, and the field along its center are shown in Fig. 12-59. The magnetic forces per unit length

Fig. 12-53. A cross section of the blanket showing the temperature distribution assumed in the blanket. The O indicates the position of the lines along which the induced field is calculated.

Fig. 12-54. The induced magnetic fields mod (B) along the z-axis from the constant temperature blanket (X), and from the varying temperature blanket (O).

Fig. 12-55. The induced magnetic fields mod (B) along the line $X = 0$, $Y = 0.44$ m (point A on Fig. 12-53) from the constant temperature blanket (X), and from the varying temperature (O).

Fig. 12-56. The induced magnetic field mod (B) along the line $X = 0.44$ m, $Y = 0$ (point B on Fig. 12-53).

Fig. 12-57. The azimuthal variation of the induced field mod (B') at radius $R = 0.44$ m and the plane $Z = 3.6$ m.

Fig. 12-58. Cross section showing the different relative positions of the coolant pipes with respect to the magnets. The dotted parts in the figure show the locations of the simulated sections of the pipe.

Fig. 12-59. The equivalent beam of the feed pipe at $Y = 2.88$ m, and the magnetic field along the beam's axis.

are shown in Fig. 12-60. The forces are given relative to the magnitude of the weight of HT-9 per unit length. The large arrow in Fig. 12-60 represents the magnitude of the weight per unit length of Li-Pb coolant that would fill the original pipe (in the same units). In this case, the magnetic force would have a maximum value slightly larger than the weight of the coolant.

Fig. 12-60. The force distribution on the beam shown in Fig. 12-59. The large arrow represents the magnitude of the weight per unit length of the Li-Pb coolant that would fill the original pipe.

Note that the magnetic forces tend to pull the beam toward the center of the machine. Thus, the vertical forces on the discharge pipe that is located at the same horizontal and axial position but at the negative y-position of the feed pipe will have equal--but positive--vertical forces. In other words, the vertical magnetic forces will be in the same direction as the weight of the feed pipe, but will be in the opposite direction of the weight of the discharge pipe.

When the position of the beam is raised to that shown in Fig. 12-61, the magnetic forces become much smaller as shown in Fig. 12-62. The corresponding stresses resulting from the magnetic forces in the above two cases were calculated^{4,5} and are shown in Fig. 12-63. The maximum stress is about 15 MPa in the first case and is negligible for the second case.

Fig. 12-61. The equivalent beam of the feed pipe at $Y = 5$ m, and the magnetic field along the beam's axis.

Fig. 12-62. The force distribution on the beam shown in Fig. 12-61.

Fig. 12-63. The maximum effective stresses along the pipe due to the magnetic forces shown in Figs. 12-60 and 12-62.

The next case considered is the magnetic forces on the vertical part of the inlet coolant pipe that is adjacent to the magnet and extends from $y = 2$ m to $y = 3$ m. The outer diameter of the pipe is 0.43 m and the thickness of the pipe chosen in this calculation is 0.05 m. The pipe is symmetric with respect to the y - z plane, and only the $x > 0$ part of the pipe was simulated. The magnetic force distribution on this part of the pipe is shown in Fig. 12-64 for different y positions, and as a function of the azimuthal angle that is measured from the z -axis to the radius of the pipe. The radial and the azimuthal components of the force exhibit a strong variation with the azimuthal angle. Figure 12-65 shows a schematic diagram of the radial and the azimuthal forces. Although the magnitude of these forces is small compared with the coolant pressure, its nonuniform distribution may lead to distortion where the pipe can deform significantly by either thermal or irradiation creep. The total force on the whole pipe has a magnitude of 2.2×10^5 N ($\sim 5 \times 10^4$ lbf), and because of symmetry it has no component in the x -direction. The distribution of the magnetic force (given in the cartesian coordinate system) along the pipe axis is shown in Fig. 12-66 together with the distribution of the magnetic force on the equivalent square-cross-section solid beam.

When the pipe is shifted to be exactly midway between the central cell magnets, i.e., corresponding to the location of the coolant pipe at servicing stations, the force distribution exhibits a mirror symmetry as shown in Fig. 12-67. The total force in this symmetry location has components only in the y -direction and is equal to -1.3×10^5 N. The calculations show that any slight displacement of the pipe from this azimuthal location will develop a net force in the direction that tends to increase the displacement. This is in agreement with the fact that magnetized bodies are not in stable equilibrium.

Fig. 12-64. The azimuthal variation of the magnetic force components in N per cm of length: radial (o), azimuthal (□), and the vertical component (x), at different vertical locations of the pipe adjacent to the magnet.

Fig. 12-65. Schematic of the force distribution shown in Fig. 12-64.

Fig. 12-66. The distribution of the magnetic force components on the pipe (○) and on an equivalent solid beam (□).

Fig. 12-67. The azimuthal variations of the magnetic force components: radial (o), azimuthal ($\square$), and the vertical component (x) at different vertical locations of the pipe midway between the magnets.

In conclusion, the magnetic forces on the magnetic components vary according to the location and the shape of the component. This should be considered in designing the appropriate structure to support such forces. The stresses resulting from these magnetic forces are tolerable, but should be included in all stress calculations.

12.13 BLANKET SAFETY

12.13.1 Emergency Cooling

12.13.1.1 Loss of Flow and Loss of Coolant Accidents. During reactor operation, loss of flow and loss of coolant accidents (LOF and LOC) may occur. Failure of the pump is one of the possible causes of these accidents. In this case, the plasma is still at full power. It is essential to calculate the temperature response of the first wall to estimate how soon the plasma has to be quenched. The heat transfer model assumes a stationary $\text{Li}_{17}\text{Pb}_{83}$ blanket or empty blanket and calculates the first wall temperature under nuclear heating. We have obtained the nuclear heating curve from neutronic calculations.

The temperature response of the first wall due to a loss of flow and loss of coolant accident is calculated and shown in Fig. 12-68. The rate of the first wall temperature rise is very similar in the two cases. The reason for this unexpected result is 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 LOF accident case, 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 LOC accident case. As time reaches $\sim 20 \text{ s}$, the $\text{Li}_{17}\text{Pb}_{83}$ farthest away from the first wall--and, consequently, with a much lower volumetric heating--starts to cool the first wall by conduction. This explains the crossover point of the two temperature response curves.

If LOF or LOC occurs, the blanket will suffer permanent damage if the plasma continues to operate for 15 s after the accident. The first wall will melt if the plasma is not extinguished within $\sim 35 \text{ s}$ after the onset of the accident.

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. 12-69, will reach a maximum of 800°C in $\sim 30 \text{ s}$. The coolant volumetric flow rate required is $5 \text{ m}^3/\text{s}$ for the entire reactor. The total emergency cooling time provided has to be optimized between surge tank cost and accident detection time.

12.13.1.2 Afterheat Calculations. The afterheat is calculated by the ANISN and DKR codes. We ran a forward 25-neutron group ANISN calculation to obtain the steady-state flux throughout the blanket and shield region. This output was then fed into the DKR program, which produced a delayed gamma source spectrum and also gave the total beta heat produced in each zone. The gamma source was then fed back into the ANISN program and a forward 21-group gamma transport problem was run. In this calculation, we used activation cross sections to determine the heat deposition in each region and material. This

Fig. 12-68. Temperature response of the first wall for loss of flow and loss of coolant in the blanket.

Fig. 12-69. Temperature response of the first wall with gravitational coolant flow.

calculation was performed for both the case of the $\text{Li}_{17}\text{Pb}_{83}$ remaining in the blanket and the case of $\text{Li}_{17}\text{Pb}_{83}$ drained from the blanket. In all, a total of 12 ANISN runs were made. Finally, the beta heat from the DKR and the gamma heat from ANISN were summed up to give the total afterheat in each zone. The results of these calculations are given in Figs. 12-70 and 12-71. Figure 12-70 shows the afterheat with the blanket still filled with $\text{Li}_{17}\text{Pb}_{83}$ whereas Fig. 12-71 shows the afterheat with the $\text{Li}_{17}\text{Pb}_{83}$ drained.

Fig. 12-70. Afterheat in different regions with $\text{Li}_{17}\text{Pb}_{83}$.

Table 12-36 shows the adiabatic temperature rise rate in different regions of the blanket and shield. Note that at 1 hour after reactor shutdown, the temperature of the blanket and reflector will increase 1°C every 10 s. Therefore, active cooling is clearly required. About 1% of the normal cooling rate is needed at 1 hour after shutdown. Note also the very high afterheat in the reflector after shutdown. This is caused by the ^{56}Mn in Fe-1422. The half-life of ^{56}Mn is 2.6 hours and, therefore, the afterheat of the reflector decays rapidly in the first day. After one day the reflector can be used as the heat sink for radiation cooling of the blanket. The blanket needs active cooling for the first day. It can be cooled effectively after that and maximum temperatures due to afterheat may reach 390°C if the surface emissivity is 0.4.

Fig. 12-71. Afterheat in different regions after $\text{Li}_{17}\text{Pb}_{83}$ is drained.

Table 12-36. Adiabatic temperature rise rate (in °C/s).

	With $\text{Li}_{17}\text{Pb}_{83}$		Without $\text{Li}_{17}\text{Pb}_{83}$	
	1 hour after shutdown	1 day after shutdown	1 hour 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}

We have completed a set of calculations to obtain the temperature response of different regions of the blanket if active cooling is not available and the plasma is quenched. In all cases, the shield is cooled. The cases are:

- a. Blanket loss of flow, reflector loss of coolant (Fig. 12-72).
- b. Blanket loss of coolant, reflector cooled (Fig. 12-73).
- c. Blanket loss of coolant, reflector loss of coolant (Fig. 12-74).
- d. Blanket loss of flow, reflector cooled (Fig. 12-75).
- e. Reflector loss of coolant, blanket cooled (Fig. 12-76).

Fig. 12-72. Blanket temperature response for blanket loss of flow, reflector loss of coolant.

Fig. 12-73. Blanket temperature response for blanket loss of coolant, reflector cooled.

Fig. 12-74. Blanket temperature response for blanket loss of coolant, reflector loss of coolant.

Fig. 12-75. Blanket temperature response for blanket loss of flow, reflector cooled.

Fig. 12-76. Reflector temperature response for reflector loss of coolant, blanket cooled.

Case (a) results in the highest temperature in the accident case, with a maximum temperature of 1300 K. Therefore, the blanket will not melt as a result of afterheat. However, the blanket will suffer permanent damage within 2 to 3 hours of accident time. The following operations will have to be performed within this period of time to keep the blanket from overheating:

- Drain the blanket;
- Activate the He preheating system;
- Circulate the He to provide emergency cooling;
- If this cannot be done, flood the reactor cavity with building atmosphere to cool the blanket.

Table 12-37 summarizes the thermal radiation calculation for the blanket one day after the reactor is shutdown. It can be seen that the blanket can be cooled effectively by thermal radiation. The best estimate of emissivity of the steel tubes under corrosion and high temperature is 0.4. The maximum temperature that can be reached is 390°C at $\epsilon = 0.4$.

Table 12-37. Radiation-cooled blanket temperature with forced cooling stopped one day after shutdown.

	Maximum blanket temperature (°C)		
	Steel tube emissivity		
	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

12.13.2 Lithium-Lead Water Interactions

12.13.2.1 Introduction. Current evaluations of the hazard potential of lithium and lithium-alloy breeders in contact with other reactor materials have been based upon thermodynamic calculations of possible chemical reactions and on dynamic parametric calculations for specific fusion reactor designs. These calculations have led to rough comparisons of the overall hazard potential of different lithium and lithium-alloy breeders with other potential reactor materials.^{44,45} Also, scoping experiments were performed at Argonne National Laboratory by R. Clemmer et al.⁴⁶⁻⁴⁸ and are now underway at HEDL by L. Muhlestein.⁴⁹ These tests indicated that qualitatively the reaction between lithium and lithium-alloy breeders with H₂O is quite different (see Table 12-38). Additional work by Jepson et al.⁵⁰ with the ternary oxides (LiAlO₂, LiZrO₃, LiSiO₃, Li₄SiO₄, and LiTiO₃) showed that these oxides present minimal safety-related problems when used with H₂O; however,

Table 12-38. Reactions of Li-Pb alloys and lithium with water.

Case	Composition	Sample state ^a	Temperature (K)	Water temperature (K)	Reaction
1	Li ₇ Pb ₂	s	773	298	Modest
2	Li ₇ Pb ₂	s	773	369	Vigorous
3	Li ₇ Pb ₂	s	873	368	Vigorous
4	Li ₇ Pb ₂	ℓ	1103	368	Very vigorous
5	Li _{0.62} Pb _{0.38}	ℓ	773	368	Vigorous
6	Li _{0.17} Pb _{0.83}	ℓ	773	368	Very modest
7	Li	ℓ	773	368	H ₂ detonation
8	Li ^b	ℓ	773	368	Detonation

^as = solid, ℓ = liquid.

^bInjected under water.

they do require neutron multipliers, such as lead, beryllium, or lithium-lead for breeding and power needs. Jepson et al.⁵⁰ go on to state that lithium oxide and Li₁₇Pb₈₃ are prime candidates for blanket materials because of their combined favorable neutronics and minor safety compatibility concerns. Recently some European investigators^{51,52} have initiated studies on the potential of certain breeder-blanket designs and have provided specific design proposals to reduce the overall hazard potential of a Li₁₇Pb₈₃ breeder-blanket.

In this section we will consider Li-Pb/water interactions in MARS. For comparison purposes, we will also consider the equivalent conditions for Li/water interactions and thereby demonstrate the inherent safety characteristics of Li-Pb.

For various accident sequences we can identify four possible contact modes between the molten metal breeder and the water. The first might occur after a steam tube rupture in a liquid metal steam generator. We have called this the "coolant injection mode," because of the high-pressure injection of the steam/water in the low-pressure liquid metal (Fig. 12-77). The second contact mode is characterized by pouring of the liquid metal from a ruptured blanket component into a pool of water, which is present because of the accident. This contact mode is identified as a "fuel pouring contact mode: fuel here means any molten metal that comes into contact with water. A fuel-coolant interaction is a generic class of heat transfer phenomena in which one hot liquid fuel interacts with a more volatile, cold liquid coolant producing coolant vapor and perhaps oxidizing the fuel. Historically it has been considered in fission reactor safety primarily for this contact mode, although the other modes of contact are just as plausible. This contact may

Fig. 12-77. Steam generator tube rupture.

occur in a fission reactor after a severe accident in which molten materials pour from a ruptured vessel into a stagnant pool of coolant in the containment (Fig. 12-78). The third contact mode is characterized by a rupture of reflector water and breeder-blanket tubes in the central-cell vacuum vessel, resulting in a spray of these reactants into a common volume (Fig. 12-79). This mode is of special concern in a fusion design because the major radioactive inventory resides within the vacuum vessel. In addition, this mode may be considered to be a subset of the previous two because it is simply the simultaneous injection of the liquid metal breeder and water into a common volume. The fourth contact mode would occur if the liquid metal and the water came into contact as stratified layers of materials (e.g., lithium pouring on water, water pouring on lithium-lead). We expect this to be the most benign of the possible contact modes because even though the two materials are liquid, density stratification would initially limit their surface area for contact.

This section will be concerned only with modeling the coolant-injection contact mode. A model for a fuel-pouring contact mode is presented in Ref. 53. This model was originally developed for fission reactor safety concerns,^{54,55} and is easily modified for a fusion reactor case. We plan to investigate the third and fourth contact modes in future work.

12.13.2.2 Background. For the coolant injection we specifically considered a steam-tube rupture in a Westinghouse liquid-metal/water steam generator,⁵⁶ the specifications for this case are given in Fig. 12-80. The model being developed predicts the temperature and pressure history and the hydrogen generation rate due to an assumed guillotine break of a steam tube and the

Liquid metal/concrete interactions

Fig. 12-78. Liquid-metal spill in containment.

Fig. 12-79. Coolant leakage into vacuum chamber.

Fig. 12-80. Specifications for steam-tube rupture in a liquid-metal/water steam generator (Ref. 56).

resulting liquid-metal/water-chemical reaction. The model is applied to both a lithium and lithium-lead breeder to obtain a general comparison between these two candidate breeder-blanket materials.

In 1981, Krane and Kazimi⁴⁴ compared lithium and lithium alloy/water reactions for a hypothetical coolant tube leak in the blanket of the University of Wisconsin NUWMAK conceptual fusion reactor. Their work proceeded in two directions. They first performed a static calculation where the breeder and coolant were allowed to interact instantaneously and then determined the final equilibrium temperature of the blanket materials, based on a stoichiometric amount of water and breeder, with a variable degree of completion of the chemical reaction (0 to 100%). They then performed a dynamic calculation in which they modified the LITFIRE computer program to predict the thermal response of the blanket materials. They based the calculation on a specified flowrate of water into a constant volume, spherically shaped, reaction zone, in which the reaction occurred and the reaction products remained. They then determined the temperature response of concentric shells of the undisturbed breeder. They used the resulting temperature profiles as the basis for their qualitative comparison of the hazard potential of different lithium-alloy-based breeders.

Our current work goes beyond that of Krane and Kazimi. We begin with a thermodynamic equilibrium calculation to determine the final temperature of a mixture of lithium, lithium-lead and water. We generalize this calculation so that it only depends on the initial molar ratio of water to lithium in the mixture and not on any absolute amount of water or liquid metal. We then performed a dynamic calculation using a parametric model to predict the consequences of a steam tube rupture. Our parametric model allows for a number of effects:

1. A dynamic reaction zone volume;
2. Variable liquid-metal/water mixture ratios;
3. A dynamic pressure calculation;
4. Pressure relief valve flow out of the steam generator;
5. A critical flow model for the tube rupture break flow.

In the next section, we discuss the thermodynamic equilibrium model followed by a discussion of the model and the current results. Finally, we present some current ideas on the mechanisms for liquid-metal/water chemical reactions.

12.13.2.3 Thermodynamic Equilibrium Model. The stoichiometric chemical reaction for lithium with water is

and for lithium-lead with water it is

$$- 8.33 \frac{\text{kcal}}{\text{gmole of Li}_{17}\text{Pb}_{83}} (@ 25^\circ\text{C}) , \quad (12-37)$$

where i_f is the heat of formation of $\text{Li}_{17}\text{Pb}_{83}$. The thermodynamic equilibrium model consists of determining, for various amounts of liquid metal breeder and water allowed to react completely, the final equilibrium temperature of the products and any remaining reactants; assuming that the energy generated by the reaction does not leave the fuel-coolant system. The initial temperature of the reactants is given by the average temperature of the breeder and water in the steam generator (400°C for the breeder and 375°C for the water). To determine the final equilibrium temperature of the system, we simply balance the energy of the reactants and the products of the above reactions for a given ratio of initial moles of lithium to moles of water (defined at the parameter $x = n_{\text{H}_2\text{O}}/n_{\text{Li}}$). With the parameter x , we can solve for the final equilibrium temperature regardless of the absolute amount of reactants. There are three different equations for each energy balance depending on whether $x < 1$, $x = 1$, or $x > 1$. For the lithium breeder the three balances are

$$C_L(T_{\text{BO}} - T_0) + x i_{\text{wO}} = (C_{\text{LH}} + C_{\text{H}})(T_f - T_0) + Q + (x - 1)i_{\text{wf}} \quad \text{for } x > 1, \quad (12-38)$$

$$C_L(T_{\text{BO}} - T_0) + i_{\text{wO}} = (C_{\text{LH}} + C_{\text{H}})(T_f - T_0) + Q \quad \text{for } x = 1, \quad (12-39)$$

$$C_L(T_{\text{BO}} - T_0) + x i_{\text{wO}} = (C_{\text{LH}} + C_{\text{H}})(T_f - T_0) + xQ \\ + (1 - x) C_L(T_f - T_0) \quad \text{for } x < 1. \quad (12-40)$$

Definition of terms are supplied in Sec. 12.15 (Appendix 1). The equations for lithium-lead are similar in form to these.

The results of this calculation are shown in Figs. 12-81 and 12-82. These figures are graphs of the final equilibrium temperature (T_f) as a function of the parameter x for the lithium and lithium-lead. For $x > 1$, these figures have three curves. These graphs were drawn by assuming that the enthalpy of the water is determined at the constant pressures of 25, 1000, and 2400 psia. The $\text{Li}_{17}\text{Pb}_{83}$ energy balance was carried out for two values of the heat of reaction (Q). One of the values Q (-8.33 kcal/gmole of $\text{Li}_{17}\text{Pb}_{83}$) is arrived at by assuming that the heat of formation of $\text{Li}_{17}\text{Pb}_{83}$ (i_f) is negligible. The other value of Q (-6.36 kcal/gmole of $\text{Li}_{17}\text{Pb}_{83}$) is arrived at by including an estimate of the heat of formation i_f . This estimate of the heat of formation of $\text{Li}_{17}\text{Pb}_{83}$ is given by

$$i_f = \Delta G + T \Delta S, \quad (12-41)$$

where G is the Gibbs free energy and S is the entropy as suggested by E. M. Larsen.⁵⁷

This model provides an upper bound on the mixture temperature in the reaction zone of the steam generator caused by a steam tube rupture. Comparison of Figs. 12-81 and 12-82 shows that the lithium-lead may produce substantially lower temperatures than lithium because of the much larger

Fig. 12-81. Thermodynamic equilibrium temperature (T_f) vs x curves for Li breeder.

Fig. 12-82. Thermodynamic equilibrium temperature (T_f) vs x curves for $Li_{17}Pb_{83}$ breeder.

thermal inertia of the lead. Also, the liquid lithium may produce substantial system pressure increases because of lithium or lithium-hydroxide vaporization for a constant volume situation. This possible vaporization of lithium or lithium hydroxide from the use of a lithium breeder would pose additional safety hazards as lithium and associated radioactivity may take the form of aerosols.

12.13.2.4 Dynamic Parametric Model. Our model for the coolant injection contact mode is applied to a Westinghouse liquid-metal steam generator.⁵⁶ We decided that this design coupled with sizing for the MARS heat transfer requirements was the most logical choice, since Westinghouse is currently involved in the component development of a large prototype steam generator for LMFBF applications.

The Westinghouse "duplex tube" design (Fig. 12-80) uses double-walled tubes with a helium gas at the interface of the two tubes, that can be used for detection of small leaks in either tube. The MARS design requires that about 2800 MW from the blanket be dissipated from 500 to 350°C. Because the power rating of the Westinghouse steam generator is 293 MW, the MARS power station would require 10 steam generators of this design. The large number of plant steam generators, although more expensive than a smaller number, would reduce the hazard of steam generator accidents by confining the accident to one steam generator and isolating the extent of the damage and cleanup or replacement costs.

The steam generator accident we modeled is one in which there is a sudden total rupture of a steam tube (or tubes) characterized by a guillotine break. Because the water is at a much higher pressure here than in the breeder, the mode of contact between the liquid metal and the water is high-pressure coolant injection. For our current calculations we assumed that a steam tube is sheared in half leaving essentially two water-flow paths into the liquid metal breeder, one path connected to the upper plenum and the other path connected to the lower plenum. Our model contains these major assumptions:

1. We base our calculations on the MARS design parameters. The initial pressure of the reactants is 16.5 MPa (2400 psia) for the water and 0.17 MPa (25 psia) for the breeder. Also, the initial shell-side liquid-metal breeder volume and temperature are taken to be $2.78 \times 10^7 \text{ cm}^3$ and 673 K, respectively.
2. There are two interaction zones. We assume that the first zone, designated the reaction zone, is a spherically shaped region that forms around the break. This zone is a homogeneous mixture of reactants and products at thermal equilibrium, which can grow in time. The other zone, designated the nonreaction zone, consists of the rest of the shell side of the steam generator. This zone is assumed to be a homogeneous region of unreacted liquid metal breeder at thermal equilibrium.

3. The flowrate of water into the reaction zone is currently modeled by the one-directional Homogeneous Equilibrium Model (HEM) for critical flow, i.e.,

$$S_{wO} = S_{wb} \quad (12-42)$$

$$V_{wb} = [2(i_{wO} - i_{wb})]^{1/2} \quad (12-43)$$

$$\dot{m}_w = A_{bOwb} V_{wb} \quad (12-44)$$

4. The flow of the liquid metal breeder into the reaction zone is determined by a mixing parameter x , where x is defined as the ratio of the molar flowrate of water to the molar flowrate of lithium into the reaction zone.
5. The reactants, including hydrogen, remain in the reaction zone.
6. The system pressure is maintained below 2400 psia (the water back pressure) by a pressure relief valve. The model accomplishes this by allowing a portion of the unreacted liquid metal breeder to leave the nonreaction zone each timestep. The flowrate out through the valve is modeled by a quasi-steady mechanical energy balance (Bernoulli's equation)

$$\dot{m}_{bp} = \rho_b A_{prv} \left[\frac{2}{\rho_b} (P - P_\infty) \right]^{1/2} \quad (12-45)$$

7. We assume that the increase in pressure in the shell side of the steam generator suspends the normal flow of breeder through the steam generators.
8. There is no back flow through the broken steam tubes.
9. The thermodynamic properties of the reactants and products are constant. The liquids are incompressible. The gases are perfect. For $x > 1$, any unreacted water is assumed to be superheated steam.

This parametric model is the next logical step beyond a simple thermodynamic calculation. We employ two control volumes and a specified mixing parameter in order to keep the modeling of the dynamic process relatively simple and unambiguous. Our current knowledge of the mechanisms of liquid-metal/water interactions is so limited that we feel such an approach is justified. Consistent with this approach is the use of a one dimensional HEM critical flow model for water inflow and a quasi-steady mechanical energy balance for liquid-metal outflow. Because the pressure transient within the shell side of the steam generator is expected to be large but not larger than 16.5 MPa assumptions 7 and 8 are quite reasonable. Finally, the simple equations of state for liquid metal and water are known to be approximate; subsequent calculations will investigate the effect of using more complete equations of state.

Using the mixing parameter x and the relief valve area A_{prv} as variables as well as and the above assumptions, we solve the following mass and energy balances. The reaction zone mass balance is

$$\frac{dm_R}{dt} = \dot{m}_{br} + \dot{m}_w \quad (12-46)$$

The reaction zone energy balance is

$$\frac{dE_R}{dt} = -Q_c - Q_r - p \frac{dV_r}{dt} + \dot{m}_{br} + \dot{m}_{wiw} \quad (12-47)$$

where the reaction zone forced convection heat transfer (Q_r) to the unbroken coolant tubes is given by

$$Q_r = h_t A_{t_r}(t) (T_r - T_w) \quad (12-48)$$

and the derivation of the conductive heat transfer between the reaction zone and the nonreaction zone is given in the Sec. 12.16 (Appendix 2). The nonreaction zone mass balance is

$$\frac{dm_n}{dt} = -\dot{m}_{br} - \dot{m}_{bp} \quad (12-49)$$

The nonreaction zone energy balance is

$$\frac{dE_n}{dt} = Q_c - Q_n - p \frac{dV_n}{dt} - \dot{m}_{br} i_b - \dot{m}_{bp} i_b \quad (12-50)$$

where the nonreaction zone forced-convection heat transfer (Q_n) to the unbroken coolant tubes is given by

$$Q_n = h_t A_{t_n}(t) (T_n - T_w) \quad (12-50)$$

where the areas $A_{t_r}(t)$ and $A_{t_n}(t)$ always equal the total tube surface area and h_t is the overall convective heat transfer coefficient between the tube-side water and the shell-side liquid metal breeder. Note that we include in the enthalpy the heat of formation of each constituent and therefore automatically include the heat of reaction from the oxidation of lithium.

Approximating the differentials by finite-difference techniques, we have written a computer program that explicitly solves the energy balances and numerically calculates shell side pressure, reaction and nonreaction zone temperature, reaction zone volume, and the hydrogen mass generated; all as functions of time from the accident initiation.

In Fig. 12-83, we have plotted the system pressure as a function of time, for three different values of the pressure relief valve area A_{prv} . This parameter is used to determine the flow rate of unreacted liquid-metal breeder out of the system by Eq. (12-45). A comparatively small value of A_{prv} would mean a comparatively small flow rate of unreacted liquid-metal breeder out of the system, and little pressure relief. This is illustrated in Fig. 12-83 by comparing the top curve ($A_{prv} = 5 \text{ cm}^2$) with the other two curves ($A_{prv} = 50 \text{ cm}^2$ and $A_{prv} = 500 \text{ cm}^2$). One notes that the system pressure quickly approaches the limiting pressure of 2400 psia, which is initial pressure of the water, for the smaller values of A_{prv} . Another trend illustrated in this figure, and which is exhibited in the results of every calculation, is that the system pressure quickly ($\sim 0.1 \text{ s}$) reaches a maximum pressure (P_{max}), then it levels off to a pressure (P_g), which is slightly less than P_{max} ($P_{max} - P_g < 50 \text{ psia}$). In Fig. 12-84, we have plotted the maximum system

Fig. 12-83. System pressure for $\text{Li}_{17}\text{Pb}_{83}$ ($x = 1.0$).

Fig. 12-84. Maximum pressure for Li and $Li_{17}Pb_{83}$ ($x = 1.0$).

pressure as a function of A_{prv} for both lithium and lithium-lead liquid-metal breeders. The similar shape of the two curves in this figure suggests a relationship between the lithium and lithium-lead. From Eq. (12-36), we note that the flow rate of unreacted liquid-metal breeder out of the system ($\dot{m}_{bp}$) is proportional to A_{prv} and the square root of the liquid-metal breeder density (ρ_b)

$$\dot{m}_{bp} \propto A_{prv}(\rho_b)^{1/2}$$

The flow rate out of the system is only a function of the properties of the liquid-metal breeder through the square root of the density. In fact, if the abscissa of Fig. 12-84 was $\rho_b(-1/2)A_{prv}$ instead of A_{prv} , the two curves would be coincident. The similarity in the behavior for the two liquid-metal breeders is further established in Fig. 12-85. This figure plots the mass of hydrogen generated for both liquid-metal breeders after 10 s from the onset of the accident as a function of $\rho_b(-1/2)A_{prv}$. Here, the curves for lithium and lithium-lead overlap.

The liquid-metal/water interaction given a fixed value of x is governed by the pressure response. This is because the flowrate of water into the reaction zone (and thus the flowrate of liquid-metal breeder into the reaction zone because it is coupled to the water flowrate through the parameter x) is a function of the system pressure. The flowrate of unreacted liquid-metal

Fig. 12-85. Mass of H_2 generated as a function of $\rho_b^{-1/2} A_{prv}$ ($x = 1$).

breeder out of the system is also a function of the system pressure. Thus, the flowrates into and out of the system are coupled together through the pressure history. For a constant mixing parameter the integral behavior of water injection into lithium or lithium-lead is very similar. If we attempt to determine the mechanistic amount of hydrogen generated, this preliminary conclusion might be altered.

One would not expect the temperature response of the two liquid-metal/water pairs to be similar because of the lower thermal inertial of lithium.

In Fig. 12-86 we have plotted the temperature of the reaction zone (T_r) and the temperature of the nonreaction zone (T_n) as functions of time, for a typical calculation. This figure illustrates trends in the system's two average temperatures, which are exhibited in the results of every calculation regardless of the values of the parameters x and A_{prv} or the choice of liquid-metal breeders. The trend in the reaction zone temperature is that it peaks very soon after the onset of the accident (0.01 to 0.5 s) and then quickly levels off to 375°C. The trend in the nonreaction zone temperature is that it steadily decreases and quickly levels off to 375°C; this temperature is the ambient temperature of the water in unbroken steam tubes. This shows that the heat from the reaction is quickly and effectively removed

Fig. 12-86. Temperature of the reaction zone and nonreaction zone.

from the system by convection to unbroken steam tubes. This occurs because the accident suspends the normal flow of liquid-metal breeder through the steam generator due to the rapid pressure buildup. The normal function of the steam generator, which is transfer of the energy of the liquid-metal breeder, is thus diverted to transfer of the chemical heat of the reaction. A similar calculation for liquid lithium indicates that the maximum reaction temperature occurs early in time (~ 0.5 s) and is slightly below the thermodynamic maximum value (~ 2000 K).

12.13.2.5 Considerations for Liquid-Metal/Water Reactions. Given a mixing ratio between the liquid metal (Li or Li₁₇Pb₈₃) and water, the parametric model indicated that the pressure history and hydrogen generated are quite similar. The temperature history of each is different, although both are below the thermodynamic limits. The next step in our work is to relax the assumption of a constant mixing parameter ratio x , and investigate the rate mechanisms for liquid metal oxidation. In this way we can improve our chemical reaction modeling and more accurately determine if the amount of hydrogen generated is different for different lithium alloys. This may be an important consideration in determining the hydrogen inventory after an accident.

The amount of hydrogen generated during a liquid-metal/water interaction is the product of two physical processes: the first is the kinetic rate of reaction per unit surface area and the second is the available metal surface area that evolves during the fuel-coolant interaction.⁵⁸ Metal oxidation is practically limited by mass transfer in the gas and liquid (or solid oxide) phases near the metal surface.^{59,60} In the gas phase, steam must diffuse through the evolved hydrogen to the liquid metal surface. If the chemical reaction is exothermic and the heat of reaction is large, the liquid metal surface will heat up substantially as it oxidizes until the liquid metal vapor pressure becomes significant causing substantial liquid metal vapor evaporation, oxidizing as it diffuses into the steam vapor. This vapor-phase oxidation of the liquid metal is the dominant mechanism of normal combustion processes; it has a very rapid oxidation rate. Liquid sodium oxidation is thought to be governed by this mechanism.⁶¹ Because of the low thermal inertia of lithium, it is quite possible that under certain circumstances lithium metal can overheat sufficiently and undergo "vapor-phase combustion."

On the other hand if liquid metal evaporation rate is negligible as in lithium-lead (the lithium chemical activity is 0.1% that in pure lithium), then steam vapor must diffuse to the metal surface through evolved hydrogen for liquid metal oxidation. Following past work,⁵⁹ the governing mass transfer equation for this process can be written in a general form for equimolar counter diffusion of perfect gases (steam-hydrogen) as

$$\frac{dN_H}{dt} = A \frac{D_H}{R_0 T_V} \frac{dP_H}{dn} \quad , \quad (12-51)$$

where dN/dt is the molar rate of hydrogen diffusion in steam (mole/s), D_H is the diffusion coefficient for steam and hydrogen, R_0 is the universal gas constant, P_H is the hydrogen partial pressure, T_V is the gas-vapor temperature, n is the outward direct normal, and A is the surface area. One can integrate this equation for a certain geometry, assuming a constant D_H for a pressure and a mean gas temperature.

The second mass transfer resistance, in the absence of "vapor-phase combustion," is the diffusion of oxygen atoms through the metal oxide (liquid or solid) to the unreacted metal. This diffusion process is quite complex; for example, for solid oxide being formed on the metal surface, the hydrogen generation rates are usually correlated from test data⁶⁰ under the boundary conditions that an excess of steam is present and the solid metal oxide resists further mass transfer. For zirconium,⁶⁰ the rate of reaction is given by a parabolic kinetic rate expression

$$\frac{d}{dt} (W^2) = 3.8(10^3) \exp \left(\frac{-84300}{R_0 T_V} \right) \quad ; \quad (12-52)$$

where W is the cumulative mass of hydrogen in kg/m^2 and R_0 is given here in cal/gmole K . For a liquid oxide the process is even more complex because the oxide may be miscible in the metal or the molten metal and oxide components could become mechanically mixed by internal convection currents. In this case, very little data exists for liquid metals.

At the present time there have been no controlled experiments that measure the kinetic rate of oxidation for lithium or lithium-based alloys. Therefore, it is not yet clear what is the reason for the qualitatively less reactive behavior observed for lithium-lead as compared to lithium in scoping tests.⁴⁶⁻⁴⁸

The second physical process to consider is the available surface area for reaction that is evolved during liquid-metal/water contact. For this coolant injection mode of contact, the water is depressurized as it flows out the break and begins to flash into steam as it reacts with the liquid metal. In the current model we consider the reaction zone to be spherical and remain intact during the reaction. In reality, the water-steam mixture will break apart into individual bubbles and rise in the liquid metal. The available surface area then will be dictated by the breakup of the two-phase jet issuing forth from the break, and the rise time of the steam and water to the liquid metal surface at the top of the steam generator. To date no tests have been done for lithium or lithium-alloy/water system, although we are now reviewing data from sodium-water tests to determine their applicability [e.g., Ref. 62].

12.13.2.6 Conclusion. A parametric model was developed to predict the transient pressure and temperature behavior for a liquid-metal/water interaction in a steam generator following a steam tube rupture. The model results suggest three preliminary conclusions. First, the maximum temperature for lithium-lead due the reaction although above normal limits is not of a safety concern for the steam generator, because it is below the thermodynamic maximum value and quickly falls to within operating limits. Second, the pressure history and hydrogen generated from water injection into lithium or lithium-lead are quite similar for a given value of the metal/water mixing parameter. This suggests that the pressure flow characteristics govern the integral response given an assumed degree of chemical reaction. Finally, one must consider reaction kinetics for an accurate prediction of accident response. We have begun to consider reaction kinetics for lithium and lithium-lead interactions; it is expected that lithium may be a "vapor phase combustion" process while lithium-lead is governed by metal surface mass transfer phenomena.

12.14 R&D REQUIREMENTS

There are six major R&D areas suggested by this work and these are briefly discussed below.

1. The effect of magnetic fields on the flow of liquid metals needs to be experimentally measured. Such studies need to be conducted on realistic geometries and should be closely coupled with the design community.
2. Criteria for first-wall lifetime need to be continually refined. This requires closer coupling with the structural design community and reexamination of the present limits on creep, ductility, and yield strength.
3. The corrosion rates of Li-Pb on both austenitic and ferritic steels need to be measured and confined by several independent experimenters. The effect of corrosion inhibitors and the

possibility of removing radioactive corrosion products needs to be examined as soon as possible. The reason that such studies are so urgent is that the upper limits on fusion blanket operation with Li-Pb alloys are now set by interpretation of very sparse or, in some cases, irrelevant data.

4. Irradiation effects on ferritic steels in the range from 150 to 200 dpa are needed to see if lifetimes of 10 to 15 MW-y/m² can be achieved.
5. Experiments on the interaction of ferritic blankets with superimposed magnetic fields are required. Furthermore, the effect of the magnetic field on the stress in the blanket and associated piping needs to be experimentally measured.
6. More effort needs to be applied on the safety aspects of Li-Pb alloys. In particular, the fate of dissolved radioactive corrosion products needs to be determined and compared to similar studies with Li.

There are many more R&D items which could be discussed here but those stated above are critically important to the success of a safe fusion plant and should command a large fraction of the attention and resources in this area for the next 5 to 10 years.

12.15 APPENDIX 1

NOMENCLATURE

A	an area.
A _b	the area of the steam tube break.
A _{prv}	the area of the pressure relief valve and a variable in dynamic model equations.
A _t	the surface area of a steam tube.
C _H	the molar specific heat of hydrogen.
C _{Li}	the molar specific heat of lithium.
C _{LiH}	the molar specific heat of lithium hydroxide.
E _R	the total internal energy of the reaction zone.
h	the overall heat transfer coefficient of the steam tube.
i _b	enthalpy of the liquid-metal breeder entering the reaction zone.
i _f	the heat of formation of the liquid metal breeder, Li ₁₇ Pb ₈₃ .
i _{wb}	the enthalpy of the water/steam flowing through the steam tube break.

i_{wf} the final enthalpy of the water in the thermodynamic equilibrium model.
 i_{wo} the initial enthalpy of the water in the thermodynamic equilibrium model.
 k_n the thermal conductivity of the nonreaction zone, liquid metal breeder.
 $\dot{m}_{bp}$ the molar flow rate of unreacted liquid metal breeder through the pressure relief valve.
 $\dot{m}_{br}$ the molar flow rate of liquid-metal breeder into the reaction zone.
 $\dot{m}_w$ the molar flow rate of water into the reaction zone.
 $\dot{m}_n$ the total mass of the nonreaction zone.
 $\dot{m}_R$ the total mass of the reaction zone.
 N_H the number of moles of hydrogen.
 P the variable system pressure.
 P_ℓ the final quasi-steady pressure.
 P_{max} the maximum system pressure.
 P_∞ the pressure relief valve back pressure, assumed to be 1 atm.
 Q the heat of reaction.
 Q_c the conduction heat flow between the reaction zone and nonreaction zone.
 Q_n the convective heat flow from the nonreaction zone to unbroken steam tubes.
 Q_r the convective heat flow from the reaction zone to unbroken steam tubes.
 R the radius of the reaction zone.
 S_{wb} the entropy of the water at the tube break.
 S_{NO} the initial entropy of the water.
 T_f the final thermodynamic equilibrium temperature.
 T_n the average temperature of the nonreaction zone.
 T_{ni} the initial temperature of the nonreaction zone.

T_0	the reference temperature in the thermodynamic equilibrium model, chosen to be 0°C.
T_r	the average temperature of the reaction zone.
T_w	the average temperature of the water/steam in the steam tubes, assumed to be 648 K.
V_r	the volume of the reaction zone.
v_{wb}	the velocity of the water flowing through the steam tube break.
x	the mixing parameter. In the thermodynamic equilibrium model, it is defined as the molar ratio of water to Li in the initial mixture. In the dynamic model, it is defined as the ratio of the molar flow rate of H ₂ O to the molar flow rate of Li into the reaction zone per timestep.
α_n	the thermal diffusivity of the liquid metal breeder.
ΔG	the Gibb's free energy of the Li ₁₇ Pb ₈₃ .
ΔS	the entropy of the Li ₁₇ Pb ₈₃ .
λ	the conduction length scale derived from boundary layer theory.
ρ_b	the density of the liquid metal breeder.
ρ_{wb}	the density of the water/steam flowing through the steam tube break.

12.16 APPENDIX 2

Assuming the reaction zone is spherical, the heat conduction flow from a sphere to a surrounding fluid is given by

$$Q_c = 4\pi R^2 \frac{k_n}{\lambda} (T_r - T_n) \quad , \quad (A.1)$$

where λ , defined as the penetration depth, is the conduction length scale from boundary layer theory. To determine the penetration depth, we first consider the equation of energy for the nonreaction zone

$$\frac{\alpha_n}{r^2} \frac{\partial}{\partial r} \left(\frac{T_n}{r} \right) = \frac{\partial(T_n)}{\partial r} + \frac{\partial r}{\partial t} \left[\frac{\partial(T_n)}{\partial r} \right] \quad , \quad (A.2)$$

with the initial condition

$$T_n|_{t=0} = T_{n_i} \quad , \quad (A.3)$$

and boundary conditions

$$Tn|_{r=R} = Tr, \quad (A.4)$$

$$Tn|_{r \rightarrow \infty} = Tn_i, \quad (A.5)$$

$$\left. \frac{Tn}{r} \right|_{r \rightarrow \infty} = 0. \quad (A.6)$$

The nonreaction zone is considered to be semi-infinite and an assumed quadratic temperature profile is inserted into Eq. (A.2):

$$Tn - Tn_i = (Tr - Tn_i) \left(1 - \frac{r - R}{A} \right)^2. \quad (A.7)$$

Integrating, the resulting equation is

$$B \frac{d\lambda}{dt} = \frac{\alpha n}{\lambda} - \frac{\lambda A}{2(Tr - Tn_i)} \frac{dTr}{dt} - C \frac{dR}{dt}, \quad (A.8)$$

where

$$A = \frac{1}{3} + \frac{1}{6} \frac{\lambda}{R} + \frac{1}{30} \left(\frac{\lambda}{R} \right)^2, \quad (A.9)$$

$$B = \frac{1}{6} + \frac{1}{6} \frac{\lambda}{R} + \frac{1}{20} \left(\frac{\lambda}{R} \right)^2, \quad (A.10)$$

$$C = \frac{1}{2} + \frac{1}{3} \frac{\lambda}{R} + \frac{1}{12} \left(\frac{\lambda}{R} \right)^2. \quad (A.11)$$

In the computing scheme, Eqs. (A.8) through (A.11) are used to determine the penetration depth. The penetration depth is then used to determine the heat conduction flow from Eq. (A.1).

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12 Blanket and Reflector

13

Central Cell Shield

13 Central Cell
Shield

13. Central Cell Shield

13.1 DESIGN REQUIREMENTS

13.1.1 Magnet Shielding Requirements

Shielding is required to protect the central cell magnets from neutron and gamma radiation. The blanket is a poor attenuator of neutrons because it contains a large volume of lead and needs a separate shield. The shielding requirement is determined by the radiation limits for the different magnet components. The components most sensitive to radiation damage are the stabilizer, superconductor, and insulator.

Irradiation of the superconducting material (NbTi) degrades the critical current density. In this work, we require that the peak neutron fluence ($E > 0.1$ MeV) not exceed 3×10^{22} n/m², which corresponds to a 10% decrease in the critical current density.¹ Seventy percent of this effect will anneal out at room temperature.²

Mechanical strength tests of irradiated magnet insulators have shown that polyimides are 5 to 10 times more radiation-resistant than comparably prepared epoxies.³ Polyimide is used for electrical insulation in the MARS coils. Aluminized Kapton, which is about 100 times more radiation-resistant than aluminized Mylar,⁴ is used for thermal insulation. Radiation effects on organic insulators are not reversible and it is essential that they last for the whole reactor life. Based on recent experimental data, we considered the limits on the dose for the electrical and thermal insulators to be 2×10^{10} and 10^{10} rad, respectively.^{4,5}

Irradiation at cryogenic temperatures produces immobile point defects and the resistivity at zero field increases by the radiation-induced resistivity. This has an impact on the total magnetoresistivity at the operating field according to Kohler's rule.⁶ For the magnet to be cryostable, the resistivity increase must be limited such that the I^2R heat generated in the stabilizer can be removed by the normal helium (LHe I) coolant, which can handle a maximum heat flux of 0.2 W/cm². Using the conductor design for the central cell coils, we determined that the total resistivity after irradiation at the maximum field of 7.14 T should not exceed 136.5 nΩcm (Ref. 7). Half-hard copper with a residual resistivity ratio (RRR) of 80 is used with a residual resistivity of 19.31 nΩcm. Using the charts given in Ref. 7, we obtained a damage limit of 5.6×10^{-4} displacements per atom (dpa). Experimental results indicate that 80 to 90% of the radiation-induced defects can be recovered by room temperature annealing.⁸ Hence, annealing frequency should increase as the reactor approaches the end of life. Based on reactor availability requirements for MARS, the time between anneals should be at least one full power year (FPY). Assuming 85% recovery with annealing and a 24-FPY reactor lifetime, the charts in Ref. 7 yield a minimum required time of 4.45 FPY before the first magnet anneal. Hence, the limit on the dpa rate is 1.26×10^{-4} dpa/FPY.

The limit on the cryogenic heat load resulting from nuclear heating in the magnet is design dependent. Although peak power densities in the winding pack as large as ~ 200 mW/cm³ can be accommodated without exceeding the surface heat flux removal limit of 0.2 W/cm², much lower power densities are required to avoid excessively high cryogenic refrigeration and plant costs. The central-cell cost trade-off analysis discussed in this section implies a peak power density limit of 0.08 mW/cm³. The composite cost of the magnets and cryoplant tends to increase sharply if the peak power density increases beyond this limit.

13.1.2 Determination of Optimum Magnet Heat Loads

The economic cost trade study to determine the optimum heat loads in the central cell magnets and, therefore, the inside radius of the magnet winding pack was performed as follows. First, we fixed the radial dimensions of the blanket and reflector at the values given in Sec. 12 of this volume. Second, we determined the volume fractions and composition of the shield (but not the radial dimensions) by a separate optimization study, discussed below in this section. Third, using the inner radius of the winding pack r_{wp} as a datum point, we varied the radial dimensions of the shield, magnet, and magnet system (i.e., magnet case, liquid N_2 shield, and dewar) such that r_{wp} spanned the range $r_{wp} = 1.80$ to 2.50 m; the clearance gap between the outside of the shield and the inside of the magnet vacuum dewar was held constant at 7 cm. Fourth, we computed the total heating rate in the magnet for each incremental value of r_w . This total heating rate has two contributions, namely (1) nuclear heating and (2) leakage heating from magnet supports, current supply leads, and radiation from the liquid N_2 shield. Nuclear heating is a function of the shield dimensions at this particular value of r_{wp} (see Sec. 13.4), whereas contributions to the leakage heating rates scale variously as the mass and surface area of the magnet winding pack. Fifth, at each incremental value of r_{wp} , we computed an economic figure of merit, this being a composite cost figure composed of the total capital cost of the shield, magnet (including case, dewar, and radiation shields etc.), liquid helium cryogenic plant, and liquid nitrogen cryogenic plant, as well as a discounted operating cost for the cryogenic systems over the 24-FPY lifetime of the plant.

Figure 13-1 plots this composite cost figure of merit in dollars as a function of the inner radius of the magnet winding pack r_{wp} . As r_{wp} is increased from 1.8 m, the composite cost decreases sharply, reflecting the rapid decrease in nuclear heating in the magnet and, therefore, the rapid decrease in capital and operating costs of the cryogenic system. For large

Fig. 13-1. Variation of the composite cost figure of merit for the shield, magnet, and cryogenic plant as a function of the inside radius of the magnet winding pack. The corresponding peak heat load at the inside edge of the winding pack is shown for reference.

values of r_{wp} up to and above 2.5 m, the composite cost increases steadily as a result of the increasing capital cost of the magnet system. At these large radii, the nuclear heating has fallen to negligible levels because of the corresponding large radial thickness of the shield; the only contribution to magnet heating at cryogenic temperatures comes from leakage heating.

The minimum in the composite cost curve is seen to be rather broad. Accordingly, we selected a design point of $r_{wp} = 2.10$ m as shown. This point is close enough to the true minimum of $r_{wp} = 2.06$ m but is slightly farther removed from the lower region of the curve where the composite cost increases rapidly and, therefore, provides a margin of safety. Note that this has determined the inside radius of the magnet winding pack as 2.10 m.

Also shown in Fig. 13-1 is the corresponding peak heat load in mW/cm^3 at the inside radius of the magnet winding pack. This provides a convenient datum parameter to characterize the total heating in the magnet system and, therefore, the economic impact on the cryogenic system. At the true minimum in the curve ($r_{wp} = 2.06$ m), the corresponding peak heat load in the magnet winding pack is $0.04 mW/cm^3$, whereas at the design point of $r_{wp} = 2.10$ m, the peak heat load is $0.02 mW/cm^3$. An upper limit on the peak heat load would appear to be $\sim 0.08 mW/cm^3$, above which the composite cost increases very rapidly with decreasing r_{wp} .

13.1.3 Radioactivity and Activation Requirements

Although the previous considerations are adequate for protecting radiation-sensitive components of the magnets, other considerations influence blanket and shield design. One of these is the desire to minimize long-term radioactivity in the system. Previous experience has shown that the presence of nickel in structural materials results in significant long-term radioactivity. Consequently, the basic structural material for the blanket and shield was chosen to be a low-nickel manganese austenitic steel, namely Fe-1422.

The final factor that we considered was the requirement that there be sufficient shielding to allow 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 Regulation (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-hour work week, or 2.5 mrem/hour. In interpreting these requirements for this design, 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 2.5 mrem/hour after 1 day, 2.5 mrem/hour after 2 days, 10 mrem/hour after 1 day, 10 mrem/hour after 2 days, and 100 mrem for 5 working days after 1 to 2 days. Although the first criterion is the most preferable because it permits relatively quick access to the machine and a low radiation dose, it is overly restrictive. We rejected the two 10-mrem criteria because they could result in excessive radiation doses. We adopted the last criterion for this design because it satisfies the limits in 10CFR20 for the first week and results in lower doses for subsequent periods, thus still permitting reasonably prompt access for servicing.

13.2 DESIGN SUMMARY

The central cell shield, optimized to minimize the radiation effects in the central cell magnets, is 41 cm thick. It consists of three layers. The innermost layer is 10 cm thick and has the same composition as the reflector, namely 95 vol% Fe-1422 and 5 vol% H₂O. The intermediate layer is 23 cm thick and consists of 86 vol% boron carbide (0.87 density factor), 10 vol% Fe-1422, and 4 vol% H₂O. The third layer is 8 cm thick and is made of 90 vol% lead, 5 vol% Fe-1422, and 5 vol% water. This shield configuration provides adequate protection for the magnets with a comfortable margin that allows for possible data uncertainties and calculational deficiencies. The outer lead-shield is extended for an extra 15 cm in the space between magnets to allow for hands-on maintenance 1 1/2 days after shutdown. The shield is fabricated in the form of compartmentalized structure and the filler blocks of the appropriate materials are inserted and fastened. Water cooling surrounds the filler blocks on all sides. The last layer of the shield is a cover plate that is sealed from the outside. The unit weight of the central cell shield is 35.2 tonnes/m.

13.3 MECHANICAL DESIGN

The primary function of the shield in the central cell is to provide radiation protection for the central cell magnets. In spaces between magnets an additional biological shield is provided to allow for hands-on maintenance at the shield boundary 36 hours after shutdown.

The central cell shield is cylindrical with an inner radius of 144 cm and an outer radius of 185 cm. From the description of the reflector in Sec. 12.5 of this volume, it is obvious that the shield and the reflector are in contact with no space in between. The shield is segmented in the same way as the reflector: there are 19 segments 6.32 m long and 2 segments 5.08 m long.

Figure 13-2 gives the overall view of the central cell shield with an expansion of the cross section at A-A. The space for a central cell magnet is indicated and the 15-cm thick additional biological shield is also shown. The compositions in the four shield zones are shown in Table 13-1.

The shield consists of a cylindrical inner structure that is grooved for cooling, followed by compartments for the various materials in the three zones. The filler blocks in zone B are of boron carbide; in zone C they are of lead. The boron carbide and lead blocks will be canned in thin steel sheets to prevent direct contact with the cooling water. Passages provided between the filler blocks and the structure and between the blocks themselves are for cooling water. Since the nuclear heating in the shield is very low, the water flow is very small and the pressure is on the order of 0.15 MPa.

The vertical ribs that form the compartments subtend the shield in the circumferential direction. Some of the ribs go all the way to the end of the shield whereas others go only partway. The final structural layer is welded to the ribs as shown in Fig. 13-2. Between welds it can be bolted to intermediate ribs with sealed head bolts.

The shield is fabricated in a similar manner to the reflector, although it will most likely be made out of two halves and then assembled together over the reflector. A void space of 7 cm between the back of the shield and the central cell magnet dewar surface provides ample space for fastening the two halves of the shield. The ribs are cut out to fit the contour of the inner

Fig. 13-2. Central cell shield structure.

Table 13-1. Compositions of the central-cell shield.

<u>Shield A</u>	
Thickness (cm)	10
Fe 1422 (vol%)	95
Water (vol%)	5
<u>Shield B</u>	
Thickness (cm)	23
Boron carbide (vol%)	86
Fe 1422 (vol%)	10
Water (vol%)	4
<u>Shield C</u>	
Thickness (cm)	8
Lead (vol%)	90
Fe 1422 (vol%)	5
Water (vol%)	5
<u>Biological shield</u>	
Thickness (cm)	15
Lead (vol%)	75
Aluminum structure (vol%)	20
Water (vol%)	5

cylinder half and are then welded to it. The preformed filler blocks are then inserted into the compartments and fastened in place. Finally the cover is placed over the ribs and welded. The two halves of the shield are then assembled and fastened over the reflector.

The biological shield is independent of the other parts and is added on after the shield is assembled with the central cell coils. The biological shield can be made out of either two cylindrical halves or four quarters. The segments are then assembled and fastened between the central cell coils.

Penetrations in the shield are designed in the same way as in the reflector with sleeves welded to the inner and outer surfaces.

Since the reflector is a stronger structure than the shield, it would seem prudent to have the supports for the central cell attached to it. This implies that the shield construction must also provide spaces through which the supports can be inserted and attached to the shield. Alternatively, the reflector can have extended stubs for attaching the supports and the shield geometry must accommodate them. Thus, when the shield is assembled on the reflector, the stubs will protrude sufficiently so that the supports can be easily attached.

At the interface between two reflector/shield modules, a removable shield must be provided to circumvent the expansion bellows attached to the reflector modules at that point. The shield must be removable to provide access to the bellows for maintenance, or in the event that a complete sector of the central cell must be extracted. This will only be needed if there is a failure in a central cell magnet, reflector, or shield. Such an eventuality is very rare and will be further discussed in Sec. 18.2 of this volume. Table 13-2 gives the pertinent masses in the central cell shield.

Table 13-2. Masses in the central cell shield.

Unit mass of central cell shield (t/m)	35.2
Mass per 6.23 m module ^a (t)	219.3
Mass per 5.08 m module ^b (t)	178.8
Total mass of central cell shield (t)	4585

^aMass of the 19 main central cell shield sections.

^bMass of the short (5.08 m) shield module at each end of the central cell.

13.4 NEUTRONICS

We have performed several one-dimensional neutronics calculations to determine the required shield composition and thickness for adequate protection of the superconducting magnets. The materials we evaluated for the water-cooled shield were layers of Fe-1422, boron carbide, and lead. The shield consists of three zones, as shown in Fig. 13-3. Shield A is used primarily to attenuate and moderate high energy neutrons that penetrate the blanket and reflector. This zone has the same composition as the reflector. Hence, as far as magnet shielding is concerned, the reflector zone can be considered as a part of the shield. The boron carbide used in shield B will

0			
44	Plasma		44
60	Void		16
79.2	Blanket I 73.7 vol % $Li_{17}Pb_{83}$, 7.1 vol % HT-9		19.2
80.0	Void		0.8
99	Blanket II 85 vol % $Li_{17}Pb_{83}$, 15 vol % HT-9		19.0
101	Void		2
	Reflector 95 vol % Fe 1422, 5 vol % H_2O		43
144			
154	Shield A 95 vol % Fe 1422, 5 vol % H_2O	10	41
	Shield B 86 vol % B_4C , 10 vol % Fe 1422, 4 vol % H_2O	23	
177	Shield C 90 vol % Pb, 5 vol % Fe 1422, 5 vol % H_2O	8	
185			
	Void		7
192			
	Dewar 304 SS		1
193			
	5 vol % Kapton		3
196			
	LiH_2 radiation shield (1/8-in. 304 SS)		1
197			
	5 vol % Kapton		7
204			
	Casing 304SS		5
209			
	Polyimide		1
210			
	Winding pack 35 vol % 304 SS, 15 vol % polyimide, 31.5 vol % Cu, 3.6 vol % NbTi, 15 vol % liq. He		117.3
327.3			
	Radius (cm)	Thickness (cm)	

Fig. 13-3. Geometrical model for central-cell magnet shielding calculations.

result in neutron thermalization by carbon and the subsequent absorption of thermal neutrons in boron, producing only a soft 0.5-MeV gamma and an easily absorbed alpha particle. The lead in shield C will effectively attenuate gamma-rays produced from (n, γ) reactions taking place in the steel structure of the blanket, reflector, and shield. The calculations were performed in cylindrical geometry using the one-dimensional multigroup discrete ordinates code ONEDANT. A P₃-S₈ approximation was used together with a coupled 46-neutron 21-gamma cross section library based on the VITAMIN-C and MACKLIB-IV-82 libraries.^{10,11}

The design for the central cell magnets allows for a 41-cm-thick shielding space. Shield optimization has been performed to determine the optimum shield configuration that results in the best use of this available space. Preliminary results showed that the limit on the peak power density in the magnet is the most severe constraint on the shield design. The optimum dimensions of the different shield layers are, therefore, those that minimize the peak magnet heat load.

Figure 13-4 shows the result of changing the thickness of the boron carbide shield at the expense of the Fe-1422 shield with the lead shield thickness kept constant. The results show that the lowest magnet heat load can be obtained when the smallest thickness of shield A is used. The reason is that the 43-cm-thick reflector will act as a Fe-1422 shield. For shield A we chose a thickness of 10 cm so that the overall energy multiplication is not affected by the strong neutron absorption in boron carbide. Fixing the thickness of shield A, the thickness of the lead shield was varied at the expense of the boron carbide shield. Figure 13-5 shows that the optimum shield design should have thicknesses of 23 and 8 cm for the boron carbide and lead shields, respectively. Since neutrons absorbed in the 5-cm-thick steel magnet casing will produce secondary gamma photons, a relatively large boron carbide shield is needed for neutron attenuation. Although the results in Figs. 13-4 and 13-5 are based on a wall loading of 5 MW/m², the resulting optimum shield configuration is independent of wall loading.

Fig. 13-4. Results of changing the thickness of the boron carbide shield at the expense of the Fe-1422 shield, keeping the lead shield a constant thickness.

Fig. 13-5. Optimum thicknesses of boron carbide and lead shields based on a wall loading of 5 MW/m².

The variation of the different magnet radiation effects with shield thickness is given in Fig. 13-6. The thickness of the Fe-1422 shield was kept constant while the thicknesses of the boron carbide and lead shields were changed so that they remain in the proper proportion determined by the optimization study. The results are normalized to a wall loading of 4.3 MW/m² at the central cell wall radius of 60 cm. Using the optimum shield configuration in the 41-cm available shield space results in the peak magnet radiation effects given in Table 13-3. The dpa rate of 3.9×10^{-6} dpa/FPY implies that no magnet annealing is needed during the whole reactor life. The other radiation effects are well below the design limits. The results in Fig. 13-6 indicate that all design criteria can still be met if the shield thickness is reduced by 8 cm. However, the entire 41 cm of available shield space are utilized in MARS to allow for data uncertainties and calculational model deficiencies.

A major geometrical perturbation that leads to hot spots in the central cell magnets is due to the 10-cm-deep notch in the reflector needed to provide space for the header flange during axial modular translation. Magnet hot spots at the top and bottom are obtained where the magnet is protected from the fusion neutrons by the 39-cm-thick Li-Pb manifold, 33-cm-thick reflector, and 41-cm-thick shield. We performed a one-dimensional calculation to determine the peak radiation effects at these hot spots. The results are given in Table 13-4. The peak copper dpa rate at the hot spot implies that magnet annealing is still not required. The other radiation effects are still much lower than the design limits except for the peak power density, which is marginally acceptable. However, this local peaking in nuclear heating results only in a small increase in the total cryogenic heat load. We conclude from

Fig. 13-6. Variation of magnet radiation effects with varying shield thickness. The Fe-1422 shield is kept constant; the boron carbide and lead shields are varied so they remain in the proper proportion (see Figs. 13-4 and 13-5).

Table 13-3. Radiation effects in the central cell magnet with a 41-cm-thick shield.

Peak power density in winding pack (mW/cm ³)	0.018
Peak dpa rate in copper stabilizer (dpa/FPY)	3.9×10^{-6}
Peak dose in thermal insulator at end of life (rad)	8.3×10^8
Peak dose in electrical insulator at end of life (rad)	4.0×10^8
Peak neutron fluence ($E > 0.1$ MeV) in NbTi at end of life (n/m ²)	1.0×10^{21}

Table 13-4. Radiation effects at central cell magnet hot spots.

Power density in winding pack (mW/cm ³)	0.063
dpa rate in copper stabilizer (dpa/FPY)	1.6×10^{-5}
Dose in thermal insulator at end of life (rad)	2.7×10^9
Dose in electrical insulator at end of life (rad)	1.4×10^9
Neutron fluence ($E > 0.1$ MeV) in NbTi at end of life (n/m ²)	4.0×10^{21}

these results that the optimized 41-cm-thick shield provides adequate magnet protection with a comfortable margin that allows for possible data uncertainties and calculational deficiencies.

13.5 THERMAL HYDRAULICS

The MARS shield generates 22 MW of thermal power and the maximum heating rate is only 0.1 W/cm². The shield is water-cooled by forced convection.

To simplify the system, the water is not pressurized. Therefore, we used a water temperature range of 50 to 80°C and a coolant flow rate of 6.3×10^5 kg/hr. The water pressure is 1 atm and the maximum shield temperature is 100°C. The total pressure drop of the water system 0.02 MPa, which requires a pumping power of only 3.5 kW.

13.6 ABSORBED DOSE

We have determined the absorbed radiation doses in the MARS reactor at several locations. We gave the criteria used for the dose in the beginning of this section. To determine the actual absorbed dose, we used an ANISN¹² 25-neutron, 21-gamma energy group calculation using the P3-S8 approximation. This provided the scalar flux in each interval. We then input this flux into the DKR¹³ code and a gamma source was produced. This gamma source was then transported by the ANISN code and a gamma flux was calculated. The gamma flux was multiplied by the appropriate KERMA factors to obtain the dose. Figure 13-7 shows the MARS blanket schematic used in these calculations.

Table 13-5 gives the doses that were present at certain locations (these locations are marked on Fig. 13-7). It can be seen that the shielding is adequate behind the magnets. The shielding is also adequate between the magnets (assuming that one is not too close to a magnet on either side). This location (location 1) meets the 100-mrem/working week criteria about 1.5 days after shutdown. A 15-cm "collar" has been added around the magnets (shown in the schematic) to reduce the radiation generated in shield C, in the magnet radiation shield, and passing through the magnet void region. This added collar as well as the 15 cm of shield outside the inner radius of the magnet is made of 75% Pb, 20% Al, and 5% H₂O. The aluminum is included for structural support. If iron was used instead of aluminum, calculations show that the activity in location 1 would initially be lower (about 5 mrem/hour 1 day after shutdown), but would not decrease enough to meet the 100-mrem/work week or 2.5-mrem/hour criteria within 1 to 2 days or even a week (the dose after a week was about 3.9 mrem/hour).

The calculations that were performed only considered objects within the outer radius of the magnets. This neglects the material outside this region that could also be activated and that could also contribute to the dose. This might increase the amount of waiting time before the dose criteria could be met.

Table 13-5. Radiation doses at three reactor locations at various times after shutdown.

Location	1 day (mrem/hour)	3 days (mrem/hour)	1 week (mrem/hour)
1--outside the shield	14.04	1.96	0.551
2--outside the magnet	0.24	0.235	0.23
3--at shield level of magnet	5.7 ^a	2.7 ^a	2.00 ^a

^aIn rem/hour.

Fig. 13-7. Radial sections through the central cell shield and magnet.

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14 Central Cell Structure

14. Central Cell Structure

The primary elements of the central cell consist of a radiation shield enclosing a water-cooled reflector and power blankets, through which Li-Pb is circulated for cooling and heat transport, and superconducting magnets that are used to confine the reactor plasma. The central cell structure is that structure necessary to support the shield and magnets (which are described in Secs. 3.2 and 13 of this volume). The reactor shield/reflector is segmented into 21 parts each 6.32 m long. These segments and their supporting structure are defined as the central cell modules, shown in Fig. 14-1.

Fig. 14-1. The central cell is assembled with modules.

14.1 DESIGN REQUIREMENTS

14.1.1 Loading Conditions

The loads that the basic structure must carry are derived from earthquake inertial conditions and magnet attraction forces. The module weight and inertial load factor for the design earthquake are summarized in Fig. 14-2. We have combined these to provide the design loads for the module's supporting structure.

1. Earthquake loading condition

0.75g inertial acting singularly along the reactor's three axis.

Fig. 14-2. Central cell design loads are derived from earthquake conditions.

The magnet attraction forces that determine the design of the magnet casing and innercoil column structure are summarized in Fig. 14-3. The maximum load occurs for the "all coils energized" case. Although this peak load occurs only at central cell magnet No. 21, the loads experienced by most of the remaining magnets (i.e., 1 through 19) for other design conditions are not low enough to warrant a separate column structure design. For this reason we have selected the critical 196.6-MN force as the design load. This will allow the central cell magnets to be interchangeable.

Magnetic loading condition — MN (1 MN = 225,000 lb)

Fig. 14-3. Central cell magnet positions are maintained under varying conditions of field strength.

14.1.2 Thermal Deformations

As shown in Table 14-1, the central cell structure must accommodate thermal expansions that, unless provided with expansion joints, could exceed 40 cm of axial extension. At the same time, a total axial contraction of 31 cm could be expected of the central cell magnets.

To accommodate magnet removal requirements (see Sec. 14.1.3) expansion joints have been located between each module length of 629 cm normal

Table 14-1. The central cell design accommodates thermal deformations.

	<u>Effectuated normal geometry</u>		Operating temp	ΔT from normal (20°C)	Exp/contr per geometry (cm)	Accum growth assuming no flex joints (cm)
	Axial length (cm)	Radius (cm)				
Blanket						
Longitudinal	179.43		550°C	530°C	1.138 exp	
Radial		99			0.653 exp	
Reflector						
Longitudinal	632		300°C	280°C	2.12 exp	44.5 exp
Radial		100			0.336 exp	
Shield						
Longitudinal	632		140°C	220°C	1.67 exp	35.04 exp
Radial		143			0.378 exp	
Magnet						
Pack + casing						
Longitudinal (cu)	46.8		-4 K	289 K	0.225 contr	11.2 contr
Radial (cu)		210			1.008 contr	
Vacuum vessel						
Longitudinal	63.7			0	0	
Radial		193			0	
Polyimide compression	189		178 K (assumed)	100 K	0.47	19.8 contr

temperature length. As shown in Table 14-1, when at operating temperature, the module was expanded to 6.32 m. The deflection requirements for these as well as other critical sections are also given in the table.

14.1.3 Maintenance Requirements

The key maintenance requirements affecting the central cell structure are summarized in Table 14-2. The removal of a central cell solenoid, although rather infrequent, will require segmenting the reactor into modules that can be separated from the reactor to allow removal or replacement of a magnet. As we have stated, we have selected a module operating length of 6.32 m. Each module supports two magnets as well as the shield, and is joined to adjacent modules by vacuum-sealed expansion joints.

Table 14-2. The central cell structural design accommodates both hands-on and remote maintenance requirements.

-
1. Central cell magnets to be removed from reactor only in the unlikely event of a failure
 2. Blankets to be replaced every 3 to 4 years
 3. Biological entry to reactor restricted for 24 hours after reactor shutdown
 4. Accessibility to all Li-Pb piping and joints
 5. Use hands-on maintenance when warranted by safety conditions
-

Personnel will be able to enter the reactor building 24 hours after the reactor has been shut down in order to perform considerable maintenance on components outside of the reactor shield. Hands-on maintenance, therefore, will be used for all service line fittings (i.e., water, nitrogen, helium, and electrical) and Li-Pb reactor external connections, and robotic operations will be necessary for such needs as vacuum seal separating and joining.

14.2 MAGNET-INTERCOIL STRUCTURAL-COLUMN DESIGN ALTERNATIVES

We analyzed three central-cell magnet intercoil/support concepts designed to react magnetic forces and compared them to determine which could most easily satisfy our structural requirements and accommodate thermal growth and special maintenance requirements. A summary of this comparison is given in Fig. 14-4.

The tension/compression concept uses pin-ended struts to transfer intercoil loads. The load paths are well-defined because only one end of the reactor strut chain is attached to a rigid structure (i.e., the end cell). Because high tension loads are required at times to be transmitted through the coil, increased bridging structure is required around the winding pack. In

Fig. 14-4. Summary of magnet intercoil strut trade-offs.

addition, this bridging structure must mount pin attachment fittings for the intercoil column structure that must, at the same time, be enclosed in a vacuum to minimize heat flow into the magnet. To minimize the effects of a vacuum leak, each magnet vacuum vessel must be independent of all other magnet vacuum vessels. To provide a pin-ended easily accessible disconnect in the intercoil column structure to accommodate magnet removal, we shortened the column structure in the vacuum vessel. This will significantly increase the heat flow to the magnet. Just as important is the fact that the large tension ties required to secure the choke coil must be disengaged each time the choke coil insert is to be removed (approximately 3.2 years). Because this is a manual operation, the time for removal and replacement of the choke coil will significantly decrease the reactor availability.

By putting all of the interconnect structure in compression, loads can be carried through the winding pack structural case, eliminating the need for any large pack bridging structure and pin joints required for tension loads. This approach allows almost entire interconnect structure to be enclosed in a vacuum and, using compression blocks of glass-reinforced polyimide, will restrict the heat load to the magnet. Preloading all of the structure in compression requires a jack. To accommodate the magnet axial thermal deflections the jack stroke must be adjustable to maintain the preload. The preload need only be continuously applied and be large enough to ensure contact of all compression members.

The magnet attraction loads are very large. They are carried by the very long column (approximately 140 m) formed by the 42 sets of intercoil struts. Stability of this column is provided by the central cell magnets, which are not free to translate laterally. Their large weight restricts vertical translation.

The potential long column effects could be reduced if each magnet is supported with fittings attached to the reactor shield/reflector assembly. Although this approach eliminates the transfer of accumulated loads between magnets (each magnet will carry its own load and will not transfer it to the next magnet as a reaction to its own load), it requires either bridging the structure between modules or securing each module structure to the floor. Most detrimental, however, would be the relatively short heat paths through the fitting to the superconductor. Although these heat paths can be intercepted at pin joints with liquid nitrogen, the large relative mass of the fittings would make heat intercept difficult.

For the baseline design, we have elected to carry all magnet loads through compression blocks.

14.3 SELECTED STRUCTURE DESIGN

14.3.1 General Arrangement

As we stated in Sec. 14.1, the reactor's central cell comprises 21 6.32-m-long modules, linearly arranged and configured in a manner to facilitate scheduled and unscheduled maintenance. The modules are configured in two designs, each of which support two solenoid magnets: a basic module that supplies Li-Pb piping for four blankets, shown in Fig. 14-5, and a service module that not only provides Li-Pb piping for three blankets, but also supports the service stack through which the blankets are extracted for replacement.

View A-A

Fig. 14-5. The central cell module is configured to facilitate scheduled and unscheduled maintenance.

The shield and reflector that support the blankets in both designs are in turn supported by a structural box positioned between the two magnets. This assembly is secured to the module support platform, which in turn is either secured to the floor for normal operation or can be moved by small railroad-type transporters.

The magnet weight bears directly against the platform through the magnet support structure and a contact pad. The magnets also interface with each other through four compression blocks (called intercoil struts) to transfer attraction loads developed when the magnetic fields interact.

Each module is interfaced with an expansion joint, which also ensures vacuum continuity, and an interface shield.

14.3.2 Shield Support Structure

The shield is supported by the box structure shown in Fig. 14-6. This structure encloses, but provides access to, two Li-Pb exit pipes and is attached to the support platform. The platform provides the flooring for supporting the magnets as well as transferring all inertial loads to the ground. The platform floor is a plate supported by structural beams, which in turn are supported by three box support legs, the two outer ones containing tie-down fittings. The box legs are spaced sufficiently to allow access for two trains of transporters for module removal. Integral to and extending within the plate floor are four rails that provide a track for magnet axial movement and react with any magnet vertical loads resulting from earthquake lateral motion. These loads are carried in bearing and shear through the structure into the ground.

Platform/ground attachment is through threaded structure secured to the ground as shown in Fig. 14-7. The cylindrical-attachment tension-tie structure also incorporates an adjustable bearing plate that secures the platform to the ground. The tension tie is vertically aligned to the ground-threaded structure by lateral displacement of the tension tie within the confines of the bearing structure.

14.3.3 Magnet Support Structure

Magnet weight is supported, as shown in Fig. 14-8, by two polyimide-compression column-type structures encased within the magnet vacuum. These columns are joined by a lateral support structure, also of polyimide material, and are designed to transfer lateral inertial loads. All columns are pinned to accommodate thermal deflections and contain liquid nitrogen inserts to inhibit heat conduction to the magnet cryo case.

The structural columns are attached to contact pads that can accommodate rollers and a track guide (not shown in Fig. 14-8). The track guide is used to transfer the magnets' vertical loads to the platform while the tracks guide the magnets' axial thermal deflection. Rollers are used for magnet handling.

14.3.4 Magnetic Interconnect Structure

The magnet intercoil strut structure is designed to react magnet attraction forces. The structure is a column of glass-reinforced polyimide material supported as shown in Fig. 14-9 by the vacuum structure and magnet case fittings. Liquid nitrogen intercepts are located near the flat face to inhibit heat conduction to the coil.

Fig. 14-6. The central cell structure has been designed to accommodate earthquakes.

Fig. 14-7. The module tie-down fittings are easily secured and released.

Fig. 14-8. The magnet support structure is designed to inhibit conduction heat flow into the magnet cryo case.

Fig. 14-9. The intercoil struts maintain the axial positions of the central cell magnets during reactor operation.

The flat face is secured to the vacuum vessel by a diaphragm-type attachment that permits axial deformations during cooldown and yet retains the magnet's vacuum. Adjustable shims fill the gaps between the columns that result from cooldown to permit the transfer of compression loads without moving the magnet. Differential thermal radial contractions are accommodated by the column ball joint.

The liquid nitrogen intercepts will limit the polyimide upper temperature to 70 K. At this temperature the allowable compression yield is 276 MPa. At the design load of 1.67×10^6 N, therefore, the column has a margin of safety of 1.08.

14.3.5 Module/Module Interface

The central cell modules are joined with a circular, welded vacuum seal, (shown in Fig. 14-10) that can deflect to accommodate thermal expansions. With the shield removed, the seal can be cut and welded with remotely operated equipment. Water jackets are located in the reflector near each seal attachment for cooling.

Two 180 deg segment shields are supported by the module shields over the seals to restrict radiation streaming through the interface gap. The shield is water-cooled Fe-1422 steel.

Fig. 14-10. Removable shields are used to restrict radiation streaming from module/module interfaces.

14.3.6 Transportation

All reactor modules and end cell magnets are moved by the transporters shown in Fig. 14-11. These transporters, capable of 250 tons of lift, are currently used by General Dynamics Electric Boat Division to move submarine components, and by Kawasaki Heavy Industries in Japan to move ship components.

Current design

Capacity — 250 tons

Drive — Electric (2 ft/min)

Lift — Hydraulic ($\frac{1}{4}$ in./min)

Direction orientation (90-deg rotation—manual)

Cost — \$75 k (1980 dollars—60 units)

Modification required

Automatic direction orientation

Fig. 14-11. The movement of large masses (>10,000 tons) by transporters in normal operations has been demonstrated by industry.

Manufactured by Western Gear at a 1980 cost of \$75,000, the transporters are remotely controlled and are capable of speeds up to 0.6 m/min and a 90-deg change in direction. When modified, the speed can increase to 6 m/min. The orientation of the transporter to allow a 90-deg change in direction is accomplished remotely. The four to six units required for each module can be simultaneously operated in all movements.

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Heat Transport and Energy Conversion

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15. Heat Transport and Energy Conversion

15.1 DESIGN REQUIREMENTS

Thermal energy is deposited in the blanket modules and reflector of the MARS central cell, and also is deposited on the direct converter and halo scraper in the end cells. The heat transport system removes the thermal energy at a high temperature for a high thermal conversion efficiency.

MARS blanket modules are cooled with the lithium-lead eutectic (Li-Pb). The Li-Pb coolant is also the breeding material for tritium generation in the MARS reactor. These two roles of heat transport and tritium generation place dual requirements on the heat-transport system. It must be both a low-loss heat-transport system and must provide for the transport of tritium with little or no loss to the environment.

The heat generated in the reflector, direct converter, and halo scraper is removed with pressurized water. This water is maintained at a high enough temperature to provide for a high-efficiency thermal-electric conversion system. As with the Li-Pb system, the water systems must provide for tritium isolation from the steam cycle and prevent or minimize tritium leakage to the environment.

Because Li-Pb has a melting temperature of 235°C , those sections of the heat-transport system containing Li-Pb must have a preheat capability above that temperature before the admission of Li-Pb to the system. In addition, the system must be capable of maintaining the Li-Pb temperature above its melting point during short-term reactor shutdown and also be capable of being totally drained for long-term reactor shutdowns to prevent the Li-Pb from freezing.

15.2 DESIGN ALTERNATIVES

We have defined a system that cools the blanket, reflector, and direct converter/halo scraper combination at the highest temperatures reasonably achievable to maximize overall energy conversion efficiency. Because more than one working fluid is used and because the pressures and temperatures are different, several options for the energy conversion system are possible. In addition to possible cycle differences to improve thermal conversion efficiency, the need for tritium extraction and isolation also has a significant effect on cycle configuration.

Both the Li-Pb loops and the water-cooling loops have some tritium inventory. The Li-Pb loop has this inventory by design because the Li-Pb is the breeding material for tritium within the reactor. Isolation of the tritium from the secondary, power-producing loops in both the Li-Pb cycle and the high-pressure water cycle is of key importance to minimize those plant areas that must have tritium atmospheric release control. Removal of the tritium content to a sufficiently low level from the primary loop has been discounted as an uneconomic means of controlling vulnerable tritium. Therefore, we are left with some form of isolation within a single device such as double-wall-tube steam generators, or the inclusion of an intermediate loop using either the same or a separate fluid to recover tritium that may pass through the heat exchangers.

The presence of two cooling fluids (Li-Pb at a temperature of 500°C and water at 320°C) provides several options for the power cycle. In part, the high steam temperatures and pressures attainable (for high efficiency) with

the 500°C Li-Pb cause the total steam flow to exceed the capacity of currently available steam-turbine generators. This is true even with the use of six-flow low-pressure turbines. Thus, to handle the total expected output of MARS, at least two steam-turbine generators are required using today's technology. This approach has been used in the MARS design. A cost savings could be achieved with a more advanced, single large turbine.

In our first option, we consider the use of two identical-capacity steam-turbine cycles, using Li-Pb as the heat source for a superheated single-reheat-nonextraction steam cycle. In this case, the cooling water from the reflector and the end cells is used for feedwater heating. The nonextraction steam cycle concept pushes the limit of mass flow through the low-pressure turbines with current steam-turbine technology. The chief advantage of this cycle is the use of two identical-superheated-nonextraction steam-turbine cycles resulting in common system operation and duplicate components.

Our second major option considers the use of Li-Pb as the heat source for a conventional superheated single-reheat-extraction steam-turbine cycle. By using extraction steam for feedwater heating, the low-pressure mass flow is reduced to the point where it is possible to produce 900 megawatts on a single shaft using a six-flow low-pressure machine. In this scheme, the high-pressure 320°C cooling water is used to drive a saturated steam cycle. The principal disadvantage of this approach is the presence of two separate steam-power cycles: (1) a 3600-rpm superheated steam cycle, and (2) an 1800-rpm saturated steam cycle. Another disadvantage is that a saturated steam cycle presents a greater design challenge and is usually less reliable than a superheated cycle. The principal advantage is an increase in efficiency of the overall thermal conversion cycle, therefore, producing an additional 64 megawatts net electric power.

15.3 CONFIGURATION SELECTION

Compared with a conventional intermediate loop system, the double-wall-tube heat exchanger exhibits lower initial cost, lower operating cost, and less temperature loss. Therefore, in view of its anticipated low tritium permeation characteristics (see Sec. 16.4), we selected the double-wall configuration for MARS.

To maintain the higher overall cycle efficiency, we decided to use a superheated extraction steam cycle with two primary heat sources: (1) the Li-Pb blanket coolant, and (2) a saturated steam cycle, using the cooling water from the reflector and the end cells as its heat source.

Some heat is deposited in the central cell and end-cell coil shields, but its recovery is uneconomical because of the complexity of designing a shield to contain water at a sufficient pressure to allow for high-temperature operation, the insufficient heat quantity available, and the expected low thermal conversion efficiency.

15.4 POWER-CYCLE DESIGN AND PERFORMANCE

15.4.1 Primary Blanket Loop

The MARS blanket is cooled with the Li-Pb eutectic. As noted above, we chose a superheated steam cycle with a single reheat for heat transport in energy conversion from the MARS blanket. The linear nature of the MARS

reactor (the central cell is 131 meters in length) and the weight of the Li-Pb coolant dictate certain design features of the loop.

Essentially, two separate Li-Pb loops are provided, each cooling one-half of the MARS blanket modules. As will be discussed in Sec. 22.4 of this volume, the steam sides of these two Li-Pb loops are combined to feed the single steam-turbine generator. At the exit from the blanket module, a common header of 76-cm diameter pipe is provided. This header is connected to the suction of the five Li-Pb circulating pumps (one is a spare) through the 76-cm-diameter piping.

A common discharge header is provided and the Li-Pb flow is divided into four parallel loops. Two of these loops provide thermal energy to the superheat steam cycle passing through superheaters, steam generators, and the economizers. The other two paths lead through parallel reheaters for the steam cycle. A slip stream is taken from a common header to the cold trap for the corrosion removal and tritium removal system. Return pumps or booster pumps are required to reintroduce the Li-Pb passing through the tritium removal system back into the loop. The Li-Pb is then returned to another common header that supplies the 36-blanket modules in one-half of the reactor. Figure 15-1 is a schematic diagram of the Li-Pb loop used for the MARS reactor.

Fig. 15-1. Simplified Li-Pb flow diagram for MARS.

The major primary loop design and performance parameters for MARS are shown in Table 15-1. The Li-Pb leaves the blanket modules at a temperature of 500°C and is returned to the blanket modules at a temperature of 350°C. The 150°C temperature rise through the blanket provides for efficient heat transfer in the steam generator cycle and is consistent with keeping the Li-Pb mass flow to a minimum. Nonetheless, the total Li-Pb mass flow is 91,000 kilograms-per-second. The Li-Pb velocity through the system should be kept below 2 m/s to minimize pipe wall erosion. A design value of 1.4 m/s is used here.

Because the Li-Pb must be maintained above its melting point to prevent freeze-up in piping and/or steam generators, the system can be drained during reactor shutdown periods. To accomplish this, two large Li-Pb storage tanks are provided with drains from all low points in the system. The total inventory of the Li-Pb in the heat transport system is 14,610 tonnes and each tank has the capability of storing over half of this inventory. These tanks are trace heated to maintain them above the melting point of the Li-Pb. Induction heating is used for initial Li-Pb melting in the tanks. System fill-up pumps are provided to fill the system prior to reactor startup.

All system piping, heat exchanger shells, and pump casings outside the blanket are trace heated to raise them to a temperature above the melting point of Li-Pb before introduction of Li-Pb into the system. An auxiliary boiler is provided to supply heat to the tube side of the heat exchangers before admission of the Li-Pb.

Although plant startup will be discussed in Sec. 23 of this volume, "Plant Operation," it is important to note that the admission of Li-Pb into the primary coolant loop will immediately produce steam in the steam generator cycle, if water is present. Steam bypass is provided during this period of initial water circulation until sufficient thermal input is generated by the reactor to preheat and then power the steam turbine generator.

Table 15-1. Primary blanket-loop parameters for MARS.

Coolant	Li-Pb
Heat input (MWt)	2,110
Blanket outlet temperature (°C)	500
Blanket inlet temperature (°C)	350
Operating pressure (MPa)	2.5
Number of loops	2
Parallel paths per loop:	
Superheat	2
Reheat	2
Number of pumps per loop	3 (1 spare)
Normal pipe size (mm)	762
Coolant velocity in pipe (m/s)	1.4
Coolant mass flow (kg/s)	91,000
Total coolant mass (kg)	13.15×10^6
Pumping power (MWe)	23

15.4.2 Primary Loop Components

All primary components for the heat transport system are located in the steam generator building. As noted earlier, the density of Li-Pb has a significant impact on the sizing of components. With the exception of a few common headers, all piping is kept at or below 76 cm nominal diameter for seismic design considerations. Even at this diameter, the weight of the Li-Pb piping exceeds 4000 kilograms-per-linear-meter. Fortunately, the internal working pressure is in the neighborhood of 2.5 MPa, allowing the use of rather thin-walled pipe and somewhat alleviating the pipe-support problem.

Section 15.4.5 presents a summary of the major system components. However, the double-wall-tube steam generators chosen for the MARS reactor deserve some special discussion. These steam generators are based on the double-wall-tube design developed by Westinghouse for the liquid-metal fast-breeder reactor. All Li-Pb heat exchangers are straight-tube type with Li-Pb on the shell side and water and/or steam on the tube side. Figure 15-2 shows a cross section of one double-wall tube for the steam generator and the tubesheet welds. The gap between the inner tube and the outer tube will be exposed to 20 parts-per-million moisture in a helium atmosphere at approximately 700°C during manufacture. This process causes the buildup of a thin layer of oxide. This oxide layer provides a tritium barrier within the double-wall tube that resists permeation of the tritium from the shell side to the tube side. The gap between the inner and outer tubes contains a helium purge gas, which provides a means of leak detection. In addition, the helium is doped with ~ 1 Torr of oxygen so that diffusing tritium is converted to the oxide form. Further details can be found in Sec. 16.4 of this volume.

All piping, steam generators, and pump casings are thermally insulated and have a continuous aluminum skin that acts as an additional tritium barrier.

15.4.3 Primary Reflector and Direct Converter/Halo Loop

Charged particles incident on the direct converter and halo dumps, plus neutrons and gamma deposited in the reflector provide 1008 megawatts of usable thermal energy within the MARS reactor. Therefore, an efficient energy transport and thermal conversion system is defined using a saturated steam-energy conversion cycle. The reflector thermal power is distributed uniformly along the 131-meter length of the MARS central cell, whereas the thermal energy, approximately 300 megawatts, coming from the direct converter is split equally between two locations that are approximately 210 meters apart. Both the reflector and direct converter are cooled by high-pressure water, which exits at 320°C. This cooling water (heat source) is transported to a common header within the steam generator building that also houses the Li-Pb steam generators, as discussed above. The coolant flow of 4470 kilograms-per-second is piped through four identical steam-generator, reheater and moisture-separator combinations, consistent with pressurized water-reactor technology within the United States. A slip stream off the return header is used for the tritium-removal system, water filtration, and makeup.

Table 15-2 lists the major performance parameters of the energy conversion system for the direct converter/reflector. Outlet temperature from both the direct converter and reflector is 320°C, with return to those components at 280°C. Because there will be tritium in the cooling water from the direct converter/halo scraper, the double-wall-tube heat-exchanger design as previously discussed is used.

Major system components are outlined in Sec. 15.4.5. These major system components are connected to common headers. The heat exchangers are slightly

Tube-to-tubesheet welds
(lower tubesheet)

Tube-to-tubesheet welds
(upper Li-Pb and steam tubesheet)

Fig. 15-2. Cross section of double-wall tube and tube-to-tubesheet welds for MARS.

Table 15-2. Primary direct-converter/reflector-loop parameters.

Coolant	Water
Heat input (MWt)	1,008
Outlet temperature (°C)	320
Inlet temperature (°C)	280
Number of parallel loops	4
Coolant mass flow (kg/s)	4470

oversized for their normal heat-transfer capacity to provide for significant partial-load operation should a steam-generator/reheater combination be out of service for repair.

15.4.4 Residual Heat-Removal Loop

The afterheat generation within the reactor blanket after reactor shutdown or in the event of loss-of-flow/loss-of-coolant accidents is not sufficient to cause meltdown of the blanket (see Sec. 12.13 of this volume). Therefore, a residual removal loop, operating independently of the primary coolant loops, is not required for safe reactor operation. MARS has four independent coolant loops: (1) the Li-Pb primary-blanket loop; (2) the water-cooled reflector and direct-converter loop, (3), the shield-water loop; and (4) the helium preheat loop, providing redundancy for residual heat removal. Through common headering of the Li-Pb loop, blanket cooling can be maintained at the required capacity in the event of a pump failure. A steam bypass to the condenser has been provided as a means of heat rejection for the residual heat removal system.

15.4.5 Major Components Summary

The major components of the primary blanket-coolant loop are listed in Table 15-3, and depicted in Fig. 15-3.

The major component parameters of the reflector, direct converter/halo scraper loop are described in Table 15-4 and shown in Fig. 15-4.

Table 15-3. Major components of the primary blanket-coolant loop for MARS.

Equipment	Major parameters
Circulating pumps	Ten pumps including two standbys, vertical, can type mixed flow, motor driven; each 10,514 kg/s capacity
Return pumps (from tritium removal)	Four pumps including two standbys, vertical, can type mixed flow, motor driven; each 12,500 kg/s capacity
Piping	ASTM A335 GR-P22, estimated design pressure 2.5 MPa gauge and design temperature 525°C
Economizers, steam generators, superheaters, reheaters No. 1, reheaters No. 2	<p>Each piece of equipment listed includes four shell and double tube heat exchangers, Li-Pb on shell side, water/steam on tube side and helium is the intermediate fluid</p> <p>A) Each economizer sized for 90 MWth and 35.8°C log mean temperature difference (LMTD).</p> <p>B) Each steam generator sized for 178 MWth and 50°C LMTD</p> <p>C) Each superheater sized for 153 MWth and 44.5°C LMTD</p> <p>D) Each reheater No. 1 sized for 57 MWth and 49.7°C LMTD</p> <p>E) Each reheater No. 2 sized for 49 MWth and 28.5°C LMTD</p>
Tanks	Two drain/melting tanks, sized to contain all the Li-Pb normally in the system, estimated capacity each 7,800 tonnes, 10 m diameter, 10.5 m high. Material of construction is 2-1/4 Cr, 1 Mo steel

Table 15-4. Major component parameters of the reflector, direct-converter/halo-scraper loop for MARS.

Equipment	Major parameters
Pumps	Four half capacity, centrifugal, motor driven, each 1620 kg/s capacity for the reflector. Four half capacity, centrifugal, motor driven, each 620 kg/s capacity for the direct converter/halo scraper
Piping	Stainless steel type 304, estimated design pressure 20 MPa gauge, and design temperature 340°C
Economizers and steam generators	Each piece of equipment listed includes four shell and tube heat exchangers, cooling water on the tube side, turbine cycle water/steam on the shell side, and helium is the intermediate fluid. These pieces of equipment serve the combined cooling load of the direct converters and the reflectors a) Each economizer is sized for 40 MWth and 24.6°C LMTD b) Each steam generator is sized for 214 MWth and 19.3°C LMTD

Fig. 15-4. Mirror advanced reactor reflector and direct converter cooling-water flow diagram for heat transport in unit No. 2.

15.5 RESEARCH AND DEVELOPMENT REQUIREMENTS

The heat transport system for MARS is based on present day, commercially available components. Therefore, the research and development requirements for this system are minimal. However, one key area that should result in a significant research effort is the performance of the Westinghouse designed double-wall-tube heat exchanger from the standpoint of tritium isolation and reliability. The performance of the oxide layer between the inner and outer tubes of that heat exchanger should be tested. The performance of the helium tracing between the tube has already been demonstrated as a means of leak detection.

Additional research is needed for the development of suitable electromechanical or, alternatively, electromagnetic pumps for the pumping of Li-Pb. These studies should emphasize the long-term corrosion aspects for the electromechanical pump of Li-Pb on pump impellers. The nature of impurities generated and an economical means for their removal from the system should also be the subject of research and development.

Research on the structural and heat transport characteristics of heavy-metal heat exchangers should be reinitiated. A great deal of work was done when heavy metals were considered for application to thermal nuclear (fission) reactors. This work needs to be updated to arrive at optimal component size, design, and configuration.

Finally an effort should be initiated to develop "hard" seals for valve stems and pump shafts.

16 Tritium Systems

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16. Tritium Systems

16.1 INTRODUCTION

The tritium fuel systems perform the following functions: prepare high-purity DT fuel for injection into the plasma; collect and purify the reactor exhaust; remove and purify the tritium produced in the reactor breeder blanket; and store excess fuel for future use. All of these functions must be performed with minimal tritium escape because of the high cost of tritium and the undesirable environmental effects of such a release.

Specialized components are used to perform the functions of the total tritium fuel system. The function of each of these components is listed in Table 16-1, and the components are described in subsequent sections. Figure 16-1 is a flow diagram of the process. It shows that approximately 14% of the tritium injected into the reactor is consumed per unit time. As a result, recycling of tritium is a major concern. The quantities of fuel (in g/day) that must be handled by each of the major process components are shown in greater detail in Fig. 16-2 and their steady-state tritium inventories are listed in Sec. 16.3.

The tritium recovery system (TRS) for removing tritium from the Li-Pb alloy breeder is described in Sec. 16.2. All of the components of the fuel processing system are described in Sec. 16.3.

Specialized control measures must be used to regulate tritium loss from the contaminated heat transfer fluids--either liquid $\text{Li}_{17}\text{Pb}_{83}$ or water--in the steam generator systems. In these systems only one containment structure, the heat exchanger tubing, separates the tritium from easy pathways to the environment. The control measures that we have adopted are the use of tritium permeation barriers on the double-walled, actively purged Li-Pb steam generator as described in Sec. 16.4. A tritium water recovery unit designed to maintain the tritiated water concentration at an acceptable level of 1 Ci/liter is described in Sec. 16.5.

Leakage of tritium from process equipment is controlled by the use of secondary containment structures. Tritium that escapes the secondary containment is confined within the reactor buildings. The tritium concentration in the building atmosphere is maintained at a low level by the use of atmospheric detritiation systems, as described in Sec. 16.6. Accidental tritium releases are considered and their consequences are assessed in Sec. 19 of this volume.

We have designed a tritium process building to house all the tritium process equipment not required in the main reactor building. This building, described in Sec. 23 of this volume, will have a high-integrity structural shell that will significantly reduce the likelihood of external forces causing the release of tritium from the process equipment.

Table 16-1. Function of each component of the tritium systems.

<u>Fuel processing systems</u>	
<u>Component</u>	<u>Function</u>
Fuel delivery system	Pellet injector
Plasma exhaust	Mechanical vacuum pumps
Fuel purification (chemical)	Fuel cleanup unit
Fuel purification (isotopic)	Isotope separation system
Fuel composition adjustment	Fuel blending station
Fuel storage (interim and long term)	Fuel storage vault
<u>Tritium breeder systems</u>	
Tritium breeder	Liquid $\text{Li}_{17}\text{Pb}_{83}$ breeder/coolant
Tritium removal from breeder	Tritium recovery system
<u>Tritium disposal systems</u>	
Decontamination of process water	Tritium water removal unit
Decontamination of airborne tritium	Atmospheric detritiation systems
Decontamination of gaseous wastes	Tritium waste treatment systems

Fig. 16-1. MARS tritium fuel system.

Fig. 16-2. Fuel and exhaust flow for total system (g/day).

16.2 TRITIUM RECOVERY SYSTEM

16.2.1 Introduction

Several methods for recovering tritium from a liquid Li-Pb breeder have been proposed in previous reactor designs, including extraction by molten salts,¹⁻³ gas sparging, and vacuum degassing.^{1,2,4} Of the various vacuum degassing techniques available--namely pumping on an agitated pool, falling liquid metal films, and droplet sprays--we chose the latter technique for the MARS TRS.

Presently there are no experimental data for the vacuum removal of tritium from Li-Pb alloys and data are also lacking for many of the transport calculations. In the absence of such information, we have modeled the TRS after processes used in the steel industry for removing hydrogen from liquid steel.^{5,6}

16.2.1.1 Baseline Parameters for the TRS. The choice of which vacuum degassing system to use requires the establishment of a set of baseline parameters for the TRS. The selection of these parameters involves a tradeoff between reasonable operating conditions for recovery of tritium and the constraints set by tritium containment requirements. Thus, the selection of an appropriate operational tritium partial pressure involves a compromise between a pressure high enough for tritium recovery and yet low enough to minimize losses due to permeation through metal vessels and piping.

The TRS baseline parameters must be based on certain fixed parameters: a liquid Li-Pb flow rate of 8.8×10^4 kg/s, a tritium removal rate of 7.7×10^{-4} moles- T_2 /s, and a temperature of 500°C. At this temperature the Sievert's constant K_s for the concentration of tritium in the liquid $Li_{17}Pb_{83}$ was preliminarily given⁷ as 5.1×10^{-2} wppm/Torr^{1/2}, which we have used. Recent experimental data⁸ indicate K_s may be as small as 2×10^{-2} wppm/Torr^{1/2}.

To determine the operational specifications for the TRS it is necessary to establish the limits for the partial pressure of tritium in the liquid alloy. The quantity $\dot{m}$ of breeder/coolant per second that must be diverted to the TRS to remove tritium at the rate at which it is generated in the blanket, $\dot{g}$, is given by the relationship

$$\dot{g} = \epsilon (C_1 - C_2) \times \dot{m} \quad , \quad (16-1)$$

where C_1 and C_2 are the solubility of tritium in the alloy at pressures P_1 and P_2 , respectively, and ϵ = the efficiency of the extraction process. For an efficiency of 100% and C_2 equal to 10% of C_1 , we have calculated the value of $\dot{m}/\dot{v}$, where $\dot{v}$ is the total coolant flow rate, for values of the tritium partial pressure in the coolant ranging for 10^{-2} to 10^{-6} Torr. Table 16-2 indicates that a tritium partial pressure as low as 10^{-6} Torr is not achievable because 100% of the coolant would have to be processed in the TRS. For T_2 partial pressures of 10^{-2} and 10^{-4} Torr, the sidestream fractions required for T_2 processing are 1.0 and 10.3%, respectively. On the other hand, the tritium permeation rates in the steam generator (shown in Table 16-2) are high at both of these tritium pressures. Discussions in Sec. 16.4 indicate that fabrication of the double-walled heat exchanger may be possible with barriers⁹ for tritium permeation in the resistance range of 10^3 as compared to bare metal tubes. Several of the barriers in series could provide up to 10^4 resistance to tritium permeation. Such barriers at a tritium pressure of 10^{-4} Torr would reduce the tritium permeation to the desirable range of ~ 10 Ci/day into the steam generator water. At a tritium partial pressure of 10^{-2} Torr in the coolant, 100 Ci/day would permeate into the steam generator, which would be considered unacceptable; therefore, a tritium partial pressure in the coolant in the range of 10^{-4} Torr appears reasonable. At this pressure the concentration is 5.1×10^{-4} wppm of tritium in Li-Pb.

Table 16-2. Considerations for establishing the tritium pressure in the Li-Pb coolant.

Event	Tritium pressure (Torr)		
	10^{-2}	10^{-4}	10^{-6}
Coolant flow diverted to TRS (%)	1.0	10.3	100
Tritium barrier required in steam generator for permeation, <10 Ci/d	2×10^5	2×10^4	2×10^3

With the tritium partial pressure in the coolant established, Eq. (16-1) can be used to establish a relationship between the amount of tritium removed ($C_1 - C_2$) at 100% efficiency and m/v (the fraction of Li-Pb diverted to the TRS). The results (Fig. 16-3) indicate that the amount of coolant diverted decreases rapidly as the fraction of tritium removed, $\alpha = [(C_1 - C_2)/C_1]$, increases from 10 to 40%; above 40% tritium removed the amount of coolant diverted to the TRS decreases very slightly.

Because a vacuum degassing system is used, the vacuum system must remove 37 Torr-liter/s of τ_2 at 500°C. The tritium pressure needed in the vacuum chamber to remove the tritium is determined from the fraction of tritium removed α , the Sievert's constant K_s , and the initial concentration C_1 , by the relationship

$$P_{T_2} = \left[\frac{C_1(1 - \alpha)}{K_s} \right]^2.$$

The speed of the vacuum pumps S_p is

$$S_p = \frac{37 \text{ Torr-liter/s}}{P_{T_2}}.$$

These relationships were used to determine the pumping speeds required as a function of the fraction of tritium removed in the TRS, and are also shown in Fig. 16-3. The pumping speed increases by only a factor of four as the tritium removal increases from 10 to 70% but rises more rapidly as >70% of the tritium is removed.

Based on the amount of coolant that must be handled, it is desirable to divert less than 20% of the coolant flow to the TRS which implies that >40% of the tritium must be removed (see Fig. 16-3). On the other hand, based on the size of the vacuum pumping system required, it is desirable to limit the tritium removal to <70%. The TRS design point is selected, therefore, at an intermediate value.

Fig. 16-3. Tritium extraction parameters as a function of coolant diverted to TRS at a T_2 pressure of 10^{-4} Torr in the coolant.

Having established the requirements for the TRS, we then examined three vacuum degassing techniques and gas sparging to determine their ability to meet the TRS needs.

16.2.2 Vacuum Degassing of Liquid Metals

16.2.2.1 Gas Transport Mechanisms. The transport of dissolved gas through a liquid metal has been studied and reviewed.^{10,11} It consists of the following three steps:

1. Transport from the liquid metal interior to the liquid/gas interface by convective transport in the interior and by diffusion through the boundary layer of the metal.
2. A phase transition region where gas atoms in the dissolved state at the surface combine to form molecules that are desorbed from the surface.
3. Diffusion of gas molecules in the gas space and removal by the pumping system.

Step 3 is rapid compared to steps 1 and 2 as long as there is a large enough mean free path for the molecules leaving the surface. Therefore, lack of impedance to gas flow must be incorporated into the detailed design.

Step 2 was not found to be rate controlling for removal of hydrogen gas from liquid steel. However, the phase transition step can become dominating if the concentration in the surface boundary layer is very small.

Step 1 is usually the slowest process so we examined it in more detail than the other two. The transport of dissolved gas in the liquid metal is described by the equation¹⁰

$$\ln \frac{C_{mt}}{C_{mo}} = -\beta_m \frac{A}{V} t \quad , \quad (16-2)$$

where C_{m0} and C_{mt} are the respective concentrations in the liquid at time zero and at time t , A is the surface area, V is the volume, and β_m is the mass transfer coefficient. The value of β_m is approximately proportional to the square root of the diffusion coefficient D and a term related to the degree of mixing in the bulk of the liquid metal. This relationship is expressed as

$$\beta_m = \sqrt{D/t_s} \quad , \quad (16-3)$$

where D is the diffusion coefficient (cm^2/s) and t_s is the mean dwell time of a volume element at the surface before returning to the interior of the melt. For hydrogen in steel, values of β_m at 1600°C are approximately 10^{-1} to 10^{-2} cm/s for bubble-free degassing and melts stirred by natural convection. In induction-stirred and gas agitated-melts^{10,11} where the agitation is vigorous and t_s is small, β_m is about 10^{-1} to 1.0 .

We used Eq. (16-2) to analyze the parameters necessary for the TRS to process the Li-Pb breeder/coolant. In this case, the volume V is equal to (flow rate $\times$ time). The value for β_m of tritium in liquid Li-Pb is unknown, but should be in the same range as hydrogen in molten steel, or 10^{-2} to 10^0 cm/s . The surface area required for progressive amounts of tritium removal in the TRS is plotted in Fig. 16-4 for three values of β_m and 25% of the coolant flow diverted to the TRS. It can be seen that the surface area required for 40 to 70% tritium removal, the proposed extraction efficiency, is very large, 10^2 to 10^4 m^2 . If all of this surface were contained in one pool, the diameter of the vacuum chamber would indeed be very large.

Fig. 16-4. Surface area required as a function of tritium removed in the TRS for three values of β_m at an entrance pressure of 10^{-4} Torr.

16.2.2.2 Droplet Spray. One method that provides a large surface area is called stream degassing, in which numerous small droplets are formed.^{10,12} This process, routinely used in the steel industry, is accomplished when the liquid metal is forced through a nozzle to form droplets that fall through a vacuum chamber. The degassing is enhanced if bubbles of the dissolved gases form within the droplets, which happens when steel is degassed. To form the bubbles, the gas pressure within the droplet must exceed the vacuum chamber and surface tension pressures. The surface tension pressure is $2\sigma/r$, where σ is the surface tension and r is the drop radius. For the liquid Li-Pb alloy, the surface tension is estimated to be 430 dyn/cm, based on the pure components; consequently, the surface tension pressure for a 1-mm droplet is ~ 6 Torr, which greatly exceeds the 10^{-4} -Torr T_2 pressure in the droplet. Therefore, bubbles will not form in the Li-Pb alloy droplet, and the transport tritium is solely dependent on diffusion within the liquid droplet without the aid of internal circulation.

The total amount M_t of diffusing substance leaving the sphere in time t compared with the amount leaving in infinite time M_∞ is given by the rapidly converging series

$$\frac{M_t}{M_\infty} = 1 - \frac{6}{\pi^2} \sum_{n=1}^{\infty} \frac{1}{n^2} \exp - \left[\frac{Dn^2 \pi^2 t}{r^2} \right], \quad (16-4)$$

which is conveniently solved in graphical form by Crank.¹³ To use Eq. (16-4), we assume the diffusion coefficient for tritium in the alloy to be 10^{-3} cm²/s based on similar systems, and the time of degassing to be the same as the time of fall in the vacuum chamber. The time of fall t can be calculated from the expression¹²

$$t = \frac{-v_o + (v_o^2 + 2gh)^{1/2}}{g}, \quad (16-5)$$

where v_o = velocity imparted by the spray nozzle, g = acceleration of gravity, and h = height of fall. If v_o is 3.7 m/s, the quantity of tritium that can be removed in times of 0.5 and 1.0 s (which correspond to $h = 5$ and 10 m) is shown in Table 16-3 for spheres of two different radii.

To determine the desirable efficiency ϵ of the removal system, we return to Eq. (16-1), whereby

$$\dot{m} = \frac{\dot{g}}{\epsilon(C_1 - C_2)}.$$

Previously, $\dot{m}/v$ was set at $\sim 20\%$ of the total coolant flow. Therefore,

$$\dot{m} = 20\% \times 8.8 \times 10^4 \text{ kg/s} = 1.7 \times 10^4 \text{ kg/s}$$

and

$$\dot{g} = \frac{37 \text{ Torr-liter } (T_2)}{s} = 4.6 \times 10^{-3} \text{ g(T)}/s.$$

Here, C_1 and C_2 are set by the Sievert's constant and the respective tritium partial pressures. The T_2 partial pressure P_1 was previously set at 10^{-4} Torr in the coolant and P_2 is now set at 10^{-5} Torr in the vacuum chamber so that the pumping speeds are not excessive; therefore, ϵ needs to be $\sim 80\%$. As Table 16-3 shows, this can be accomplished with a droplet 1 mm in radius and a fall of 1 s requiring a height of 10 m.

The area of the orifice A_o that is required to evacuate the TRS is determined from the relationship for conductance of the orifice C_o in free molecular flow by¹⁴

$$C_o = \frac{A_o v_{T_2}}{4} = \frac{37 \text{ Torr-liter/s}}{10^{-5} \text{ Torr}} = 3.7 \times 10^6 \text{ liter/s}, \quad (16-6)$$

where v_T = velocity of the T_2 molecules (1652 m/s). Therefore, $A_o = 9.0 \text{ m}^2$.

Having established the stream degassing parameters, it is necessary to determine if the degassing mechanisms are impeded by either (1) the mass of droplets in the vacuum chamber or (2) the gas/liquid dissolution reaction at the surface of each droplet. In regard to the possible loss of conductance by molecular flow of the T_2 molecules in the TRS as a result of the mass of droplets, we can make an analogy to an effusion cell. The TRS has been designed so that there is only one droplet per cm^3 ; therefore, the void volume is 99.6%. In an effusion cell, the change in the velocity distribution of gas molecules is negligible if the following ratio is small, i.e. $R_\lambda = A_o/4\pi\lambda^2$, where A_o = area of the orifice and λ = mean free path of gas molecules. At the vacuum chamber pressure (10^{-5} Torr), the value of λ is 46 m. While the droplets fall at 10 m/s, the gas molecules travel at 1652 m/s; therefore, the droplets appear to the gas molecules as stationary targets of cross section A_o . In this case the ratio R_λ becomes

$$R_\lambda = \frac{\pi(10^{-1} \text{ cm})^2}{4\pi(46 \text{ m})^2} \times 4 \times 10^8 \text{ droplets} = 0.05. \quad (16-7)$$

Table 16-3. Gas removal from a droplet spray.

Droplet radius (mm)	Time of fall ^a (s)	Gas removal (%)
1	0.5	60
	1.0	77
3	0.5	24
	1.0	32

^aTwo times of fall (0.5 and 1 s) are given as a representative range.

This is comparable to having 5% of the gas particles intercept the orifice of an effusion cell, and because the number is small, the velocity distribution of the remaining gas molecules is relatively unaffected. By analogy, in the TRS if 5% of the T_2 molecules collide with the droplets, then the remaining 95% are free to come to equilibrium with the residual gas pressure in the chamber, i.e. 10^{-5} Torr. Expressed in another manner, the molecular flow in the TRS is similar to the conductance of a gas through a vacuum system in which 5% of the molecules collide with a solid surface.

Monte Carlo calculations have been performed to determine the relationship between gas molecular collisions and the loss of conductance in pipes,¹⁵ elbows,¹⁶ baffles, etc. (see Table 16-4). For a similar transport phenomenon--the radiative heat transport through randomly packed bed of spheres¹⁷--up to 49% void volume has been analyzed. This latter value was extrapolated to 99% void volume and also listed in the table, which indicates that the conductance decreases slowly as the collisions increase. In such a case, it seems reasonable to calculate a conductance C_T for the TRS based on the relationship

$$C_T = \alpha \left(\frac{A_T v_T}{4} \right),$$

where α = relative conductance of TRS chamber containing droplets, $A_T = \pi(r_T)^2$, and r_T = radius of TRS (3.8 m). Table 16-4 shows that $\alpha > 0.8$ is reasonably conservative for 0.05 collisions; therefore, $C_T = 1.5 \times 10^7$ liter/s. The conductance of the TRS chamber will be in series with the conductance of the orifice C_o , so that the conductance of the system C_S will be

$$\frac{1}{C_S} = \frac{1}{C_T} + \frac{1}{C_o} \quad \text{or} \quad C_S = \frac{C_o \times C_T}{C_o + C_T} = \frac{4}{5}. \quad (16-8)$$

Because $C_T > C_o$, the value of C_S is controlled by C_o , not C_T ; therefore, $C_S = 1$ can be obtained by making $C_o = 1.3 \times 9 \text{ m}^2 = 11.7 \text{ m}^2$. This analysis indicates that the conductance of the TRS chamber should not limit the vacuum degassing of the droplets.

To discover if the transport by diffusion of tritium from the interior of the nonconvective droplet is the rate-determining step, the other two steps in the process must be considered. The step involving the resistance to diffusion in the gas space is negligible if there is a long enough mean free path for molecules leaving the surface. The previous discussion indicates that the mean free path is large (46 m), and that there is only a slight impedence to gas flow in the chamber.

More analysis is necessary in the phase transition region where gas atoms in the liquid combine at the surface to form molecules that desorb from the surface. The phase transition region can dominate if the resistance to flow across this region $1/K_m$ is larger than the resistance of tritium diffusion from the interior $1/\beta_m$. The rate constant for the phase transition of a diatomic molecule such as T_2 can be derived from the kinetic theory of gases,¹⁰ as in

$$K_m = \frac{\epsilon C_{ms}}{K_s^2 \cdot R} \left[\frac{R}{2\pi M T_s} \right]^{1/2}, \quad (16-9)$$

Table 16-4. Relative conductance of vacuum system components as a function of collisions with side-walls or packing.

Collisions	Component		
	Straight ^a /pipe	ELBOW ^b	Packed-bed ^c
0.1	0.94	--	--
0.3	0.89	--	--
0.5	0.85	--	--
0.7	0.81	--	--
0.9	0.77	--	--
1.0	--	--	0.7
2.0	0.64	--	--
5.0	0.48	--	--
7.0	0.43	--	--
9.6	--	0.28	--
12.0	0.33	--	--
100.0	0.10	--	--

^aRef. 15.

^bRef. 16

^cExtrapolated to 99% void from 49% void space for radiative transmission, Ref. 17.

where ϵ = the sticking coefficient, C_{ms} = the surface concentration, K_s = Sievert's constant, M = molecular weight, T_s = surface temperature, and R = gas constant (in appropriate units). For a sticking coefficient of one, the evaluation of this relationship for the TRS indicates that

$$K_m = 32 \text{ cm/s} \quad \text{or} \quad 1/K_m = 3 \times 10^{-2} \text{ s/cm}.$$

We calculated the value of β_m , the rate constant for diffusion from the pellet, based on the definition¹⁰

$$dn/dt = -\beta_m(C_m - C_{ms}), \quad (16-10)$$

where dn/dt = the volume of gas escaping per unit of surface area, C_m = the concentration of dissolved gas at the center of the sphere, and C_{ms} = the concentration of dissolved gas at the surface in equilibrium with the T_2 pressure in the gas phase.

We evaluated the value of dn/dt from a knowledge of the tritium gas that evolved in the TRS per surface area of a single droplet. We determined the value of C_{ms} based on the Sievert's constant and the T_2 gas pressure in the TRS, i.e., 10^{-5} Torr. The value of C_m was taken as the concentration in the center of the sphere at the end of the 1-s fall, which is 80% of the initial concentration¹³; therefore, the value of β_m should be larger than the average value. Even with this conservatism, the value of β_m is

0.04 cm/s, or $1/\beta = 25 \text{ s/cm} \gg 1/k_m (0.03 \text{ s/cm})$. This analysis indicates that the degassing of the sphere is not limited by a surface reaction but is limited by the diffusion of tritium in the droplet, which we used as the basis for sizing the TRS.

The above analysis therefore indicates that the degassing of tritium from the Li-Pb droplets in the TRS is not impeded by either the mass of droplets in the TRS or by a slow surface reaction at the gas/liquid interface of each droplet. The operational parameters for the TRS are given in Table 16-5.

In addition to tritium vaporization in the TRS, other volatile constituents will effuse into the vacuum system. The principal components to be considered are the constituents of the coolant (Li + Pb), any transformation products formed by the neutron irradiation in the coolant, and helium formed by the neutron irradiation of the lithium. Based on the vapor pressures¹⁸ of lithium-- 3.7×10^{-7} Torr--and lead-- 1.4×10^{-5} Torr--at their concentrations in the coolant and their respective conductances through the area of the orifice in the TRS, i.e., 9.0 m^2 , the effusion of these constituents would be $2 \times 10^{-4} \text{ g/s}$ lithium and $4 \times 10^{-3} \text{ g/s}$ lead. The principal radioactive transformation product of concern (see Sec. 20 of this volume) is polonium, which has a growth rate of $6 \times 10^{-3} [1 + t(\text{years})/8] \text{ Ci/s}$. Eventually, the polonium will effuse from the TRS at the rate at which it is formed and will deposit $1.3 \times 10^{-6} [1 + t(\text{years})/8] \text{ Ci/s}$ in the vacuum system. Because of the effusion of lithium, lead, and polonium, the off-gas from the TRS must transit a series of cold traps to remove these undesirable products from the tritium. The tritium removed from the TRS subsequently enters the fuel cleanup unit in the tritium process building for purification before injection into the fuel cycle.

The fate of the insoluble helium in the coolant is more difficult to predict. It will probably accumulate at the highest point in the coolant flow loop, such as the pressure surge tanks. Some entrained helium may remain in the coolant that enters the TRS. For this reason, pumps such as a turbomolecular pump or a compound cryopump are used to evacuate the helium. It is preferable in this instance to use turbomolecular pumps so that a low inventory of tritium is maintained in the pumping system.

16.2.2.3 Thin Films. A large surface area can also be created with favorable transport conditions for degassing when a liquid metal falls over a series of vanes, such as a "waterfall"-type cascade (see Fig. 16-5). The feasibility of using this technique for the TRS was considered when we evaluated the Reynold's number R_e for laminar flow, i.e.,

$$R_e = \frac{dv}{s} = 2000 \quad ,$$

where d = thickness of the liquid film, v = velocity of the liquid film, and s = kinematic viscosity of liquid Li-Pb.

Table 16-5. Parameters for tritium recovery by stream degassing/droplet spray.

<u>Input</u>	
Li-Pb flow rate (kg/s)	1.76×10^4
Tritium partial pressure in Li-Pb (Torr)	1×10^{-4}
Tritium concentration in Li-Pb (wppm)	5.1×10^{-4}
<u>Droplet spray</u>	
Time of fall (s)	1.0
Height of fall (m)	10.0
Total mass of droplets (kg)	1.76×10^4
Radius of droplets (cm)	0.1
Surface area of droplets (cm ²)	5.3×10^7
Number of droplets	4.2×10^8
Distance between droplets (cm)	1.0
Volume occupied by spray (m ³)	4.2×10^2
<u>Vacuum system</u>	
Tritium pressure in TRS (Torr)	1×10^{-5}
Tritium removal rate (Torr-liter/s)	37.1
Pumping speed required (liter/s)	3.7×10^6
<u>Outlet</u>	
Li-Pb flow rate (kg/s)	1.76×10^4
Tritium partial pressure in Li-Pb (Torr)	2.2×10^{-5}
Tritium concentration in Li-Pb (wppm)	2.4×10^{-4}
<u>Pool</u>	
Mass Li-Pb (kg)	7.0×10^4
Surface area (m ²)	42
Depth (m)	0.16
Residence time (s)	4.0
<u>TRS unit</u>	
Height (m)	10.2
Radius (m)	3.7
Volume (m ³)	440

Because the kinematic viscosity of Li-Pb is small (2×10^{-3} cm²/s), the thickness δ must be kept small (0.04 cm) to obtain a reasonably high velocity (~ 1 m/s). The velocity needs to be fast to minimize the amount of liquid in the TRS. These parameters were incorporated into Eq. (16-2) for out-gassing of a pool, i.e.,

$$\ln \frac{C_2}{C_1} = -\beta_m \frac{A}{V} t \quad ,$$

Fig. 16-5. Schematic diagram of thin film degassing over inclined vanes.

where C_1 and C_2 are the same values as for the stream degassing, β_m was assumed to be 10^{-1} cm/s, and $A/v = 1/d$. Therefore, $t = 0.5$ s. In 0.5 s the liquid would traverse 0.5 m of vane at a depth of 0.04 cm; therefore, 4400 linear meters of vanes 0.5 m wide would be required to deliver the flow required in the TRS.

The slope of the vane from the horizontal θ can be determined by using the relationship¹⁹

$$\text{velocity} = \left[\frac{8 g d \sin \theta}{f} \right]^{1/2}, \quad (16-11)$$

where g = acceleration of gravity, d = depth of liquid, and f = friction factor. For the coolant at a Reynold's number of 2000, f is ~ 0.03 . Evaluated for the parameters of the TRS, the angle θ is $\sim 40^\circ$. These vanes, which are 0.5 m wide, could be stacked into a TRS that would be ~ 5 m high x 5 m wide x 10 m long for a total volume of 250 m³. This technique appears to be feasible for the TRS; its disadvantage is that the corrosive liquid achieves intimate contact with the metal vanes.

16.2.2.4 Stirred Pools. Favorable transport of tritium from a liquid metal pool requires agitation of the pool to equalize the concentration within the pool and create a narrow diffusion boundary layer near the surface. The parameters for such a pool can be determined by use of Eq. (16-2). For tritium removal of 53%, the same as in the droplet spray technique, and a $\beta = 0.1$ cm/s, the surface area required is 1.3×10^3 m² (see Fig. 16-4). This represents a circular pool 20.5 m in diameter. For an inventory in the pool of 100 s, the depth would be 0.14 m. The large pool area and the need for vigorous agitation make the technique inferior to the droplet spray and flow-thin-film designs.

16.2.2.5 Gas Sparging. The formation of gas bubbles in the melt has two important effects on mass transfer. It causes agitation and creates a surface area for gaseous diffusion in the interior of the melt. Bubbles form spontaneously if the equilibrium pressure in the melt is greater than the sum of the ambient pressure on the liquid, the hydrostatic pressure on the bubble, and the capillary pressure caused by surface tension. The minimum pressure¹⁰ for bubbles to form spontaneously directly under the surface is

$$P_{\min} > 2\sqrt{20\rho g} \text{ dyn/cm}^2, \quad (16-12)$$

where σ is the surface tension, ρ is density, and g is the gravitational constant. For Li-Pb, $P_{\min}$ is approximately 4.4 Torr. Therefore, a pool containing 1.0×10^{-4} Torr of tritium will not spontaneously form bubbles when exposed to a vacuum; consequently, a purge gas must be injected at pressures much greater than 4.4 Torr to form bubbles throughout the melt.

The change in bubble size with pool depth due to the changing hydrostatic pressure on the bubble²⁰ is shown in Fig. 16-6. In this case we have assumed 0.1-cm-diameter bubble formed in a 200-cm-deep pool with an external helium pressure of 1 Torr on the pool. The bubble size remains fairly constant for the first 150 cm of its rise. The bubble radius begins to increase and expands very rapidly in the last 5 cm. We can estimate the required purge gas flow from this diagram. The maximum radius the bubble attains near the surface is 0.60 cm. If tritium saturates the bubble at this volume the bubble will contain 1.9×10^{-12} moles of T₂. Helium bubbles with 0.1-cm radii injected at the bottom of the melt at a pressure of 1388 Torr contain 1.2×10^{-7} moles of helium. The helium:tritium ratio in a saturated bubble immediately before it is lost from the surface is 6.3×10^4 . Since the tritium in the bulk must be removed at a rate of 7.7×10^{-4} moles T₂/s, the required purge gas rate is 49 moles He/s or 2.4×10^6 liter/s at 1.0 Torr and 500°C. This calculation estimates the minimum flow rate because it assumes that the bubble is always saturated with the equilibrium amount of tritium. This is particularly unlikely for the last 5 cm near the surface where the bubble volume is expanding too rapidly for equilibrium to be established. If the bubble radius at a depth of 5 cm is chosen as the final equilibrium point, the resulting purge gas flow rate is 1.4×10^7 liter/s at 500°C and 1 Torr. These excessive purge gas flow rates and the subsequent separation at large helium:tritium ratios make this degassing option unfavorable.

Fig. 16-6. Growth of helium bubble, 0.1-cm radius, as it rises from bottom of Li-Pb pool.

16.2.3 Conclusions

From our analysis of various vacuum degassing options for a MARS TRS, the droplet spray and the flowing thin films are superior to stirred pools and gas sparging. The droplet spray is chosen as the main design option primarily because of its simplicity. The short time of fall of the droplets results in short residence times and a low instantaneous Li-Pb inventory in the TRS. In addition, the design of the vacuum parts and chamber is simple and the overall volume is smaller. There is experience with stream degassing of steels to remove hydrogen, and predicted removal rates are in fair agreement with what is obtained in practice. There are presently no analogous systems to the thin film design in operation.

16.3 FUEL PROCESSING

This section describes in detail the fuel processing components listed in Table 16-1 and assesses the steady-state tritium inventory in all of the components.

16.3.1 Pellet Injector

Under steady state operation, a pellet injector is used to provide fuel to the reactor. To fully penetrate the center of the plasma in the central cell, pellet velocities as high as 20 km/s may be required. However, the actual velocity, determined by a detailed analysis, may be much less.²¹ These requirements represent an advance over present technology. A cryogenic pellet fabrication apparatus will be needed with an injection system based on electromagnetic "rail-gun" technology. Because it will be necessary to introduce in one pellet < 10% of the confined plasma particles, we selected a pellet radius of 2 mm. Such a pellet of frozen DT can contain at most 2.1×10^{21} (D+T) atoms; plasma confinement parameters require new fuel at the rate of 2,100 equivalent amperes of D and T atoms. If 100% of the pellet fuel is deposited within the plasma, the rate of pellet injection would be

$$\frac{1.37 \times 10^{22} \text{ (D+T) atoms/s}}{2.1 \times 10^{21} \text{ (D+T) atoms/s/pellet}} = 6.5 \text{ pellet/s}$$

For a 10-min inventory of fuel in the pellet injector, the volume of frozen DT fuel would occupy $\sim 130 \text{ cm}^3$ ($\rho \sim 0.26 \text{ g/cm}^3$) and would consist of 20.4 g of tritium and 13.6 g of deuterium.

16.3.2 Plasma Exhaust System

This system is discussed in detail in Sec. 8. Because mechanical blowers are used, the tritium inventory (<0.1 g) in these pumps consists of only tritiated gas molecules at a pressure of 10^{-2} Torr. All of these pumps are enclosed in a secondary containment structure so that any tritium leakage is contained and the tritium can be recovered.

16.3.3 Fuel Cleanup Unit (FCU)

The plasma fuel exhaust consists principally of DT molecular species (D_2 , DT, and T_2) and He; however, it will also contain carbon, nitrogen, and oxygen impurities released by bombardment of the first wall with the energetic plasma ions plus protium, H. The protium is introduced into the plasma exhaust from several sources, namely: DD plasma reactions ($\sim 0.4 \text{ A}$), corrosion followed by H diffusion in the water-cooled end cells ($\sim 3.6 \text{ A}$), and water leaks and evaporation from water-cooled metal surfaces ($\sim 1.0 \text{ A}$). Therefore, 1800-A DT fuel exhaust may contain 0.3% H. Based on the power loss in the plasma caused by the impurities and the cost of this lost power, the impurity content of the fuel has been limited to the following concentrations: H = 0.3%, He = 0.1%, O = 0.01% and heavy metals < 0.001%.

The impurities in the DT exhaust will appear as several molecular species, such as: HD, HT, C(H,D,T)₄, N(H,D,T)₃, NO, CO, etc. The entire exhaust stream is processed by the fuel cleanup unit, which is designed to remove the C, N, O, heavy metals, and most of the He so that only pure hydrogen isotopes are delivered to the isotope separation system. The fuel cleanup unit should also decompose most of the compounds containing tritium, such as CT₄ and NT₃, and recover the valuable tritium. The tritium removed from the TRS of the Li-Pb breeder is also cycled through the fuel cleanup unit to remove any trace of impurities.

Several fuel cleanup unit systems are presently being developed at the Tritium System Test Assembly (TSTA).²² The system chosen for MARS uses cryogenic adsorption separation processes (because of the large inventory of cryogenic fluids at the plant site) and avoids the use of high temperatures. In this system the exhaust gases flow through columns of type-5A molecular sieves (MS) cooled to 77° K, which adsorb all the impurities except He. When one column is saturated with impurities it is taken off-line and regenerated at ~ 400°C while the exhaust gases flow to alternate columns.

The size of the MS columns can be estimated from TSTA data in which 1.5 kg of MS are utilized to purify 356 moles H₂/day. In MARS, the columns should contain 770 g of MS for 4 hours of use before regeneration. At this stage, the MS contain the impurity condensed gases and some adsorbed DT at a concentration of 35 cm³ of DT gas at STP per gram of MS. Consequently, the tritium adsorbed on the column is ~ 3.6 g. With controlled warming of the columns during regeneration, most of the DT inventory is released and recovered before the impurity gases are released. Finally, the impurity gases, which contain tritiated species such as HTO, CT₄, NH₃, etc., must be prepared for disposal. If their tritium content is low, the gases are sent to the tritium waste treatment (TWT) system, which oxidizes all the tritium to water and adsorbs it on desiccants. The tritiated water from the regeneration of the TWT desiccants may be sent to the tritium water removal unit for recovery of the tritium. Alternatively, if the waste gases have high tritium contents they are oxidized in a closed system so that all the tritiated water is recovered with minimal dilution. The tritium from this water is recovered either by electrolysis or reaction with a hot metal such as uranium, in which pure DT is released and uranium oxides, carbides, etc., are retained. The DT fraction can then be sent to the isotope separation system (ISS).

The deuterium recycle flow from the anchor neutral beam injectors is processed in a similar but separate cryogenic fuel cleanup unit because the impurities are minimal. This stream is fed separately to the ISS.

The plasma exhaust stream, from which the chemical impurities have been removed, contains nearly 14 atomic % He, too large for the cryogenic ISS treatment. This He must be removed. One technique²³ studied at TSTA for separating He from hydrogen uses He evaporation from a condensed falling liquid film of the hydrogen isotopes. For a calculated flow-rate of 15 mole H₂/hour with a composition of 5 mole % He, the tritium inventory is ~ 2 g. For the MARS system, the flow-rate is 33.6 mole DT/hour at a 25 mol % He concentration. On a direct proportional basis, the inventory in such an apparatus for the MARS design would be 22.4 g of tritium.

16.3.4 Isotope Separation System

We selected the cryogenic distillation²⁴ of hydrogen isotopes, currently being studied at TSTA, for use in the MARS design. The function of the ISS is to reduce the protium (H) in the plasma exhaust and readjust the fuel with a D:T ratio of one for refueling the reactor. Additionally, the ISS is used to remove the tritium that accumulates in the deuterium recycle stream as a result of the "sloshing ion" neutral beam injectors. The ISS consists of four distillation columns arranged in a cascade operating at ~ 20 K and pressures of 600 to 1000 Torr. The plasma exhaust flow and the neutral beam injector recycle flow enter at different stages of the cascade. The tritium stream from the blanket TRS may need to be cycled through the ISS if it contains significant protium.

The products of the ISS consist of four principal streams:

- A pure T_1 stream for direct supply to the pellet injector,
- A nearly pure T_2 stream, some of which is blended with D_2 and fed into the pellet injector while excess tritium resulting from a net gain in the tritium breeding ratio is placed in storage;
- A nearly pure D_2 stream that can either be used to supply the neutral beam injector or can be mixed with T_2 to produce pellet fuel;
- A waste stream of nearly pure HD containing ~ 0.6 Ci(T)/day and small quantities of He. This waste stream is discharged through a tritium waste treatment system to reduce the tritium concentration before the gases are released to the atmosphere.

The tritium inventory in the ISS is largely governed by the amount of liquid in the reboiler flasks of the cryogenic distillation columns and the operating liquid hold-up in the packed columns. Recent TSTA experiments with H_2/D_2 separation columns²⁵ indicate that reboiler liquid levels can be reduced to 15 mm and liquid hold-up in the columns can be as low as 11 vol % of the empty columns. The total tritium inventory for the TSTA columns is 70 g for a throughput of 360 moles/day. Based on a direct proportionality, the MARS ISS with a throughput of 830 moles/day has a tritium inventory of 162 g.

16.3.5 Fuel Blender

The fuel blender unit²⁶ consists of several surge tanks in which the purified DT , T_2 , and D_2 are delivered from the ISS or the fuel storage unit. The D:T atomic ratio must be 1.0 for delivery to the pellet injector if the cryogenic unit of the pellet injector is able to condense the fuel without separating the hydrogen isotopes. The fuel blender will consist of mass flow meters and pressure gauges to deliver the required flows of DT , T_2 , and D_2 to prepare the fuel mixture. The fuel blender tanks should contain agitators that will thoroughly mix the fuel. The blended fuel is monitored by an on-line mass spectrometer to verify its composition. The fuel blender unit should also contain several volume-calibrated standard flasks at a controlled temperature so that grab-samples of the fuel can be accurately analyzed by P-V-T measurements combined with either mass spectrographic or gas chromatographic techniques.

16.3.6 Tritium Waste Treatment

Effluents from the fuel cleanup unit, the ISS, and other specialized equipment may retain quantities of tritium that are uneconomical to recover but are too high to be released into the environment. These gases are processed in the tritium waste treatment (TWT) system^{26,27} to reduce the tritium concentrations to acceptable levels before the gases are released outside the plant. The TWT mainly oxidizes the tritiated gas and absorbs the tritiated water on desiccant beds. It usually contains a special, high-temperature precious-metal catalyst in the front-end section to ensure that difficult-to-oxidize compounds, such as tritiated oil mists, are completely oxidized. The effluent gas from the TWT is held before being released; if necessary, it can be recycled through the system to further reduce the tritium content.

16.3.7 Fuel Storage

Enough fuel must be stored for reactor startup, for emergency use if the fuel recycle system fails, and as a repository for excess tritium bred in the reactor blanket. Because D_2 is continuously needed as fuel, a 30-day supply should be on-hand; however, since it is not radioactive, it should be stored according to the same criteria as the industrial storage of hydrogen. Conversely, the tritium storage vault must be well-contained so that any leakage, normal or accidental, is prevented for reaching the environment. Most of the tritium is kept as uranium tritide, $^{28}UT_3$ which stores large quantities of tritium in a small volume at a low pressure. The T_2 release from the UT_3 is rather slow, however, because the entire package must be heated to more than $400^\circ C$ to liberate the tritium. For this reason, some of the tritium will be stored in gas cylinders at nearly atmospheric pressure so that it can be delivered promptly when necessary.

The quantity of tritium in storage should perhaps be equal to one day's fuel supply (2.8 kg) in case the fuel recycle system is out-of-service for a day. The final decision on the amount of stored tritium will depend on the availability criteria assigned to the fuel system by the reactor operator.

16.3.8 Tritium Inventory

16.3.8.1 Summary. The tritium inventory in the various components at the reactor site is listed in Table 16-6. We have already discussed tritium in the fuel processing components and the end cell structure. In addition, tritium is found in several other areas of the reactor.

Of the total tritium at the site, the vast majority--nearly 3 kg--is found in the storage vault. Only ~ 50 g (5×10^5 Ci) of the tritium on site is vulnerable to release. For our analysis we have considered that the tritium contained in the tritium process building and the storage vault is invulnerable because each of these structures is made of high-impact-resistance concrete and can be hermetically sealed. Also, the tritium dissolved in the structural metal components is not vulnerable to possible accidental release scenarios.

16.3.8.2 Neutral Beam Injector. The neutral beam injectors add only deuterium into the "sloshing-ion" plasma of the end plugs; however, the neutral beam injector beam ducts (0.4 m x 0.8 m) present a window for molecular diffusion of tritium molecules in thermal equilibrium with the colder gas at the edge of the plasma. These tritium molecules condense on the cryopanel at the edge of the duct where they consequently mix with deuterium when the cryopanel is recycled. The pressure and temperature of the molecular gas at the edge of the plasma are not well-known in the sloshing-ion region, but the pressure is probably 10^{-6} Torr or less and the temperature is $\sim 400^\circ C$. We used these parameters to calculate the tritium streaming into the neutral beam injector by use of the relationship

$$F = \frac{P_{T_2}}{(2\pi MRT)^{1/2}} \text{ moles/s} \cdot \text{cm}^2,$$

when P_{T_2} is in dyn/cm^2 and R is in $\text{erg/mole} \cdot K$.

Table 16-6. MARS tritium inventory (in grams).

		<u>Total</u>
<u>Reactor hall and steam generator building</u>		69.5
Fuel pellet injector	20.4	
Reactor exhaust pumps	< 0.1	
Li-Pb coolant/breeder	7.5	
Water coolant (1 Ci/liter)	21.6	
Neutral beam injector cryopanel	1.0	
End cell structure	18.0	
Central cell structure (HT-9 steel)	0.8	
Reactor plasma	< 0.1	
<u>Tritium process building</u>		184.4
Fuel cleanup unit	22.4	
Isotope separation system	162.0	
<u>Storage vault (1-day supply)</u>		2833
Total on-site		3087
Vulnerable tritium		~50
Initial startup inventory (12-hour supply)		1500
Minimum startup inventory		~215

This relationship indicates that ~ 1.5 g of T_2 /day streams into each duct so that for the two neutral beam injector, the total tritium trapped in the ducts is 3 g/day. If the cryopanel is recycled 3 times/day, the inventory is 1 g of T_2 .

16.3.8.3 Structural Steel (HT-9). A considerable quantity of structural steel comes in contact with the Li-Pb breeder/coolant, which contains tritium at a pressure of 10^{-4} Torr. The total amount of HT-9 steel is 1720 tons, which comprises 210 tons in central cell modules, 1040 tons in the heat transfer pipes, and 420 tons in the heat exchanger. This steel will dissolve less than the amount of tritium determined by the solubility constant at 500°C, namely,

$$T_2 \text{ solubility (HT-9 steel)} = 2.3 \times 10^{-4} \exp(-7500/RT) \frac{\text{mol } T_2}{\text{cm}^3 \cdot \text{atm}} \cdot \frac{1}{1/2}$$

The total amount of tritium dissolved in the HT-9 steel is less than 0.84 g.

16.3.8.4 Coolant Water for End Cell and Reflector. The coolant water from the end cell and reflector is cycled through steam generators for additional power recovery. The combined volume of water in the reactor, transfer pipes, and steam generator is 216 m^3 . Tritium diffuses into this water at the rate of 1320 Ci/day in the end-cell halo scraper region and in the reflector. A tritium water recovery unit has been designed, as described in Sec. 16.5, which continuously removes tritium at the rate it diffuses into the coolant water. By this method, the concentration of tritium in the water is maintained at 1 Ci/liter. The tritium inventory is 21.6×10^4 Ci or 21.6 g, therefore, for the total quantity of coolant water.

16.4 TRITIUM ANALYSIS FOR THE DOUBLE-WALL HEAT EXCHANGER

Hydrogen permeability through different ferritic steels is summarized.²⁹ The best estimate of tritium permeability through HT-9 is

$$P_e = \frac{1.8 \times 10^3}{3} \exp - \frac{11100}{RT} \frac{\text{mol } T_2 \cdot \text{mm}}{\text{d} \cdot \text{m}^2 \cdot \text{atm}^{1/2}} .$$

With this permeability and a steam generator area of $3.5 \times 10^4 \text{ m}^2$, the tritium leakage rate is $3.5 \times 10^5 \text{ Ci/day}$, which is excessively above desired environmental release rates. Therefore, a tritium diffusion reduction of about 10^5 is needed.

In the MARS design, a double-walled heat exchanger is used to provide the required tritium diffusion barrier. The construction, with two tubes in close contact, is shown in Fig. 16-7. A helium purge with 1 Torr of oxygen pressure passes through the channels to provide an oxidizing atmosphere between the two tubes so that tritium forms T_2O . In addition, oxide films form between the two tubes, as well as the oxide film on the water side, to provide effective diffusion barriers.

Figure 16-8 shows two tritium diffusion paths through the double-walled steam generator. This is an enlarged figure around one contact point between the tubes.

Path 1 is a view through wall 1, across the gap, and through wall 2;
Path 2 is a view through the contact point.

To evaluate the effect of gap thickness δ on the permeation through path 1, we performed a calculation for a two-zone diffusion problem in a plane geometry. The equations and boundary conditions are

$$D_1 \frac{d^2 C_1}{dx^2} - \frac{v}{\lambda} C_1 = 0, \text{ and}$$

$$D_2 \frac{d^2 C_2}{dx^2} = 0$$

$$\text{at } x = 0 \quad -D_1 \frac{dC_1}{dx} = J_c ,$$

$$x = \delta \quad -D_1 \frac{dC_1}{dx_1} = -D_2 \frac{dC_2}{dx_2} ,$$

$$K_s (C_1 RT)^{1/2} = C_2 ,$$

$$x = L \quad C_2 = 0 .$$

Fig. 16-7. Barrier tube to inner tubesheet detail.

Here indices 1 and 2 refer to the gap and tube wall zones, respectively; D = diffusion coefficient; K_s = solubility of T_2 in steel; λ = mean free path of the T_2 and O_2 reaction; v = velocity of T_2 ; δ = gap width; R = gas constant; and T = temperature. With calculated parameters, the solution of the equations is easy to find and gives us the attenuation factor $P = J_0/J$, where J is the tritium current at $x = L$. Figure 16-9 shows the attenuation factor P as a function of the gap thickness δ with the thickness of wall tube being 0.1 cm.

Fig. 16-8. Tritium diffusion path.

Tritium permeation can further be reduced by oxidized layers attached to the tube wall surfaces. Suppose one oxidized layer can reduce the permeation through a steel wall by a factor of $1/f$; this effect can be incorporated into the above calculation. The first oxidized layer at $x = 0$ reduces the current J_0 to J_0/f , and the next two layers at $x = \delta$ and L act as if the thickness of the tube wall is increased by a factor of $(2f - 1)$. Examining the solution of this problem, we get an approximate additional attenuation factor by these three oxidized layers in the form

$$F = \frac{1}{\sqrt{f(2f - 1)}}.$$

The combined attenuation of the gap filled with oxygen and the oxidized layers is the product of the two. From this calculation an attenuation of 10^{-5} is possible with $\delta = 0.0025$ cm and $f = 14$. A single layer of oxide coating has an attenuation factor of a few hundred. Therefore, a combined attenuation factor of 10^{-5} is clearly available for diffusion across the gap.

We performed a two-dimensional, finite-difference tritium diffusion calculation for tritium leakage along path 2. The results depend strongly on the oxide condition around the contact point. Assuming an oxide layer exists between the contact point, an attenuation factor of 10^{-6} is available. The total diffusion is the sum of the diffusion along these two paths. Therefore, a diffusion barrier factor $> 10^5$ may be achievable.

Fig. 16-9. Attenuation factor due to gap.

The effects of imperfect oxidation of the tritium in the gap and deterioration of the oxide layer between the contact points are difficult to evaluate and can be verified only experimentally. Tritium isolation is a very difficult problem and is particularly difficult in the Li-Pb system because of the high tritium partial pressure. The double-walled steam generator presented here is only one of the possible concepts that may reduce the tritium leakage to an acceptable level. Further work is certainly needed in this area.

16.5 TRITIUM RECOVERY FROM WATER COOLANTS

In this section, we review start-of-the-art procedures for removing tritium from light water systems and select the most appropriate system for the MARS tritiated water removal unit. We then present conceptual design of this unit and assess the economics and utility requirements.

16.5.1 Design Parameters

Design parameters and requirements for the tritiated water recovery unit are obtained from consideration of the waterborne pathways for tritium as shown in Fig. 16-10. The possible sources of tritiated water are shown in Table 16-7.

To remove tritium from the air, six units at 5000 scf/min ($140 \text{ m}^3/\text{min}$) rated capacity for each unit are used for several areas. The total removal capacity of the air detritiation system is 1000 Ci/day of tritium contained in 200 liter/day of condensate water at a concentration of 5 Ci/liter.

It is useful to assess whether it is necessary to remove tritium from the aqueous coolant streams and to determine the consequence of not removing the tritium. In the combined coolant streams from the collectors, the halo scrapers, and the reflector, the following would result:

- Tritium production = 1350 Ci/day;
= 394,200 Ci/year assuming at least 80% reactor operation;
- Coolant mass = 216,000 kg;
- Production rate $R = \frac{394,200}{216,000 \times 365 \times 24}$
= 2.08×10^{-4} Ci/kg/hour.

Tritium concentration $A(t)$ at time t is $A(t) = A(1 - e^{-\lambda t})$,

where

λ = tritium decay constant = $6.42 \times 10^{-6} \text{ hour}^{-1}$,

A = concentration at equilibrium where tritium decay equals production.

Fig. 16-10. Building atmosphere and waterborne pathways for tritium.

Table 16-7. Sources of tritiated water.

System	Tritium production rate (Ci/day)	Flow (liter/day)
Condensate from Li-Pb/steam heat exchanger interspace	3300	0.001
End cell collectors	370	370
End cell halo scrapers	950	950
Reactor reflector	30	30
Hot cell wash water	100	100
Air detritiation system condensate	1000	200

It can be shown that

$$A = \frac{R}{\lambda} = \frac{2.08 \times 10^{-4}}{6.42 \times 10^{-6}} = 32.5 \text{ Ci/kg.}$$

After 30 years of operation the concentration would reach approximately 27 Ci/kg. Therefore, a tritiated water recovery unit is required to maintain water tritium concentrations to 1 Ci/kg (i.e., 1 Ci/liter).

16.5.2 Basis for Sizing the TWRU

The unit must be able to remove 1350 Ci/day from the coolant stream going to the direct converter, the halo scraper, and reflector, thus keeping the concentration at 1 Ci/kg. It must also remove 3300 Ci/day from the condensate produced by tritium/steam permeation into the interspace of the Li-Pb/steam heat exchangers, as well as process 200 kg/day from the air detritiation system condensate at 5 Ci/kg. To reduce tritium emissions and the amount of water that needs to be processed, we recommend that the hot cell wash water be recycled into a holding tank and used repeatedly until a concentration of 1 Ci/kg is reached. At this point, 100 liter/day must be processed to remove approximately 100 Ci/day. The total flowrate to be treated by the tritiated water removal unit is (1350 + 200 + 100), or approximately 1650 liter/day at an average concentration of 3.5 Ci/liter.

16.5.3 Separation Processes

The separation of tritium (present in the form of parts per million of HTO) from H₂O must depend on the difference in behavior of compounds containing the tritium atoms and the same compounds containing the hydrogen and deuterium atoms. It is recognized that for this application there is at least a 150-ppm background concentration of deuterium that will interfere with

the hydrogen-tritium reaction. However, for simplicity the description considers only hydrogen-tritium reactions; the final reference design and costs include the appropriate provisions for all the hydrogen isotopes. Chemically, tritium and hydrogen are expected to be similar since tritium is a hydrogen isotope. However, because tritium has a mass three times larger than hydrogen, the physical properties of compounds containing tritium and hydrogen are appreciably different. This difference is used to separate the isotopes.³⁰⁻³²

Because our aim is to produce high purity T_2 for recycle into the fuel stream, cryogenic distillation of $H_2/HT/T_2$ at around 20 K is the only practical final step for the separation process. Tritium is radiologically much less hazardous in the elemental form than in many other compound forms, such as the oxide. In addition, cryogenic distillation offers a good separation factor (e.g., for H_2/HT , α is approximate 2 at around 20 K) compared with water distillation (α = approximately 1.05).

Several options are available in the selection of the front-end process to convert the isotopes from the oxide to the elemental form to make them suitable for feeding the cryogenic distillation system.

Among all the various processes^{33,34} and their combinations available, the following are considered to be the most representative of the industrially practical choices.

- Catalytic exchange of H_2O/HTO against H_2/HT . This can be performed either in the vapor phase (VPCE) or the liquid phase (LPCE) using a wet-proof catalyst such as the one developed by Atomic Energy of Canada Ltd. (AECL). The VPCE has been used at the Grenoble tritium removal facility in France and is also the front-end process for Ontario Hydro's Darlington tritium removal facility. The LPCE process is being used by AECL for a smaller tritium removal plant to be built in Chalk River, Ontario. All of these facilities transfer tritium from DTO into D_2 . The above catalytic processes can also be extended to the hydrogen/tritium system and will produce an H_2/HT mixture that is isotopically weaker than the source HTO. This is more pronounced for H_2/HTO transfer than for D_2/DTO . Because tritium is the heavier isotope, it prefers to stay in the water phase as opposed to being transferred into the gas stream. In addition, the gas-to-liquid flow ratio is approximately 4:1 for H_2/HTO in the VPCE as compared to approximately 2:1 for D_2/DTO separation. This means that the cryogenic unit for an equivalent H_2/HT separation using the VPCE front-end is approximately twice the size as for a D_2/DT separation.
- Electrolysis of H_2O/HTO . Electrolysis produces an H_2/HT stream of the same molar flowrate and isotopic composition as the H_2O/HTO feed. Thus, for the same separation a cryogenic unit coupled to an electrolysis system is significantly smaller than a cryogenic unit coupled to the VPCE or LPCE.
- Combined electrolysis and catalytic exchange (CECE) of H_2O/HTO . This produces an H_2/HT stream richer in tritium than the feed HTO/H_2O . However, similar to the electrolysis but to a higher degree, it has the disadvantage of concentrating tritium in the electrolyte to higher-than-feedwater concentrations. The catalyst used in this process is the wet-proof type used for the LPCE process.

Each of the above "front-end" processes is discussed in greater detail when coupled with cryogenic distillation.

16.5.4 Process Description and Status of Technological Development

16.5.4.1 Vapor Phase Catalytic Exchange + Cryogenic Distillation. This process is illustrated in Fig. 16-11. The VPCE consists of a number of discrete stages. Each stage contains an evaporator, a superheater, a catalyst bed, and a condenser. The water is evaporated, superheated to 200°C, and mixed with hydrogen gas before passing over a catalyst bed, where the following exchange reaction takes place

The equilibrium constant for the vapor-phase reaction is $K = 0.309$ at 150°C and $K = 0.366$ at 200°C.

The steam is condensed in the condenser and flows to the next stage. Flow of hydrogen and water is co-current within a stage, but counter-current over the VPCE as a whole. Essentially pure hydrogen enters the VPCE at the bottom of the cascade and picks up HT from the water, providing a suitable feed to the cryogenic distillation system. A VPCE system requires a comparatively large cryogenic distillation unit because of the low concentration stream produced by the catalytic exchange and because of the large H_2 gas/liquid H_2O ratio necessary.

We have considered a case where the VPCE operating at 200°C is fed with water at (3.37 Ci/kg) 61 Ci/kmol. We have concluded that a six-stage VPCE is required to detritiate water to 1 Ci/kmol (0.05 Ci/kg) with a H_2 gas/liquid H_2O ratio of 5. In other words, the molar gas flow to the cryogenic unit is five times the water flow to the VPCE. Also, the feed concentration to the cryogenic unit is 11.8 Ci/kmol, which is approximately one-fifth of the feedwater concentration. Clearly, the cryogenic unit for the VPCE is large.

16.5.4.2 Liquid Phase Catalytic Exchange + Cryogenic Distillation. This process is illustrated in Fig. 16-12. Because the LPCE operates below 100°C, vaporization and condensation of water is not necessary. This also eliminates the requirement of having discrete stages in separate vessels, and, in fact, a continuous column can be used to provide a number of LPCE stages. However, the LPCE is disadvantageous in that the equilibrium at lower temperatures promotes tritium staying in the water form, as follows,

The equilibrium constant for this reaction is $K = 0.143$ at 25°C, and $K = 0.189$ at 60°C.

Thus, the K value is smaller than for the VPCE and a large gas flow and a lower-than-feedwater tritium concentration is sent to the cryogenic distillation unit, resulting in a cryogenic unit that may be even larger than for the VPCE.

A simulation of each case indicates that the flow to the cryogenic unit is approximately 8.26 times the feedwater flow, and the detritiation can be achieved with seven stages to 3.5 Ci/kmol (0.19 Ci/kg H_2O). With more stages, a lower outlet water concentration could be achieved. The tritium concentration in the hydrogen gas going to cryogenic distillation is 5.8 Ci/kmol.

Fig. 16-11. Vapor phase catalytic exchange and cryogenic distillation.

Fig. 16-12. Liquid phase catalytic exchange and cryogenic distillation.

16.5.4.3 Electrolysis of H_2O/HTO + Cryogenic Distillation. Electrolysis of water is well established in industry, being used routinely for hydrogen production. There are many different types of electrolysis cells available, not all of which are suitable for tritium service. The literature contains many surveys of electrolysis cells³⁵. For example, Pešek et al.³⁶ have proposed the use of bipolar electrolysis cells for tritium removal. The electrolytic separation factor for hydrogen/tritium is 11.0 at 80°C. Consequently, for a 3.5-Ci/kg feed the tritium concentration in the cell could reach 40 Ci/kg. The combination of electrolysis and cryogenic distillation is illustrated in Fig. 16-13.

The basic types of electrolysis cells are as follows:

1. Unipolar (tank type). The advantages of this type are:

- Simple construction;
- Common electrolyte with no shunt currents;
- Ease of maintaining electrolyte concentration;
- No electrical contact surfaces within the cell;
- Lower material cost per unit cell area;
- Low gasket length;
- High reliability.

Fig. 16-13. Direct electrolysis and cryogenic distillation.

The cell operates at nominally atmospheric pressure, and thus requires gas holders and gas compressors for H_2 , O_2 transfer.

2. Bipolar electrolyzer. The advantages of filter press electrolyzers are:

- No external busbars required to connect individual cells;
- Lower currents and higher voltages required, simplifying the power conditioning and controls;
- Operation at high pressure possible, i.e., no compressors, gas holders.

The major disadvantages are:

- More sophisticated and hence, more expensive;
- Longer gasket length, more chronic leakage.

3. Solid electrolyte cell. The advantages of this solid polymer electrolyte (SPE) cell are:

- Elimination of corrosive liquids;
- Compact design;
- Pressurized operation.

However, at present the SPE cells are only available for small-scale applications. When they become available for large-scale industrial applications in the future, their application should be reconsidered. The SPE electrolyzer, as known today, is not advantageous because of its external complexity but is advantageous because it does not present electrolyte handling problems.

An electrolyzer for tritium service must have the following characteristics:

- Low leakage, minimum gasket length;
- Pressurized operation to eliminate gas holders, gas compressors;
- Simple, low-maintenance design;
- High current density and low tritium holdup.
- Use of materials compatible with tritium handling.

The cell being developed by Ontario Hydro/AECL at the Noranda Research Centre satisfies all of these requirements. It is a 7.2-kA cell with a specific inventory of 2 to 2.4 liter/kA, has full-size components, and can be built in 24-kA units. Its internal components are contained in a tank with a single circumferential gasket. The cell is currently undergoing testing.

Electrolysis of H_2O/HTO has an advantage over catalytic exchange in that the molar flow of hydrogen isotopes to the cryogenic unit is equal to, but not higher than, the feedwater flow to the front-end. In addition, the tritium concentration in the hydrogen is equal to, but not lower than, the concentration in the feedwater. These two factors produce a lower capital cost and power requirement for the cryogenic unit associated with an electrolysis front-end.

16.5.4.4 Combined Electrolysis and Catalytic Exchange (CECE). The CECE process, as the name implies, is partly based on the reaction

with an equilibrium constant of $K = 7$ at $25^\circ C$. The equilibrium favors transfer of tritium from the tritiated gas phase into the water phase.

In the CECE process (shown in Fig. 16-14), a stream of tritiated water is fed to a column packed with hydrophobic catalyst. In the column, this feed stream mixes with a liquid reflux stream flowing counter-current to a gaseous hydrogen stream. Tritium is transferred from the gas to the liquid so that as the streams leave the column, the liquid stream is enriched in tritium and the gas stream is depleted. Outside the column, the liquid is electrolyzed to provide not only the hydrogen stream for the exchange reaction, but also a product hydrogen stream that is enriched in tritium. At the opposite end of the column, the depleted hydrogen stream is recombined with oxygen from the electrolysis cell to form a detritiated water stream that provides liquid reflux to the column and the detritiated water product.

The chief advantage of CECE is that the cryogenic unit, operating on the relatively high tritium feed coming from the electrolyzer, can be drastically cut in size relative to the other processes described. The disadvantages are the need for an even larger electrolyzer because of the need for reflux H_2O at the top of the column; the need for a long catalytic column; and the added hazard of having electrolysis cells full of $H_2C/HTO/KOH$ at about 5 to 10 times the tritium concentration reacted with the direct electrolysis. The tritium inventory in the CECE system is much higher and most of it is in the more hazardous oxide form.

The CECE process was developed by AECL in Canada. A pilot-scale CECE unit has been operated at Mound Laboratory, Ohio, since 1979. However, full-scale extended operation has not been demonstrated. The catalyst used is the AECL wet proof catalyst, and lifetime and activity tests on this catalyst are continuing at AECL at present.

16.5.5 Process Selection

The CECE process is the only one of the four that concentrates tritium in the feed to the cryogenic unit to greater than the feedwater concentration. Together with the electrolysis process, the CECE shares the inherent disadvantage of concentrating tritium in the electrolytic cell. Because of the need for larger cells combined with the pre-enrichment feature,

Fig. 16-14. Combined electrolysis catalytic exchange and cryogenic distillation.

the CECE process will, depending on the pre-enrichment, have approximately 10 to 20 times higher T_2O equivalent inventory when compared with the electrolysis process. This increases the radiological hazard to the extent that the use of secondary containment may be necessary. However, since more than one cell will likely be required, and since heat dissipation from the cells is necessary, secondary containment of the cells increases complexity and cost. In addition, periodic maintenance and electrolyte replacement of the cells will cause radiological exposure to personnel. In some studies, the CECE without secondary containment was not found to be significantly lower in cost than other processes. On the basis of high tritium inventory and overall complexity, the CECE is not considered to be a likely choice for the front-end of the TWRU.

The parameters for the remaining processes are given in Table 16-8, which shows that the LPCE and VPCE flowrates to the cryogenic unit are 10 and 5 times larger, respectively, than the flowrate for electrolysis.

The size and cost of the cryogenic unit are determined by the internal circulation in the largest cryogenic column, V (kmol/h), which is defined as

$$V = F \left[1 - \frac{Y_W}{Y_F} \frac{r}{\alpha - 1} \right],$$

Table 16-8. Comparison of VPCE, LPCE, electrolysis. Basis: Remove 5270 Ci/day.

Parameter	VPCE	LPCE	Electrolysis
Feedwater flow (kg/day)	1696	1961	1620
Feedwater concentration (Ci/kg)	3.37	2.92	3.5
Return concentration (Ci/kg)	0.05	0.19	0.001
H ₂ flow to cryo. unit (kg/day)	942	1800	180
T ₂ concentration to cryo. unit (Ci/kmol)	11.8	5.8	63.5

where F is the feed flow to cryogenic column (kmol/h), Y_W is the tritium concentration in the return stream from the cryogenic column, Y_F is the tritium feed concentration to the cryogenic column, r is $V/V_{\min}$ (we will use 1.3), and α is the relative volatility for the H₂/HT system.

Correlations for the capital cost and power consumption of cryogenic units have been derived for hydrogen isotope separation based on the volume flow rate V . The capital cost of the cryogenic unit is proportional to $V^{0.366}$, and power consumption for the cryogenic unit is proportional to $V^{0.81}$. We do not feel that the front-end process costs are the controlling costs for process choice. For DTO/D₂ systems, we found the front-end costs to be comparable for the three options. For HTO/H₂ separations, electrolysis produces lower front-end costs. Also, the data in Table 16-9 predict that the capital cost and power consumption are significantly lower for a cryogenic unit with an electrolysis front-end than for a cryogenic unit with LPCE or VPCE.

Based on the above analysis, electrolysis + cryogenic distillation has been selected as a reference process for the MARS tritiated water removal unit.

Table 16-9. Comparison of internal circulation parameters for cryogenic units with VPCE, LPCE, and electrolysis front end processes.

Internal circulation	VPCE	LPCE	Electrolysis
V (Kmol/hour)	22.93	43.84	4.388
$V^{0.366}$	3.15	3.99	1.72
$V^{0.81}$	12.64	21.37	3.31

16.5.6 Conceptual Design of the Reference Process

A conceptual flow sheet for electrolysis + cryogenic distillation is shown in Fig. 16-15.

16.5.6.1 Front-End. Feedwater for the tritiated water removal unit is collected in a storage tank. From there it is pumped to a pretreatment section where dissolved and solid impurities are removed. The feedwater then flows into the electrolysis cells. The electrolysis of 1620 liter/day requires 200 kA/hour. The reference design uses eight 25-kA cells in parallel. At 2.1 V, the power requirement for electrolysis is 420 kW. The electrolysis system is operated at approximately 400 kPa and 80°C.

The hydrogen stream produced by the electrolysis cells is cooled and dried by molecular sieve dryers before being sent to the cryogenic unit. The oxygen stream is sent to the recombiner.

16.5.6.2 Cryogenic Unit. The dry hydrogen gas stream is sent to the cryogenic unit where it passes through an adsorber operating at near liquid nitrogen temperature. This adsorber removes the last traces of moisture and also any oxygen and nitrogen. The bone dry, ultra-pure hydrogen gas enters the distillation columns after being cooled further to 21.5 K.

The primary column separates H_2 from HT. It operates at approximately 135 kPa and 21.5 K.

Substantial detritiation is obtained in this column. A computer simulation program developed and tested by Ontario Hydro has been used to assess the performance of the cryogenic unit for MARS. The return hydrogen stream from the top of the column has less tritium by a factor of approximately 10^4 , so that after recombining with oxygen in the recombiner, the resultant H_2O has a tritium concentration of approximately 0.001 Ci/kg. The primary column imposes the main load on the refrigeration system. The bottom of the primary column is further distilled in a second and third column until a 99 % T_2 stream is produced (0.6 g/day) at the bottom of the last column. The influence of background deuterium concentration (Sec. 16.5.3) will result in the need for an addition separation column.

The cryogenic unit contains packed columns, and is protected from overpressure by an expansion tank that limits the pressure increases as the system warms up. The columns are insulated by a vacuum of approximately 10^{-6} Torr. The cryogenic unit is constructed from 304L or 316L stainless steel.

16.5.6.3 Recombiner. Either a catalytic or flame recombiner can be used for the depleted H_2 stream. For the expected hydrogen flow rate, a flame recombiner is much less costly than a catalytic recombiner, and is therefore used for the reference design.

16.5.6.4 Refrigeration. Refrigeration for tritium removal systems can be supplied by refrigerators operating with either hydrogen or helium as the working fluid. A refrigerator using hydrogen requires approximately 2.5 to 3 times less power input than a unit operating with helium.

For MARS, the use of helium is recommended for two reasons:

1. A much larger helium refrigerator will be necessary to cool the reactor's superconducting magnets. The additional requirements for cold

Fig. 16-15. Tritiated water removal unit for MARS (conceptual flowsheet).

- helium gas at 17 to 18 K in the tritiated water removal unit will only impose a small additional load on this large refrigerator. Thus, the refrigeration requirements for MARS can be integrated into one system.
2. The use of helium refrigeration will lower the working pressure of the cryogenic columns, increase the relative volatility of the unit, and reduce its size. Since the hydrogen in the refrigeration system should be maintained at above atmospheric pressure at all points to prevent air ingress, the hydrogen in the primary column would have to be at a higher pressure than 135 kPa to provide a sufficient temperature difference if hydrogen were to be used as a coolant in the column condenser.

We estimate that the input power requirements for helium refrigeration are approximately 100 kW.

16.5.7 First-of-a-Kind Cost Estimates for the TWRU

The cost of the cryogenic unit is given by

$$C = AV^{0.366},$$

where A is a constant in millions of U.S. dollars, V is internal circulation in kg-mol/hour in the primary column, and C is the cost in millions of 1983 U.S. dollars.

Based on the experience of Ontario Hydro, the constant A for small cryogenic units of the size of the tritiated water removal unit is 2.5. Therefore, for a first-of-a-kind unit,

$$C = 2.5 \times 4.388^{0.366} = \$4.30 \text{ million U.S. (1983)}.$$

This cost does not include a separate helium refrigerator, but includes the cold box (with its associated piping and equipment), the recombiner, and extensive engineering associated with such a system. It does not include any freight, duty, taxes, or indirect costs such as corporate overheads, etc. It can be considered as the money paid to an engineering contractor for procuring such a system.

The cost of the front-end system can be given as

$$C_{FE} = BF^{0.7},$$

where F is the feedwater flowrate to the system in kg/hour, C_{FE} is the cost in millions of 1983 U.S. dollars of front-end, including the electrolysis cells and feed treatment system, and B is a constant. Based on Ontario Hydro's experience, the constant B has a value of 0.2 million U.S. dollars. Therefore, for a first-of-a-kind unit,

$$C_{FE} = 0.2 \times \frac{(1620)^{0.7}}{(24)} = \$3.8 \text{ million U.S. (1983)}$$

The total cost and power consumption for the tritiated water removal unit are summarized in Table 16-10.

16.5.8 Conclusions

The technologies needed to remove tritium from water and air are available on the commercial market. We feel that a combination of an electrolysis front-end together with cryogenic distillation of hydrogen

Table 16-10. First-of-a-kind direct capital cost and power requirement for the tritiated water removal unit.

	Direct capital (1983 U.S. \$)	Power consumption (kW)
Electrolysis unit front-end	3.8×10^6	420
Cryogenic unit	4.3×10^6	100
Total	8.1×10^6	520

isotopes is the best choice for detritiating water streams in this particular application under the present economic climate. In addition, integrating all hydrogen isotope separation needs in one facility using parasitic cooling from superconductivity refrigerators would offer the lowest initial cost, operating economy, and simplicity, as well as higher reliability.

16.6 ATMOSPHERIC DETRITIATION SYSTEMS

16.6.1 Description

The airborne tritium is scavenged from the atmosphere in the reactor hall, tritium process building, and secondary enclosures, such as the gloveboxes, when the atmospheric gases are circulated through the atmospheric detritiation systems. These systems³⁷ oxidize all tritium species to water and adsorb the tritiated water on desiccant beds. The desiccant beds are periodically taken off-line and heated to remove the adsorbed water. The condensed tritiated water is sent to the tritium water removal unit so that the tritium can be recovered and returned to the fuel cycle.

In the atmospheric detritiation system, the airborne tritium is combined with oxygen in a catalytic "DEOXO"-type reactor, heated to 450 K. If the atmosphere does not contain enough oxygen, the needed quantity of oxygen must be added ahead of the catalytic reactor.³⁸ Alternatively, the gases can be drawn through a "Hopcalite" reactor³⁹ (MnO_2/CuO) in order to oxidize all the hydrogen isotopes. If the airborne tritium is contained in stable compounds such as methane, the oxidation must be conducted in a special catalytic reactor heated to 800 K.

The quantity of tritium released to the environment under normal or accidental conditions depends on the concentration of tritium in the atmosphere of the reactor buildings. For this reason, the amount of tritium leaking from the process equipment to the reactor hall or the tritium process building is reduced by the design of appropriate secondary enclosures around all tritium process equipment. The atmospheres in these secondary enclosures are continuously recirculated through ATR's so that their tritium concentrations are maintained at a low level. Any leakage from these enclosures is scavenged by the detritiation systems that continuously recycle the building atmosphere. A flow diagram of the tritium airborne pathways is shown in Fig. 16-16.

Fig. 16-16. Possible pathways for atmospheric release of tritium.

16.6.2 Tritium Leakage to Secondary Systems

16.6.2.1 For this analysis we have studied three types of enclosures with similar atmospheres--either nitrogen or argon--and similar sources of tritium leakage. These systems are: (1) glovebox enclosures containing all the fuel reprocessing equipment in the tritium processing building; (2) enclosures surrounding the pumps used to evacuate the plasma exhaust from the end cell compartments and the tritium removal system, in which the tritium in the blanket is separated from the Li-Pb coolant; and (3) the hermetically sealed storage vault.

Each of these enclosures has two types of seals that may leak: (1) permanent seals, such as the interface seal on a metal bellows valve, which have been helium leak-tested to indicate leakage $<10^{-8}$ cm³-atm/s, but which may eventually leak at this rate or greater; and (2) shaft seals on rotating equipment, such as vacuum pumps, which may leak at a rate of 10^{-5} cm³-atm/s.

To assess the leakage from such components, we have determined that almost 10^3 components with permanent seals may leak at the total rate of 30 Ci/day. In addition, the seals on the 50 rotating vacuum pumps may produce another 120 Ci/day; consequently, the total tritium source term from these three systems may be 150 Ci/day. If this quantity of tritium were allowed to accumulate it would present an unacceptable hazard in the glovebox; therefore, this atmosphere must be continuously scavenged for tritium.

To estimate the size of the atmospheric detritiation system required for the gloveboxes, we estimated the total volume of the enclosed systems to be ~ 2000 m³. The largest atmospheric detritiation system designed thus far processes 140 m³/min (5000 ft³/min) of air at an estimated cost of \$4.2 x 10⁶ (Ref. 40). One of these units would provide an average residence time for the atmosphere in the glovebox enclosure of

$$\text{avg. residence time} = \frac{2000 \text{ m}^3}{140 \text{ m}^3/\text{min}} = 14 \text{ min } (\sim 4 \text{ changes/hour}) .$$

If the detritiation system removes the tritium at the rate at which it is generated, 150 Ci/day, then the residual tritium concentration C_0 in the glovebox is

$$C_0 = \frac{150 \text{ Ci/day}}{140 \text{ m}^3/\text{min}} = 740 \text{ } \mu\text{Ci/m}^3 .$$

For the total volume of the contained atmosphere the tritium inventory would be $(2000 \text{ m}^3)(740 \text{ } \mu\text{Ci/m}^3) = 1.5 \text{ Ci}$. If a glovebox ruptured and all this tritium were dumped into the tritium process building (a volume of 26,000 m³), the tritium concentration would be 57 $\mu\text{Ci/m}^3$. This concentration of tritium would not cause any immediate threat to personnel in the building because more than 100 min would be necessary for a worker without a respirator to absorb 1 mCi of HTO in such an atmosphere.

To determine the tritium leakage rate from the glovebox atmosphere, we assumed the leakage to be the result of permeation of HTO through 500 m² of

butyl rubber gloves ($\sim 1 \text{ m}^2/\text{pair}$) at the experimentally determined permeation rate ϕ given by⁴¹

$$\phi(\text{total}) = \frac{7.7 \times 10^{-3} \text{ } \mu\text{Ci}}{\text{m}^2 \times \text{day} \times (\text{ } \mu\text{Ci}/\text{m}^3)} \times 500 \text{ m}^2 \times 740 \text{ } \mu\text{Ci}/\text{m}^3 \quad (16-13)$$

$$\phi(\text{total}) = 3 \times 10^3 \text{ } \mu\text{Ci}/\text{day} = 3 \times 10^{-3} \text{ Ci}/\text{day}.$$

This calculated value of $\phi(\text{total})$ is very small when diluted with the total atmosphere in the building. We can conclude, therefore, that the atmospheric detritiation system for the glovebox atmosphere is reasonably sized.

16.6.2.2 Hot Cell Facility. The hot cell facility is used to dismantle the reactor's central cell modules and other reactor components, such as the end cell apparatus, that are contaminated with both tritium and radioactive structural materials. Because of the tritium, an atmospheric detritiation system must process the air in addition to filters that remove radioactive particulates. A separate facility will be used to dismantle and repair only tritium-contaminated hardware, but such a facility will not require the gamma-ray shielding used in the hot cell.

The hot cell facility is sized to handle the central cell modules, which must be replaced seven times during the lifetime of the reactor ($\sim 24 \text{ FPY}$). On the average the facility will process about one module every two weeks, although sets of seven modules will be removed from the reactor during maintenance periods. For this analysis we have assumed that the following procedure will be followed:

1. Drain the residual Li-Pb coolant from the structure;
2. Dry-brush the inside and outside of the coolant passageways to remove all coolant sludge;
3. Wash inside and outside with high pressure water;
4. Bake-out at 550°C for 2 hours in vacuum to remove 90% of the tritium from the steel;
5. Crush to reduce volume, and store in water-filled pool.

To assess the tritium sources in the hot cell as a result of processing the blanket modules, we found that the tritium solubility in each steel module, composed of 2.9 tons of HT-9 alloy, is only 7.5 Ci, whereas a total of 140 Ci was dissolved in the Li-Pb breeder/coolant when operated at T_2 pressure of 10^{-4} Torr (reported in Sec. 16.3 of this volume). During the processing of each module perhaps 50% (70 Ci) of the tritium might be released from the Li-Pb and an additional 5 Ci from the steel for a total of 75 Ci/module. If four modules are processed in one day, the 300 Ci of contamination removed is assumed to be two-thirds (200 Ci) airborne and one-third (100 Ci) in the wash water. Although such operations are not performed every day, we have assumed a tritium release rate of this magnitude to assess the size of the atmospheric removal system, because other types of contaminated tritium apparatuses with similar surface concentrations may be processed daily.

We have shown that a T_2 concentration of $10^3 \text{ } \mu\text{Ci}/\text{m}^3$ in a glovebox causes very low tritium leakage into the building; therefore, a similar airborne concentration is maintained in the hot cell. The flow rate $\dot{v}$

to the atmospheric detritiation system necessary to remove the T_2 at the rate it is released in the hot cell is determined by the relationship

$$(10^{-3} \text{ Ci/m}^3) \dot{v} = 200 \text{ Ci/day}$$

$$\dot{v} = 139 \text{ m}^3/\text{min}.$$

The hot cell requires, therefore, one more atmospheric detritiation system at a flow rate of $140 \text{ m}^3/\text{min}$.

16.6.2.3 Maintenance Enclosures. In the reactor hall and tritium process building operational equipment contaminated with tritium must be routinely opened for scheduled maintenance, repaired in-place, or removed and replaced. When such operations are performed, a temporary structure is often erected to enclose the work area.⁴² The atmosphere within the enclosure is recirculated through an atmospheric detritiation system for tritium removal rather than permitting the tritium to escape into the building's atmosphere. The temporary structures and the ducts connecting the structures to the detritiation system are erected and removed as needed. The maintenance workers are usually dressed in bubble suits during the operation and can either place their arms into gloveparts in the structure or they can enter into the enclosure for brief periods of time.

The amount of tritium released by the apparatus during such operations has been assessed for INTOR⁴³ and varies between 0.3 to 10^3 Ci/day . For the MARS maintenance, we assume an average release of 500 Ci/day . The desirable flow rate of the recirculated atmosphere through these structures is difficult to assess because they often contain small volumes so that high flow rates would not be possible. For our assessment we have used one atmospheric detritiation system at $140 \text{ m}^3/\text{min}$ to give an average concentration in the enclosure of $2500 \mu\text{Ci/m}^3$. In practice, the radiological health supervisor would will have to assess each maintenance operation.

16.6.2.4 Insulation of Li-Pb Pipes. The volume of Li-Pb heat transfer fluid in the pipes constitutes the largest reservoir of coolant in the system. Six sets of 0.76-m -outside-diameter (30-in.) pipes are needed to conduct the fluid between the reactor and the steam generators for each half of the reactor. The total surface area of these pipes and associated items (remelt tanks, tritium removal system, etc) comprises nearly 8600 m^2 . If these pipes have an average wall thickness of 25.4 mm (1 in.) and operate at 500°C with an internal tritium pressure of 10^{-4} Torr , then nearly 6000 Ci/day would permeate into the reactor hall. In addition, permeation and leakage at pumps, valves, etc., must be considered.

Obviously, the pipes must be encased in high-performance insulation so that the outside of the 5-cm -thick insulation is nearly at ambient temperature. Surrounding this insulation is an aluminum cover sealed by either brazing or by the use of permanent pressure seals. The tritium permeability of aluminum assumed to be at 350 K can be calculated by

$$V = PM \frac{A \sqrt{P}}{x},$$

in which the permeation constant, PM, is given by the relationship

$$PM = 10^7 e^{-17270/T} \frac{CC(STP) \cdot mm}{cm^2 \cdot hr \cdot \sqrt{atm}},$$

where A is the area ($\sim 10^8 \text{ cm}^2$), t is time, P is pressure, and x is thickness. The results show $V = 3.24 \times 10^{-9} \text{ cm}^3/\text{day}$, which is $8.6 \times 10^{-9} \text{ Ci/day}$. Therefore, if the aluminum cover provides complete coverage of the pipes, the tritium loss can be assumed to be zero.

The aluminum cover and the insulation are not perfectly sealed, however. The insulators will have gaps between sections that will permit tritium to quickly leak to the interface between the insulation and the external metal container. The permeation of the hot pipes will continue until the T_2 pressure within the sheath annulus is the same as the T_2 pressure in the Li-Pb, i.e., 10^{-4} Torr.

The fabrication and installation of the hermetically sealed aluminum outer sheath on the pipes around all the pipe hangers, bends, instrument probe intrusions into the pipes, etc., will represent a formidable task. Because the annulus between the hot pipe and the metal outer sheath is filled with insulation, and because the pipes are long with many bends, it appears impractical to either evacuate this annulus or purge it with an inert gas in order to reduce the T_2 partial pressure within this annulus. In an attempt to assess the potential tritium leakage from this metal sheath, we have evaluated experimental evidence⁴⁴ of the actual in-leakage of air into the inert fuel reprocessing cell at EBR-II. This cell is fabricated of welded stainless steel 1115 m² thick and has many weld instrumentation penetrations, sealed windows, etc. It has an air in-leakage rate of $4.2 \times 10^{-4} \text{ mol/s}$. If molecular gas flow through restricted channels is assumed in each case, then the leakage of tritium can be scaled to that of the inert cell by the relationship

$$J_T = \frac{J_A}{\Delta P_A} \left(\frac{M_A}{M_T} \right)^{1/2} \frac{\text{Area}_T}{\text{Area}_A},$$

where the subscripts A and T refer to the air leakage into the inert cell and tritium leakage from the pipe container, respectively. Evaluation of the relationship indicates that 5 Ci/day of tritium would leak through the pipe cover into the reactor building.

If the insulation can be fabricated so that it retards the diffusion of tritium and part of the aluminum cover is missing (or a crack develops on the cover), tritium will diffuse through the insulation material. If the tritium diffusivity in air is estimated to be $0.4 \text{ cm}^2/\text{s}$, the tritium flux M through a large area without the aluminum cover can be calculated by

$$M = \frac{DAK}{t} (C_1 - C_2),$$

in which C_1 is the tritium concentration at 10^{-4} Torr at 773 K ($= 2.1 \times 10^{-12} \text{ moles/cm}^3$), C_2 is the downstream tritium concentration ($= 0$), D is diffusivity, A is area, K is the reduction of diffusion by the insulation ($= 0.1$), and t is the thickness of the insulation.

If M is limited to 5 Ci/day, or $8.3 \times 10^{-5} \text{ moles/day}$, the required area is 6.8 m^2 . Although this area is large, it represents only 0.08% of the total pipe covering.

16.6.2.5 Gaseous Pipe Enclosures. All pipes in either the reactor building or the tritium pressure building that carry gases and contain significant quantities of tritium are enclosed within larger pipes. The primary pipes are welded and checked to be helium leak-tight to 10^{-8} cm³ · atm/s and are maintained at ambient temperature; consequently, the tritium leakage to the annulus should be very small. The annulus surrounding the pipes is purged with a slow flow of inert gas monitored for T₂ content in order to detect leaking components at the beginning so they can then be repaired.

The components that contain significant inventories of tritium, such as the pellet fueler and the cryogenic distillation system, require special attention to ensure containment. These components are contained within hermetically sealed secondary containers. However, because a large volume and pressure of gas form if the cryo-fluids evaporate, the secondary containers must be connected to previously evacuated large surge tanks⁴⁵. These tanks may also contain a reactive metal that absorbs large quantities of the hydrogen isotopes to further reduce the pressure.

16.6.2.6 Heat Transfer Water Pipes. The coolant water streams from the end cell/direct converter and from the region of the reactor reflector are routed to steam generators for power production. Each of these streams contains tritium that diffuses through the metal structure of each component at the rate of approximately 30 Ci/day in the reflector and 10^3 Ci/day in the direct converter. The tritium concentration is prevented from continuously increasing by diverting 0.5 vol%/day of the water to the tritium water removal unit as described in Sec. 16.5 of this volume. The total coolant water inventory—216 m³—is maintained at a concentration of 1 Ci/liter as a result of this water detritiation process.

The leakage rate of this tritiated water to the reactor Hall and steam generator building can be assessed based on the experience at the CANDU reactors, in which the "heavy water" contains tritium. The CANDU reactor operators⁴⁶ have chosen not to enclose the heat transfer pipes and equipment in secondary enclosures. Instead, they have worked to improve the leak-tightness of the equipment prone to chronic water leakage, such as pumps, valves, and flow meters.

Based on the CANDU experience,⁴³ the water leakage rate is approximately 70 to 80 liter/day for an inventory 740 m³ of deuterium oxide. By analogy, for the MARS system with a water inventory of 216 m³ at a concentration of 1 Ci/liter, the total tritium leakage to the reactor hall is ~ 25 Ci/day.

16.6.3 Tritium Leakage to Tertiary Containment Structures

16.6.3.1 Reactor Hall. The reactor hall and steam generator building will probably have a common atmosphere with a volume of $\sim 4.2 \times 10^5$ m³. As discussed previously, the routine tritium leakage rate into this atmosphere would be 30 Ci/day, 5 Ci/day from the Li-Pb pipes and 25 Ci/day from the water coolant. The atmosphere in these buildings could be air or an inert gas (CO₂ or N₂). We initially preferred CO₂ because it produces less gaseous radioactive transmutation products and offered increased fire protection. Because we selected an inert atmosphere, the building cannot be ventilated; therefore, the atmosphere must be recirculated to an atmospheric detritiation system to remove the tritium. The desired tritium concentration

level of the contained atmosphere is determined only after a number of factors are considered. Although a low tritium concentration may be desirable, the cost for the detritiation system increases significantly (see Table 16-11). Conversely, as the tritium concentration increases, the radiological hazards to occupational personnel and the neighboring population increase. For the occupational worker without respirator protection³⁷, present practice requires that for a 40-hour work week the average airborne concentration of HTO must not exceed $5 \mu\text{Ci}/\text{m}^3$. At air concentrations between 5 and $500 \mu\text{Ci}/\text{m}^3$, a worker must be protected by a supplied-air bubble suit. In an inert atmosphere, a supplied air breathing apparatus would be required; however a full bubble suit is not needed unless the airborne HTO concentration exceeds $5 \mu\text{Ci}/\text{m}^3$. In a full bubble suit, a maintenance worker is only 50% effective; therefore, any operation requires twice the labor time. Also, as a safety procedure for a worker in a bubble suit, an attendant is required to maintain visual or verbal communications with the workmen at all times.

The tritium concentration of the building atmosphere also affects the total release³⁷ of tritium to the environment. Environmental safety studies have suggested that the amount of contaminated atmosphere that might be released from a large facility may be 10^4 to $10^6 \text{ m}^3/\text{day}$. This release is not through penetrations in the containment shell but by escape through air-locks and other large entryways.

The Table 16-11 indicates that maintaining the building atmosphere at $5 \mu\text{Ci}/\text{m}^3$ would require 30 atmospheric detritiation units and would be extremely expensive. The selected airborne concentration of $50 \mu\text{Ci}/\text{m}^3$ for the MARS reactor hall requires three detritiation units, provides a reasonable level for maintenance workers using bubble suits, allows only minimal release to the environment, and is reasonably priced, at a cost of 12.6 million dollars.

In the event of large tritium releases in the reactor building (if, for example, the entire content of the pellet fueler-- $2 \times 10^5 \text{ Ci}$ --were released), the three detritiation systems would be operated continuously, recycling all the building atmosphere. All outlets to the building would be closed. Maintenance personnel would not be permitted to repair the damage

Table 16-11. Options for size of atmospheric detritiation removal system in reactor hall/steam generator building.

Airborne concentration ($\mu\text{Ci}/\text{m}^3$)	Flow rate (m^3/min)	No. of units ^a	Cost, \$10 ⁶	Worker protection	Stack effluent ^b (Ci/day)
5	4170	30	126	No respirator required ^c	0.05 to 5
50	417	3	12.6	Bubble suit	0.5 to 50
500	42	0.3	1.3	Bubble suit	5 to 500

^aUnit size $140 \text{ m}^3/\text{min}$ (5000 cfm).

^bBased on release of 10^4 to $10^6 \text{ m}^3/\text{day}$ through air-locks.

^cSupplied air required in an inert atmosphere.

until the atmospheric concentration was reduced below $500 \mu\text{Ci}/\text{m}^3$. Based on the relationship⁴⁴

$$C_t = C_0 \exp - \lambda t ,$$

where λ is the volume fraction of the air recycled per minute, C_0 is the initial atmospheric tritium concentration, and C_t is the residual atmospheric tritium concentration at time t (min), the atmosphere could be reduced to $500 \mu\text{Ci}/\text{m}^3$ in 4.8 days.

This may be an excessive amount of time from the standpoint of concurrent tritium release to the environment and plant availability. Three additional detritiation units could reduce the clean-up time to 2.4 days, more nearly compatible with current assessments.⁴⁸

16.6.3.2 Tritium Process Building. Because the tritium process building is separated from the reactor hall, its atmosphere has to be considered separately. The sources of tritium in the tritium process building are limited under normal operation because all of the process equipment is enclosed in gloveboxes. We have previously indicated that tritium leakage from the gloveboxes in the tritium process building is only 10^{-3} Ci/day. This quantity of tritium could be safely vented to the stack by the once-through air ventilation system.

In a process building, several operations must be performed that could release small quantities of tritium. However, these operations are difficult to quantify at this time. They are: (1) opening of pass boxes to insert or remove large objects when the pass box can not be purged; (2) removal of contaminated trash from the solid waste disposal gloveboxes; (3) incineration of contaminated combustible waste; and (4) disposal of contaminated fluids, such as pump oil. Although all of these operations will be conducted with the goal of minimal tritium release, in actual practice one of the best-operated tritium laboratories in the United States reports³⁸ an effluent average of 12 Ci/month for several years while operating a 11.5-m^3 glovebox system. By comparison, the gloveboxes in the tritium process building have a volume of $\sim 1200 \text{ m}^3$. If the laboratory operations that release tritium are similar in both facilities and they are scaled to the 0.7 power of the volume of the gloveboxes, then the operations in the tritium process building could release ~ 10 Ci/day to the building atmosphere. Because the tritium process building is operated as a radioactive laboratory, laboratory personnel do not routinely wear respirator equipment; consequently, the once-through air ventilation must undergo 10 to 12 complete changes per hour.⁴⁹ Such ventilation reduces the tritium concentration in the air to $1.6 \mu\text{Ci}/\text{m}^3$, adequate for extended occupancy without respiratory apparatus.

The alternative to continuous ventilation of the tritium process building is the internal recycle of the building air through an atmospheric detritiation system. When designed for continuous, unrestricted occupancy of workers without respirator apparatuses, the tritium air concentrations should be conservatively targeted at approximately $1 \mu\text{Ci}/\text{m}^3$. Such a decontamination of the atmosphere would be prohibitively expensive, requiring approximately 50 detritiation units at a cost of \$208 million. Additionally, humidity would be needed for human habitation, requiring the addition of water to the air after the atmosphere was circulated through the unit. As a consequence, a dehumidifier would have to be inserted ahead of the desiccants

in the system. This dehumidifier would capture large amounts of slightly tritium-contaminated water. The disposal of this water could be difficult. For these reasons, we have rejected internal air recycle.

In the event of a large tritium spill in the building, such as the release of the 162 g from the ISS, the building ventilators would close and one of atmospheric detritiation systems would be diverted from the reactor hall to process the building atmosphere. By this method, the building atmosphere could be detritiated to a level safe enough for personnel entry in a bubble suit, or $500 \mu\text{Ci}/\text{m}^3$ in 1.5 days. This is a reasonable clean-up scenario. Such a large tritium spill could not be tolerated by personnel (without respirator equipment) in the building at the time of the accident; therefore, the ISS must be well contained to prevent such a release.

16.6.4 Total Atmospheric Detritiation System Requirements

In summary, three atmospheric detritiation units are needed to continuously process the atmospheres in the glovebox system, the hot cell, and the maintenance enclosures. Three additional units are needed to continuously process the atmospheres in the combined reactor hall/steam generator building. No atmospheric detritiation units will be routinely used in the tritium process building. The effluents from this building exit to the stack with building exhaust. The placement of the units, the residual tritium atmospheric concentrations, and the possible pathways of tritium release to the environment are summarized in Table 16-12 and Fig. 16-16. Under normal operation, $\sim 15 \text{ Ci/day}$ of tritium would be released to the stack.

In the event of a large tritium spill into the reactor hall, the three existing atmospheric detritiation units would reprocess the atmosphere until it returned to $500 \mu\text{Ci}/\text{m}^3$ in 4.8 days. Three additional units on stand-by could reduce this time to 2.4 days. In the event of large tritium release in the tritium process building, the air supply and the exhaust to the stack would be closed and one unit from the reactor hall would be diverted to reprocess the contaminated air. This air would return to $500 \mu\text{Ci}/\text{m}^3$ in 1.5 days.

Table 16-12. Tritium source terms, atmospheric concentration, and release amounts for secondary and tertiary containment systems resulting from operations of atmospheric tritium removal units (ATR).

System	No. of units	Source terms (Ci/day)	Contained atmospheric concentration ($\mu\text{Ci}/\text{m}^3$)	Release (Ci/day)
<u>Secondary enclosures</u>				
Gloveboxes	1	150	740	3×10^{-3} (to RH ^a & TPB ^b)
Hot cell facility	1	200	10^3	4×10^{-3} (to RH)
Maintenance enclosure	1	500	2.5×10^3	10^{-2} (to RH & TPB)
<u>Tertiary enclosures</u>				
Reactor hall	3	30	50	0.5 - 50 (to stack)
Tritium process building	0	10	0.3	10 (to stack)
Total to stack				10 to 60

^a RH = Reactor hall.

^b TPB = Tritium process building.

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17

Cryogenics

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17. Cryogenics

17.1 INTRODUCTION

We have completed a conceptual design for the MARS cryoplant. This design is primarily based on existing performance, cost, and component data from the facility now under construction at LLNL for the Mirror Fusion Test Facility (MFTF-B) program. We have also conducted a survey to compare the MARS cryoplant performance and cost data to existing large liquid-helium cryoplants. No superfluid helium cryoplants larger than a 300-W capacity exist today. The LHe II cryoplant design is therefore based on conceptual and practical state-of-the-art design data.

The MARS cryoplant comprises three major subsystems that supply cryogens and refrigeration for all reactor superconducting magnets, beamlines, and the tritium processing system. These subsystems are the liquid helium I (LHe I) refrigerator/liquefier, liquid helium II (LHe II) refrigerator/liquefier, and a liquid nitrogen reliquefier. Also included in the design are storage facilities to supply cryogens and refrigeration during cryoplant repair (standby). The cryoplant capacities are as follows: LHe I, 14,500 W at 4.5 K refrigeration and 1462 l/hour of liquid production simultaneously; LHe II, 2,600 W at 1.8 K refrigeration; LN₂, 630 kW refrigeration/reliquefaction at 77 K. These capacities include a 20% contingency that covers unforeseen distribution losses and also serves as a design margin.

The cryoplant is designed to accommodate all steady-state and cooldown transient MARS requirements. On-site storage facilities contain adequate cryogens to satisfy steady-state cooling/liquid requirements during an 8-hour standby for cryoplant maintenance. In addition, a helium gas bag accommodates approximately 1420 m³ of room temperature/pressure helium during a cryoplant standby or superconducting magnet quench.

The actual facility requirements are such that all cryoplant equipment, except gas bag and some outdoor storage, will be contained in two rooms located at opposite ends of the main reactor building. This arrangement places the LHe II system closest to its loads (end cell magnet cooling) and reduces LHe I and LN₂ distribution losses.

17.2 SUBSYSTEM COOLING REQUIREMENTS

17.2.1 Magnets

The superconducting magnets, which contain the plasma, are the largest users of LHe I, LHe II, and LN₂. Table 17-1 summarizes the central cell magnet requirements by listing each component heat-leak contribution as well as the total cryogenic requirements for the 42 magnets. The liquid requirement results from vapor-cooled current lead (VCL), boiloff assuming a boiloff rate of 3.5 l/hour for a 1000-A pair. We obtained the conduction and thermal-radiation heat rates of the magnet supports by scaling from MFTF-B data. In addition, we used the neutron and gamma heating rates that are reported in Sections 3.3.5, 3.4.4, and 13.4.2 of this volume.

The LN₂ conduction loads represent heat removed at 77 K by intercepts that reduce the LHe I/LHe II conduction loads. Tables 17-2 and 17-3 summarize the choke coil and end cell cryogen requirements in a similar manner. Conduction loads for the choke and end cell coil structural supports enter the LHe II cryostat even though portions of the case are trace cooled to transfer

Table 17.1. MARS central cell magnet cryogen requirements.

One central cell coil	LHe I (W at 4.5 K)	LN ₂ (kW at 77 K)
Conduction	26	
Thermal radiation	38	7.04
Neutron heating	61	0.0026
Total for 1 coil	125 ^a	7.0426
Total for 42 coils	5250 ^b	296

^aLiquid required 20.4 liters/hour.

^bLiquid required 857 liters/hour.

Table 17-2. MARS choke coil cryogen requirements.

Choke coil	LHe I (W at 4.5 K)	LHe II (W at 1.8 K)	LN ₂ (kW at 77 K)
Conduction		26.5	
Thermal radiation	53.0	20	9.704
Neutron heating	81	86.0	0.011
Total for 1 coil	134	112.5 ^a	9.751
Total for 2 coils	268	225 ^b	19.5

^aLiquid required 28.0 liters/hour.

^bLiquid required 56.0 liters/hour.

Table 17-3. MARS end-cell-magnet cryogen requirements.

	LHe I (W at 4.5 K)	LHe II (W at 1.8 K)	LN ₂ (kW at 77 K)
Conduction		9.2 ^a	
Thermal radiation	43.0		8.150
Neutron heating ^b	391	149	0.023
Unit average	434	158.2 ^b	8.73
Total for 12 coils	5208.0	1898.4 ^c	98.1

^aAverage value.

^bLiquid required 25.4 liters/hour.

^cLiquid required 305 liters/hour.

the neutron and gamma radiation load from the LHe II to LHe I. It is important to note that LHe II heat leak is kept to a minimum at all times at the expense of LHe I (four times less power/watt removed) and LN₂ (143 times less power/watt removed) intercepts, shielding, and trace cooling. The operating expense advantage is obvious, and capital expense is reduced by significant ratios as well (these are discussed in greater detail in Sec. 17.3 of this volume).

17.2.2 Miscellaneous Loads

Beamlines, tritium processing, and distribution losses account for a relatively small fraction of the total cryogen requirement. Section 6.4 and 16.2 of this volume describe beamline helium and nitrogen requirements. We determine the tritium processing requirements and distribution losses by analysis.

17.2.3 Subsystem Load Summary

The total LHe I/LHe II and nitrogen cryogen requirements are summarized in Tables 17-4 and 17-5, respectively. These tables show the actual value and value with a 20% margin added.

17.3 CRYOPLANT DESCRIPTION

17.3.1 Capacity LHe I, LHe II, LN₂

17.3.1.1 Liquid Helium I Cryoplant. To satisfy subsystem requirements, this refrigerator/liqefier must provide simultaneously 14,500 W at 4.5 K and 1462 l/hour. The plant will include the following major components:

Table 17-4. MARS helium cryoplant load summary.

Magnets	LHe I load (W at 4.5 K)	LHe II load (W at 1.8 K)
Central cell (42 coils)	5,250	
Choke/end cells (2/12) with LHe I shield	5,476	2,123
Miscellaneous loads		
Beamlines, T ₂ processing	500	45
Distribution	730	
Total	11,956	2,168
	+20% = 14,500	+20% = 2,600

Table 17-5. MARS nitrogen cryoplant load summary.

Magnets	LN ₂ load (kW at 77 K)
Central cells (42)	296
Choke/end cells (2/12)	117
Miscellaneous loads	
Beamlines, T ₂ processing and distribution	12
LHe I and II cycle precooling	100
Total	525
	+20% = 630

- Six Howden oil-injected screw compressors;
- Four gas-bearing Sulzer turbo-expanders;
- Eight heat exchangers and numerous valves that comprise the cold box and refrigerator section;
- One auxiliary 0.6 m^3 heat exchanger (for cooldown);
- 100,000 liters of liquid helium storage (dewars);
- 1420 m^3 of ambient helium storage;
- Dry storage capacity of 76 m^3 , high (16.5 MPa) and low (12.4 MPa);
- One recovery system to repressurize helium from gas bag and dry storage (analyzer, dryer, purifier, blower, recuperator);
- Two local control panels in cold box/compressor vicinity (two locations, one at each end of reactor building).

This equipment list was assembled according to information provided in Refs. 1-3. Actual layout and dimensions can be determined only by a detailed design. In general, the LHe I and LN_2 system will be arranged in a manner schematically similar to that of the MFTF-B facility, however, the physical size and arrangement will be different (14,500 W for this system vs 11,000 W for MFTF-B). The schematic diagrams, cycle diagrams, and equipment layouts for LHe I cryoplants are well documented in the literature.⁴

17.3.1.2 Liquid Helium I Cryoplant Survey. We completed an extensive literature search and facility survey on large helium refrigerators. These data provide an excellent overview of capacity, efficiency, and capital costs of existing cryoplants compared with our proposed designs and concepts. Table 17-6 contains power, cost, and facility data from a survey of existing or proposed liquid helium cryoplants. It compares data on capacity, specific costs data, and plant types for cryoplants of various sizes. Figure 17-1 plots specific cost-per-watt of refrigeration vs. plant capacity; there is considerable scatter in these data. The results indicate that specific cost does not necessarily decrease or increase with greater capacity.

Plant No. 3 has a relatively low-dollar-per-watt (b/W) capital cost, but delivers the cooling at a relatively high operating cost of 1777 W/W (low efficiency). Conversely, plant No. 4 exhibits the opposite situation, where efficiency is improved at the expense of a greater capital investment. Plant Nos. 6 and 8 are examples of plants with performance and capital costs that lie between the parameter values of Plant Nos. 3 and 4.

A MFTF-B proposal by Koch is designed to keep capital costs moderate (not necessarily low) at the expense of operating costs (Plant 8, Ref. 1). Plant No. 7 represents a plant for helium extraction from natural gas and liquefaction at 4.5 K. Such a plant produces extremely pure helium from natural gas separation, resulting in large capital costs.

MFTF-B Cryoplant. The MFTF-B cryoplant to be completed in 1985 at LLNL is being built by CVI Inc. The existing MFTF-A (3.3 kW at 4.5 K) cryoplant will be supplemented with an 8 kW at 4.5 K unit, bringing the total capacity to 11,300 W at 4.5 K, 570 l/hour, simultaneously. The reported efficiency of the MFTF-A facility is 430 watts input compressor power per watt of cooling at 4.5 K. The 8000 W addition will also operate with an efficiency of 430 W/W.

The LHe I system cost will be roughly $\$12.5 \times 10^6$, and thus the incremental capital cost will be $\$12.5 \times 10^6 / 11,300 \text{ W}(4.5 \text{ K}) = \$1106/\text{W}$ at 4.5 K. The LN_2 system cost will be roughly $\$7.5 \times 10^6$, and thus the incremental capital cost will be $7.5 \times 10^6 / 500,000 \text{ W}(77 \text{ K}) = \$15.00/\text{W}$ at 77 K.

Table 17-6. Helium cryoplant survey data.

Facility	Year built	Cost 1982 (K\$)	Capacity (W)	GPI adjusted (\$capital/W at 4.5 K)	Comments
BNL (cold neutron moderator)	1969	658	600	1,097	
Los Alamos (superconducting magnets)	1974	977	1,000	977	
Fermilab (superconducting magnets)	1976	2,200	3,357	655	This refrigerator has low \$/W, however, efficiency is poor (1777 W/W)
ORNL (large coil program)	1976	1,668	1,000	1,608	Conversely this unit has high \$/W and improved efficiency (1044 W/W)
LBL (superconducting R&D)	1977	557	1,900	293	
Fermilab (superconducting magnets)	1977	1,034	1,000	1,034	An example of "medium" performance (efficiency of 895 W/W)
Bureau of Mines (helium production)	1978	3,103	357	8,691	This unit separates natural gas and liquefies helium (< 50 ppm impurities)
MFTF-B at LLNL (proposal)	1982	12,000	9,000	1,333	600 W/W. (Koch proposal)
MARS			14,500	1,106	Assumed linear scaling from MFTF-B
NASA (Langley) (for material research vacuum chamber)	1962	1,274	1,400	910	429 W/W
MFTF-B at LLNL (actual)	1982	12,500	11,300	1,106	430 W/W

Fig. 17-1. Survey of helium cryoplant economics where the capital cost (\$/W at 4.5 K) of the cryoplants listed in Table 17-6 are plotted as a function of their capacity (kW at 4.5 K).

CVI has considerable experience in building large helium refrigerator/liquefiers, therefore, William Chronis at CVI was able to provide us with the following efficiency data.² Cryoplant efficiency may be improved slightly at the expense of a greater capital investment. The two main "components" of a cryoplant, where such improvements can be made, are the cold-box and the turbo-expanders. However, the cold-box heat exchangers currently operate at nearly 97% effectiveness and provide little room for improvement. The turbo-expanders operate at >68% efficiency and could be improved if sufficient R&D funds are invested. It is difficult to persuade turbine manufacturers to invest because helium turbo-expanders represent such a small percentage of their total business income. Other possible methods of improving efficiency include adding (1) a turbo-expander, (2) increasing compressor power (flow rate), and (3) adding a heat exchanger.

Our survey information does not clearly indicate the amount of capital investment necessary to increase the life-cycle cost efficiency of a large helium refrigerator/liquefier. These tradeoffs can only be determined, through a detailed plant design/cost analysis, which is beyond the scope of this study.²

The MARS LHe I cryoplant specific costs and efficiency figures are: \$1,106 of capital investment/watt of plant capacity at 4.5 K, 430 W of compressor power/watt of heat removed at 4.5 K. These figures compare favorably with many other cryoplants.

17.3.1.3 Liquid Helium II Cryoplant. Existing data describing large LHe II refrigerators is limited, but the largest existing plant has a refrigeration capacity of 300 W at 1.8 K. The MARS LHe II refrigerator is based on a design for the Stanford Linear Accelerator 2-mile program.² Figure 17-2 outlines the basic thermodynamic cycle of a LHe I precooled arrangement that provides approximately 2600 W of refrigeration at 1.85 K, using helium isotope 4 as the primary working fluid.

A schematic diagram shown in Fig. 17-3 illustrates the conceptual flow pattern of the MARS LHe II refrigerator. In addition to the functions performed by the LHe I cycle compressor, the vacuum pumps must handle the large flow and maintain a low-side pressure of 12 Torr. Owing to the great operating expense associated with cooling at LHe II temperatures ($T < 2.17$ K), the final heat exchangers will be placed in the magnet dewar/service stack as shown in Fig. 17-3. This will allow us to produce LHe II in the service stack in the most efficient manner possible and minimize distribution losses. Hybrid helium-isotope-3/4 systems are slightly more efficient because of the higher vapor pressure of isotope 3, and thus a reduction in the low pressure vacuum work requirement. However, the construction and operation of such a system requires excessive amounts of isotope 3, which may not be available in the required quantities. By present day standards this would present a resource problem. If, however, this isotope becomes more readily available, a hybrid system should be considered. We will also use nitrogen precooling to reduce the operating cost of the system and increase efficiency.

The LHe II system will be divided into two 1300 W plants, each having its own compressors and vacuum pump system. This plant will provide refrigeration for the loads described in Table 17-4. The following equipment list summarizes the major LHe system components.

- Two compressors (three-stage, helical rotary);
- Refrigerators, i.e., cold boxes, precooling heat exchangers, turbo-expanders, and Joule-Thomson valves (1300 W at 1.85 K, one at each end);
- Vacuum pumps (four stages, 10 Torr to 1.5 atm, two at each end including one extra at each end for redundancy);
- Purifiers (dual, full-flow, ~1 ppm impurities, 12 atm);
- Piping, storage;
- Special instrumentation including pressure and level control, remote reading, and data logger.

The MARS LHe II cryoplant specific cost and efficiency figures are: \$3515 of capital investment/watt of capacity at 1.8 K, and 1720 W of compressor power/watt of heat removed at 1.8 K (Ref. 5).

Fig. 17-3. Schematic depicting the MARS 1He II system. The thermodynamic cycle is shown in Fig. 17-2.

17.3.1.4 Nitrogen Cryoplant. The nitrogen cryoplant will have a refrigeration capacity of 630,000 W at 77 K. It will be a basic vapor-compression system with screw compressors and turbo-expanders (gas bearing/Joule Thomson expansion) to provide this cooling rate. Reference 6 details LN₂ refrigerator/subcooler operation.

The nitrogen cryoplant will provide precooling to the LHe I and LHe II cryoplants and also refrigeration to the loads summarized in Table 17-5. The major components of the system are as follows:

- Booster compressors (four stages),
- Recycle compressors (three stages),
- Two turbo-expanders,
- Two cold-box heat exchangers,
- Two subcooler heat exchangers (these provide 100% liquid phase LN₂ at 85 K with no vaporization),
- 200,000 liters of liquid nitrogen storage (dewars),
- Aftercoolers (heat exchangers),
- LN₂ flow pumps.

This is a typical equipment list and was compiled using information from Ref. 1. The capital cost per watt at 77 K is 15\$/W with an efficiency of 12 W of compressor power per watt of heat removed at 77 K.

17.3.2 Modes of Operation

17.3.2.1 Cooldown/Warmup. The superconducting-magnet cooldown scenario comprises three stages: (1) cooldown of all magnets from 300 to 90 K, using the LN₂ auxiliary heat exchangers and LN₂ plant/helium gas; (2) cooldown of all superconducting magnets from 90 K to 4.5 K, using the LHe I cryoplant plus the LHe I section of LHe II cryoplant; (3) cooldown of choke coil and end cell magnets from 4.5 to 1.8 K, using LHe II refrigeration only. The cooldown times are based on MFTF-A cooldown test data, MFTF-B cooldown design values,³ and cooldown analysis.

Figure 17-4 illustrates the relative magnitudes of the time required for superconducting magnet cooldown. It is important to recognize that cooldown is accomplished without any supplemental bulk-cryogen delivery. It is expected that 46 days will be acceptable for initial cooldown.

For non-routine central-cell coil maintenance, only the two magnets within the maintenance section to be serviced needs to be warmed up and removed (see Sec. 12 of this volume). The same is true of choke coil maintenance. Each end cell must be entirely warmed up for the repair/maintenance of any one of the seven superconducting coils. However, the steady-state load/cooldown load represented by either of these three repairs is much less than the total load; therefore, repair/maintenance cooldown will be stress limited to 120 hours.

17.3.2.2 Steady-State Mode. During steady-state operation, the three cryoplants will deliver 100% of the refrigeration/liquefaction requirements of MARS subsystems. Depending on load demands, all boiloff gas (liquefaction load) will be retained in dry storage, or will be fed directly back into the main compressors. All cold gas will be returned to the cold box for quality reduction (refrigeration load) improvement.

Fig. 17-4. MARS superconducting magnet cooldown scenario.

17.3.2.3 Standby Mode. During an 8-hour standby mode or a cryoplant repair mode, sufficient cryogenics are in liquid storage to provide 100% of steady-state LHe I and LN₂ loads. If, however, the vacuum system of the LHe II system is being repaired, or is not operating for any reason, LHe II demands of the superconducting LHe II coils cannot be met. This problem suggests that a redundant LHe II vacuum system should be considered. In such a system, boiloff gas will be captured in the ambient-condition helium gas bag until the gas-bag 1420m⁻³ capacity is reached, then helium will be vented to the atmosphere.

17.3.2.4 Emergency Shutdown. In the event of a superconducting magnet quench, current will be dumped to external dump resistors. Depending on the particular situation, helium gas will be vented through the service stack. The portion of this vented gas that will be recaptured will be determined by the size of vapor-cooled-leads boiloff-return lines to the refrigerator. The excess gas will be purged into the atmosphere. In severe quench situations, a pressure-burst disc will blow and quench boiloff will be purged.

17.3.2.5 Safety Issues. One recent finding, which affects the safe operation of LHe refrigerators in a nuclear environment, is that if any air is allowed to leak into the cold box or contact a cold surface in a confined volume, ozone is produced and condenses on the LHe temperature surface. The thermal neutrons then react with the ozone environment and a spontaneous detonation/explosion may occur. However, vacuum gauges in the cold box and in other LHe temperature heat-exchanger areas are carefully monitored. A leak can be detected and repaired before a sufficient concentration of ozone is reached.⁷

17.4 FACILITY DESCRIPTION

In compliance with space allocations, the MARS cryoplant will be divided in two sections each placed at opposite ends of the reactor. Some storage will be external to the cryoplant building. These areas will include dry storage (76 m^3), a gas-bag area (30 by 9 by 6 m) and 200,000 liters (dewars) of space for liquid nitrogen.

17.5 SUMMARY

The MARS LHe I/LHe₂ cryoplant design is based on the MFTF-B cryoplant, which is being constructed at the Lawrence Livermore National Laboratory facility at Livermore, California. We have compared cost and efficiency data with other large liquid-helium refrigerator/liquefiers through a comprehensive facility survey. In this manner, a cryoplant concept has been presented that is based on existing technology and performance, except for the LHe II section, which is based on state-of-the-art conceptual and practical design data. Table 17-7 presents a comparison of the LHe I, LHe II, and LN cryoplants in this study.

The conceptual design presented here is sufficient to provide 100% of subsystem requirements during the steady-state operation of the reactor and will allow cooldown of the entire superconducting magnet set in 46 days (300 K to 4.5 K and or 1.8 K). Repair/maintenance cooldown times are stress limited to 120 hours.

This design contains sufficient storage (with the exception of LHe II, which needs a redundant vacuum pump system) to satisfy steady-state requirements for an 8-hour standby. Ambient helium dry storage volume of 1420 m^3 is provided for boiloff during standby.

Instrumentation provides the early detection system to eliminate the possibility of "ozone hazard" detonation.

Table 17-7. Comparison of LHe I, LHe II, and nitrogen cryoplants.

Cryoplant type	Cryogen supply	Efficiency W/W	Capital cost (\$/W)
LHe I	14,467 W at 4.5 K 1,462 liters/hour	430	1106/W
LHe II	2,602 W at 1.8 K	1720	3515/W
LN	630,000 W at 77 K	12	15/W

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18 Maintenance

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18. Maintenance

18.1 REQUIREMENTS

The objective of the MARS maintenance program is to provide a reactor design that is maintainable by totally remote means but which provides enough shielding to allow some hands-on maintenance operations. To permit this mode of operation, radiation levels within the reactor building are maintained to less than 2.5 mrem/hour within 48 hours after reactor shutdown.

The principal recurring maintenance task on MARS is the replacement of blanket modules. The reactor incorporates a blanket design that greatly facilitates this recurring major maintenance task. Each year, working at two or three locations along the central cell, seven blanket modules are removed from each of these locations. This operating scenario permits all the modules to be replaced by the end of the blanket design life.

The MARS central cell is a linear cylinder approximately 130 m long containing 72 blanket modules. Of these, 70 are normal size and two are short (for energy production and tritium breeding). The blanket is fabricated from HT-9 steel. Table 18-1 provides the pertinent physical parameters of the blanket relative to handling.

The blanket structure is cooled with the eutectic $\text{Li}_{17}\text{Pb}_{83}$, which also acts as the fertile material for tritium breeding. This coolant is completely drained from the blanket and supply headers before the blanket is replaced.

Table 18-1. Central cell and blanket parameters.

Central cell length (m)	130
First wall radius (m)	0.6
Outside diameter of blanket (m)	1.96
Length of normal blanket--cold (m)	1.79
Length of short blanket--cold (m)	1.58
Blanket dry weight (tons)	2.73
Full power life (years)	4.0
Central cell coil outside diameter (m)	7.5
Central cell field (T)	4.7
Net electrical power (MW)	1200

18.2 CENTRAL CELL MAINTENANCE

Figure 18-1 is a cross section through the MARS central cell. The plasma is encircled by the blanket structure, which receives the coolant breeding material from the headers entering at the top and exiting at the bottom. Surrounding the blanket is the reflector and shield structure. This structure, fabricated from Fe-1422 steel, is actively cooled with water. The shield is attached to the adjacent segment's shield by a welded leaf seal. A central cell coil surrounds the shield and is mounted on a support structure and base. This base is anchored to the floor during operation but is movable with transporters for nonroutine maintenance.

Fig. 18-1. MARS central cell cross section.

The central cell segment is the smallest integral unit of the central cell assembly. This segment can be removed in the event of a serious abnormal event such as a coil failure or vacuum and water leaks. There are two distinct types of central cell segments: the normal segment depicted in Fig. 18-1 and the service port segment shown in Fig. 18-2. An axial view illustrating both segments is given in Fig. 18-3. The service port segment can also be removed in the event of a magnet failure. We will discuss this operation later in this section.

A service port for blanket module replacement is incorporated in every other central cell segment along the length of the machine. Figure 18-2 is a cross section through the service port section of the central cell. The main structures in this section differ in that a removable reflector/shield is provided to permit blanket servicing. Above this removable shield is a secondary vacuum space to alleviate the vacuum sealing problems at the reflector interface. A large cover plate forms the boundary of the vacuum vessel at the service station. Penetrations through the cover plate have bellows or omega seals for alignment and flexibility.

18.2.1 Integral Features for Remote Maintenance

The most obvious integral maintenance features of the MARS blanket design are the small, relatively light-weight blanket modules. These modules are lifted through a loading port for installation and removal. The size and weight permit easy handling by overhead cranes, which are relatively inexpensive and readily available. A slow-acting vacuum valve is located between the end cell tank and the choke coil. This valve is closed to maintain the vacuum in the end cell tank before breaking any central cell vacuum seals.

Before the blanket can be removed, the large Li-Pb headers must be disconnected from the blankets at a location close to the blanket shell. This presents a significant remote handling challenge because the headers are located deep within the central cell.

Header connections to the blanket module at all locations are effected by a unique joint design that is remotely operable. The sealing concept is based on a frozen seal of Li-Pb that acts as a solder and is cooled by circulating helium gas.

Figure 18-4 is a cross section of such a joint with the sealing surfaces amplified. It can be seen that the flanges are clamped together by a set of latches pneumatically actuated from outside the vacuum barrier maintained by the two bellows. This clamp holds the flanges together while the seal is made and provides some mechanical rigidity during reactor operation. The expanded view shows two regions of frozen Li-Pb with a seal verification and leak detection space in between.

The frozen zones are in close contact with a coolant tube that can have hot or cold helium gas circulating through it, depending on whether the seal is being opened or maintained frozen. The initial sealing material is replaced each time the joint is broken. The seal is verified before the blanket is filled with breeding material by evacuating the space between the seals. This space is also equipped with two concentric copper rings separated by an insulator and electrically connected to an alarm. Leaking Li-Pb along any point on the periphery of the seal will short-out the circuit, tripping the alarm. The major advantage of this scheme is that the breeding material always replenishes the seal. Redundancy is provided by having two independent coolant circuits for the two seal zones.

Fig. 18-2. Central cell cross section at a service port.

Fig. 18-3. MARS central cell axial section.

Fig. 18-4. Frozen metal header seal joint.

18.2.2 Blanket Removal Procedure

Figure 18-3 is an axial cross section through the central cell of MARS. Depicted is a normal central cell module with service station modules on each side. Blanket modules are shown within the central cell with blanket number 0 always in position directly under a service station. Seven modules are serviced from each service station, including module 0, 1 through 3 right, and 1 through 3 left. Following removal of module 0, the additional six modules are translated into position under the opening and lifted from the vessel internals. The blanket removal operation begins by opening the service station to remove the movable shields as well as the blanket. Removal of the service station cover and header upper seal may be a hands-on operation working on top of the station. Figure 18-5 depicts the steps in preparing a service station for blanket removal. These include:

- Drain coolant;
- Disconnect and remove coolant lines;
- Deactivate and displace all Li-Pb seals at the blankets being serviced;
- Unbolt and remove service station cover plates;
- Remove reflection/shield movable section.

The initial steps can be accomplished manually. However, all operations must be accomplished remotely after the reflector/shield is removed. All equipment is lifted by a standard overhead crane. Standard electromechanical manipulators are used for tool handling and sling attachment in the remote mode.

Following removal of the reflector/shield, blanket module 0 is lifted from the vessel and the remaining blanket modules are then moved axially in a one or two step transfer until each is positioned under the vessel service port. Axial translation is accomplished by two pieces of maintenance equipment. A worm screw actuator, shown in Fig. 18-6, positions each of the blankets under the vessel service port. These actuators are installed into the vessel after module 0 is removed. They are mounted on the lower reflector/shield ledge, which is made available when the movable reflector/shield is removed, and remain in position throughout the servicing operation.

Modules 2 and 3, left and right, must be translated axially to position 1 in order to engage the worm screw actuator. This is accomplished by inserting a module-translating mechanism into the service port and attaching it securely to the vessel. This maintenance device, also shown in Fig. 18-6, extends via a telescoping boom to engage the module both on the end and from the bottom, much like a fork lift truck. The forks contain small, high-capacity jack pads that can dislodge a module that is stuck or galled in place. The unit then translates the module to position 1 for engagement with the crew actuator. The translating unit must be removed from the vessel before each blanket is positioned under the service port.

Fig. 18-5. Service station disassembly sequence.

Fig. 18-6. Blanket module maintenance equipment.

18.2.3 Replacement Time Estimate

We estimated the duration of the yearly blanket replacement operation assuming that the work is proceeding simultaneously at three service stations. The total time from reactor shutdown to service station closing is approximately 10 days, or 31 eight-hour shifts. Figure 18-7 is a breakdown of the operations and detail time estimates.

Fig. 18-7. Module replacement time estimate.

18.3 CENTRAL CELL SEGMENT REPLACEMENT

The central cell segments are removed and replaced only in the event of an abnormal occurrence, such as coil, vacuum boundary, or major internal failure. Replacement involves separating the segment from adjacent units, moving the 1500-ton unit to a repair area, and replacing it with a spare segment to permit continued machine operation. We have included a space segment in the MARS equipment list.

The segment is moved and controlled with the transhailer unit shown in Fig. 18-8. These units are shown in position under a typical segment in Figs. 18-2, -3, -5 and -6. The segment is actually removed by performing the following steps in the sequence given: (1) All service lines and cooling piping are disconnected and all of the blanket modules are removed from the damaged segment by the normal blanket replacement method. (2) The shielding that covers the vacuum boundary seal is then removed and the seal is cut. (3) the coil-to-coil structure is disassembled and removed. (4) The transhailer

Capacity — 250 T
 Drive — Electric motor 2 ft/min
 Lift — Hydraulic 0.25 in./min

Fig. 18-8. Transhauler unit.

units, six per segment, are positioned under the support structure and the segment is raised. (5) Riding on rails that are permanently imbedded in the floor, the unit is moved to a hot-cell area for disassembly and replacement or repair. (6) A spare segment is positioned in the machine for continued operation.

18.3.1 Spent Module Storage

We are exploring several methods for final disposal of the blanket modules. On-site burial of concrete-filled modules may be possible within present federal regulations. Low cost isotope modification of the molybdenum in the HT-9 may be possible if the regulations change or if a 14-fold reduction in volume is desired.

We have developed an interim storage scheme for the spent blankets. They could be stored under water until plant decommissioning or until some final on-site or off-site disposal method is approved. The only possible problem with this storage method is tritium release from the blanket structure. We have assessed this release, including normal release after removal following glow discharge and release from a blanket that has been baked for 2 hours after removal from the vessel. The total amount of T_2 dissolved within the structure of each blanket after removal from the reactor is estimated at ~ 0.014 kg, (i.e., 140 Ci or ~ 48 mCi/kg). The estimated amount of T_2 dissolved in the blanket following glow discharge is 2.5 mCi/kg. This can be further reduced by an order of magnitude after the 2-hour bake to 0.25 mCi/kg. However, the activity resulting from the T_2 dissolved in each blanket after removal from the reactor (~ 140 Ci) is small compared with the total activation of the spent module. In addition, there is no specific activity limit for tritium under current NRC guidelines for Class C near surface disposal (i.e., the specific activity limit is set at the theoretical maximum specific activity). Therefore, we conclude that, providing adequate tritium monitoring of the interim storage building is performed, active tritium removal from the spent modules is not warranted.

18.4 END CELL MAINTENANCE

18.4.1 Choke Coil Replacement

The choke coil insert is located in an area of high neutron fluence and is therefore subjected to accelerated material deterioration. We estimate that the choke coil may need to be replaced every 3 to 4 years. Because of this frequent maintenance requirement, the design of the coil structure is based on the remote removal method developed for the insert. Because of the size of the choke coil structure, the module must be handled on the transhauler units that are used to remove the central cell module.

Figure 18-9 is a section through the center of the choke coil. The structure consists of a superconducting coil and insulating system surrounding the copper coil insert and support plug. A number of structural jacks are arranged around the circumference of the magnet system and form a part of the superconductor's outer structural casing. Since the coil insert and support plug form the vacuum boundary, seals are required at both ends. Elastomeric seals are used at both ends with shielding to lower exposure levels to within acceptable limits.

Structural jacks, which are an integral part of the superconducting magnet case, transmit the central cell magnet loading into the end tank structure. These jacks, when remotely activated, transmit the load from the jack pad on the last central cell coil into the force of the end tank structure. The removable choke coil insert plug is held in place by a series of bolts around the circumference of the plug, which are accessible from the central cell side of the plug.

A coil can be replaced during a blanket maintenance shutdown period when the central cell is at atmospheric pressure. The vacuum valve between the choke coil and cell tank is closed to maintain vacuum for the end cell coils. The self-activating seal is released and retracted. Transhauler units are positioned under the coil structure to take the weight of the unit, and the structural jacks are retracted. The seal shield sections are removed using the overhead crane/manipulator unit normally used for blanket maintenance. With all structural attachments removed, the coil is translated axially a short distance to remove the seal at the valve end of the coil from its recess. The coil assembly is then moved laterally from the machine, as shown in Fig. 18-10.

The bolts securing the normal conducting insert coil are remotely removed and the coil is retracted from the central cell end of the superconducting magnet structure. Tables 18-2 and 18-3 provide a time estimate for each detailed operation in the installation/removal sequence. Figures 18-11 and 18-12 indicate the total elapsed time estimate of the coil replacement procedure.

18.4.2 Other End Cell Structures

With the exception of the supplemental heating systems (ECRH, ICRH, and neutral beams) and the drift pump coils, all other major structures in the end cells are expected to be lifetime components. Therefore, servicing of the transition coil, plug and anchor yin-yangs, the recircularizing coils, and the direct converter are not considered under routine maintenance requirements. In the rare event of a failure of one of these components, a method has been

Fig. 18-9. MARS choke coil.

Fig. 18-10. Choke coil removal.

Table 18-2. Choke coil removal.

Procedure	Time required (hours)
Choke coil assembly preparation:	
Disconnect bus connection	1
Disconnect cryogenic lines N_2 , He_2 , He	2
Close vacuum valve and remove vacuum duct	2
Insert magnet preparation:	
Disconnect bus connection	1
Drain and disconnect water cooling lines	1
Structural disassembly:	
Position transhaulers to support choke section	1
Retract reflector end vacuum seal	--
Retract structural assembly jack	2
Removal of shield segments (start of remote operation)	4
Removal operations:	
Move coil axially with transhauler	1
Rotate transhauler 90 deg	0.5
Move coil assembly laterally from the machine	1
Insert removal:	
Remove all insert fasteners	1.5
Attach to and remove coil insert	1

Table 18-3. Choke coil replacement.

Procedure	Time required (hours)
Insert assembly:	
Position and align insert in magnet	3
Reinstall all fasteners	2
Installation operations:	
Move coil laterally into machine	1
Rotate transhauler 90 deg	0.5
Move coil axially into seal recess	1
Structural assembly:	
Position and assemble shield segments	5
Actuate structural assembly jacks (start of manual operations)	3
Dielectric test	
Test end seal	1
Actuate reflector end seal and test	1
Remove transhaulers	0.5
Insert magnet attachment:	
Assemble and test bus connection	2
Attach waterlines and hydrotest system	2
Choke coil attachment:	
Attach vacuum ducts and leak check	2
Connect cryogenic lines and test	2
Attach bus connections and test	2

Fig. 18-11. Estimated time to remove the choke coil.

Fig. 18-12. Estimated time to install the choke coil.

provided for their removal and replacement. Access to the end cell vacuum chamber for disassembly can be accomplished with several design approaches and these are discussed in more detail in Sec. 7.1 in this volume. The approach selected for the baseline design involves the removal of the direct converter end-cap followed by subsequent outward axial translation of the end cell magnets by means of integrated transporters. Further details of this concept for the end cell structure can be found in Secs. 7-1 through 7.4 and Sec. 14.3.6 of this volume.

19 *Safety*

19. Safety

19.1 INTRODUCTION

In this section we assess the safety of the MARS reactor. The basic methodology is shown in Fig. 19-1. Our approach is deterministic in that we do not evaluate the probabilities of accident-initiating events and scenarios. In the deterministic approach, the first step is the identification of accident initiating events that can lead to a large scale radioactivity release. The second step is to identify the stored energies in the reactor systems that have the potential of driving the radioactive inventories into the environment. In the third step we presuppose that an accident has occurred and determine the migration paths and transport mechanisms of radioactivity through the plant and into the environment. Finally, we calculate the consequences to the public. In this analysis, we have not considered worker safety, nor have we considered the long-term effects on the public due to ingestion.

Fig. 19-1. Methodology used in MARS safety analysis.

19.2 FUSION SYSTEM SAFETY ISSUES

We have categorized safety issues for MARS into two main areas (1) sources and locations of radioactive materials, and (2) possible release mechanisms into the environment.

Most of the radioactive material in the MARS reactor resides in the activated HT9 structure of the first wall and blanket. The Li-Pb coolant in the first-wall and blanket is also a source of radioactivity. It contains activated corrosion products resulting from the corrosive action of the Li-Pb on the HT9 pipes and beams. In addition, the lead in the Li-Pb activates to a significant level.

The tritium inventory is divided between the reactor building and the tritium processing facility. The tritium in the reactor building is considered more vulnerable to release, and resides mainly in the Li-Pb coolant loop, plasma direct converter/reflector water coolant loop, and the fuel pellet injectors.

However, the existence of high levels of radioactivity does not in itself pose a threat to public safety without a credible scenario for mobilizing and releasing the radioactivity. Therefore, the mechanisms for releasing and transporting the radioactive sources from their location in the reactor building into the environment must be investigated. The most important potential release mechanisms that we have identified are overpressurization of the reactor containment building, a natural disaster, such as an earthquake or a tornado, or an act of sabotage that could undermine the building's structure. We discuss these issues in detail below.

19.2.1 Structural Activation

The first wall and blanket structure activate under neutron irradiation. The steady-state radioactivity level of the first wall is about 2.8×10^8 Ci/m³. The volume of the first wall structure is about 2 m³. Table 19-1 shows the percentage of the radioactivity and the inhalation biological hazard potential (BHP) contributed by the elements in the HT-9 MARS first wall. Table 19-2 shows the composition of HT-9. These tables show that there is no one-to-one correspondence between the level of radioactivity and the BHP. For example, iron contributes to about 42% of the total radioactivity but to only about 10% of the total BHP. On the other hand, vanadium only contributes to about 4% of the total radioactivity, but to 16% of the total BHP. The BHP is only one index of hazard, and is shown in this table only to indicate that high radioactivity levels and the safety impact of a given isotope do not necessarily go hand-in-hand. Only those isotopes that can be released will have an impact on public safety. Recent experiments¹ indicate that HT-9 volatilizes significantly only when temperatures exceed ~1000°C in the presence of air. Even then, oxides of molybdenum seem to be the only activation product compounds that remain airborne. Molybdenum represents about 1% of the first-wall activity.

19.2.2 Blanket Coolant Activity

The Li-Pb coolant circulating through the HT-9 pipes and beams in the first wall and blanket contains three sources of radioactivity. First, activated corrosion products are mobilized into the coolant by the corrosive

Table 19-1. Percentage contribution to the first-wall radioactivity and BHP in MARS.

Element	Radioactivity (%)	BHP (%)
Iron	41.7	9.8
Manganese	38.9	60.0
Chromium	10.0	0.9
Vanadium	3.7	16.0
Tungsten	1.2	1.7
Molybdenum	1.0	0.9
Niobium	0.1	4.2
Nickel	0.026	1.7
Other ^a	3.374	4.8

^aCobalt, technetium, zirconium, tantalum, rhenium, etc.

Table 19-2. Composition of the HT-9 used in the neutron activation analysis.

Element	Weight (%)
Iron	85.05
Manganese	0.55
Nickel	0.5
Molybdenum	1.0
Chromium	11.8
Tungsten	0.5
Silicon	0.3
Vanadium	0.3

action of the Li-Pb on the HT-9. Although Cr, Mn, and Ni are known to remove preferentially from the coolant, we have assumed that the isotopic composition of the corrosion products is the same as the composition of HT-9. We made this assumption because not enough is known about Li-Pb corrosion.

We estimate the resulting coolant activity due to activated corrosion products to be 10^{-6} times the value of the total blanket activity. This low level is made possible by filtering the corrosion products in a cold trap with an efficiency of 50% for each coolant-loop cycle. The time it takes the coolant to flow through the entire blanket and the associated piping outside the blanket is about 100 s (about 25 s for the blanket and 75 s outside the blanket), for a flow rate of 19 cm/s. The corrosion rate of the HT-9 produced by the Li-Pb coolant is estimated to be 600 kg/year, or about 2 g of activated structure per coolant cycle time (~100 s). Without filtration, the coolant activity produced by corrosion products would be much higher (about 10^{-3} times the blanket activity).

The Li-Pb coolant itself activates. For MARS we have used electrolytically refined lead--Li-Pb(e)--in accelerator shielding. It has a lower impurity content than the lead used for many other shielding applications. The nominal compositions of Li-Pb(e) and Li-Pb(n) are shown in Table 19-3. We calculated the coolant activity assuming a first-wall neutron wall loading of 5 MW/m² and a total Li-Pb lifetime of 20 full power years (FPY). We also assumed that the Li-Pb that spends 10% of the time (average) in the first wall neutron flux and the rest of the time in the piping and heat exchangers in essentially zero flux. The neutron cross sections were averaged over the first wall neutron spectrum. The major resulting isotopes and their specific radioactivities after 20 FPY are shown in Table 19-4. An improved calculation would average the flux over the entire blanket. However, our simpler analysis is satisfactory for the present purpose of driving rough measures of potential radiological hazard. In addition, the Li-Pb coolant loop (blanket, pipes, and heat exchanger) contains 7.5 g (7.5×10^4 Ci) of tritium. A loss-of-coolant accident (LOCA) is probably the easiest way of releasing above-normal levels of radioactivity into the environment. In addition, it may lead to partial first-wall damage if the plasma is not shut off in ~30 s. These accident scenarios will be discussed in Secs. 19.6 through 19.8.

Table 19-3. Nominal composition of Li-Pb coolant (wt %).

Element	Li-Pb(n)	Li-Pb(e)
Lithium	0.7	0.7
Iron	0.002	-
Cobalt	0.004	-
Nickel	0.004	-
Copper	0.080	0.0002
Zinc	0.001	-
Arsenic	0.002	-
Silver	0.020	0.001
Antimony	-	0.003
Lead	99.162	99.292
Bismuth	0.025	0.0042

Table 19-4. The major radioactive isotopes in Li-Pb(e) and their specific activities after 20 FPY of operation.^a

Isotope	Activity (Ci/m ³)	Isotope	Activity (Ci/m ³)
⁶ He	4.3 x 10 ³	²⁰³ Pb	9 x 10 ⁵
⁸ Li	2.4x10 ³	²⁰⁵ Pb	1.6 x 10 ¹
⁶⁰ Co	5.5	²⁰⁹ Pb	9.7 x 10 ⁵
^{60m} Co	3.7	²¹⁰ Pb	1.04 x 10 ⁻²
⁶² Cu	1.07 x 10 ²	²⁰⁸ Bi	1.1
⁶⁴ Cu	1.93 x 10 ²	²¹⁰ Bi	6.4 x 10 ²
⁶⁶ Cu	6.6 x 10 ¹	^{210m} Bi	3 x 10 ⁻³
^{110m} Ag	1.04 x 10 ²	²¹¹ Bi	7.1 x 10 ⁻³
¹⁰⁸ Ag	6.86 x 10 ³	²¹⁰ Po	6.4 x 10 ²
^{108m} Ag	2.9 x 10 ¹	²¹¹ Po	7 x 10 ⁻²
^{109m} Ag	4.4	²⁰³ Hg	4.5 x 10 ³
¹¹⁰ Ag	1.7 x 10 ³	²⁰² Tl	4.7 x 10 ⁴
¹¹¹ Ag	2	²⁰⁴ Tl	1.8 x 10 ⁴
¹²² Sb	1.01 x 10 ³	²⁰⁶ Tl	1.5 x 10 ⁴
		²⁰⁷ Tl	1.3 x 10 ⁴

^aThis calculation is an approximation in that much of the lithium is in the beam section of the blanket and does not receive the first wall flux.

19.2.3 Tritium Inventories

We have spent considerable effort in minimizing the MARS tritium inventories. The flow paths of tritium in the reactor system are shown in Fig. 19-2; the tritium inventories in the reactor building and tritium processing facility are shown in Table 19-5. The total vulnerable tritium inventory in the reactor building is only about 51 g (the tritium held up in the HT-9 is not considered vulnerable). A major source of tritium in our preliminary safety assessment was the tritium held up by the cryopumping system in the end-tanks and halo scraper regions. Eliminating the cryopanel in favor of Roots blowers results in a negligible tritium inventory in the exhaust pumps.

The major tritium inventory in the processing facility is found in the isotopic separation system (210 g), but is not considered vulnerable to release because it is enclosed by a glovebox. The inactive storage of 3 kg is also not considered vulnerable to release.

19.2.4 Reactor Building Overpressurization

The reactor building structure must be designed to withstand the maximum levels of overpressurization possible in an accident situation. The containment structure in MARS can overpressurize as a result of a release of either the cryogenic liquid helium (LHe) from the superconducting magnets or of water from the reflector/shield, resulting in steam flashing.

The central cell coils contain 105 m^3 of LHe I, which has a density of 125.2 gm/liter , resulting in a total reactor LHe I mass of $1.3 \times 10^7 \text{ g}$. Each reactor end has one choke coil and six C-shaped coils, that are cooled by LHe II. The total LHe II volume per end is 21.9 m^3 (5.2 m^3 for the choke coil and 16.7 m^3 for the C-shaped coils). The LHe II density under the MARS conditions is 143 g/l ,² which results in a LHe II mass per end of $1.5 \times 10^6 \text{ g}$. Loss of cryogenic LHe will lower the temperature of the building atmosphere to $\sim 200 \text{ K}$ as a result of flashing under adiabatic conditions. The building volume is taken to be $3 \times 10^5 \text{ m}^3$. Under these conditions, release of all of the LHe I from the central cell coils would result in a 2.6-psi increase in pressure. If all of the LHe II were released from one end of the reactor, the pressure would increase by $\sim 0.6 \text{ psi}$.

We have also analyzed the pressure increase due to steam flashing, caused by a reflector/shield rupture. The maximum temperature of the reflector coolant water is 320°C , with a flow rate of $5.1 \times 10^6 \text{ kg/hour}$, at a pressure of 2000 psi. The shield is also water-cooled with a maximum temperature of $<100^\circ\text{C}$ and a pressure of 17 psi. The volumes of the reflector and shield water are 25 and 30 m^3 , respectively. For these conditions, we have calculated an energy release in which all of the water in the reflector and shield flashes into steam. This would result in a pressure increase of about 0.7 psi. Because the volume of cooling water in the halo scrapers and collector plates is small (about 0.1 m^3), we did not consider it. We estimated the volume of water outside the blanket (in the pipes, heat exchangers, etc.) to be 115 m^3 . If this volume of water flashed it could lead to an additional 1.5 psi of pressure, although this is considered unlikely because of the fast-acting isolation valves ($\sim 0.5 \text{ s}$ response time).

Our results show that the loss of the helium coolant would produce the maximum overpressure possible in the containment building. But even under the worst-case conditions assumed above, the pressure increase would be limited to

Fig. 19-2. Tritium flow paths for the MARS reactor system.

Table 19-5. Tritium inventories (in grams) in the reactor building and tritium processing facility.

<u>Reactor building</u>	
Li-Pb coolant loop (including blanket, pipes and heat exchangers)	7.5
Plasma direct converter and reflector	
water coolant loop	21.6
Ht-9 steel hold-up	0.8
Plasma	<0.1
Fuel pellet injectors (10-min inventory)	20.4
Exhaust vacuum pumps (roots blowers)	<0.1
Neutral beam injectors	1
End cell structure	18.0
 <u>Tritium processing facility</u>	
Fuel cleanup unit (FCU)	22.4
Isotopic separation system (ISS)	162
Storage (1-day supply)	2833

less than ~ 3 psi. Under normal operation, the containment building is maintained at a negative pressure of about 0.1 psi. The reactor building is designed to withstand the instantaneous overpressure levels discussed above. We have not included in this analysis possible overpressurization due to hydrogen production should a Li-Pb/water or Li-Pb/concrete reaction occur. (Hydrogen production was observed in recent experiments.)³ The floor and walls are steel-lined to prevent Li-Pb concrete interactions. The Li-Pb/water interactions are considered in detail in Sec. 12.13 of this report.

19.3 ACCIDENT INITIATING EVENTS

Several events could precipitate accidents in MARS including internal system failures and external natural phenomena or acts of sabotage. In this study, we have not explicitly considered these external events. However, in our "worst-case" accident scenarios we have assumed the existence of an open path to the environment, which could be caused by an external event such as an earthquake, tornado, etc. Some of the possible internal system failures are:

- Pipe break in Li-Pb or H_2O coolant loops;
- Failure of magnet cryogenic systems (e.g., due to missile generation);
- Fueling system failure;
- Failure of glovebox containment for the isotopic separation system (ISS).

The most serious possible accident for MARS would be a LOCA in the Li-Pb coolant loop, which is accompanied by the very unlikely event that the plasma cannot be shut down before melting the first row of blanket tubes (first-wall). Such an accident could release some of the blanket tritium,

activated corrosion products, and possibly some volatilized first-wall structure. A LOCA can also occur in the H₂O coolant loop for the direct converter and the reflector/shield, leading to tritium release. A LOCA can be due to a pipe break, or may involve a more complicated sequence of events if failure of the magnet cryogenic system occurs. This would lead to large stresses in the magnet structures, resulting in possible vacuum vessel failure, which would provide an open path into the reactor building. Failure of the ISS glovebox system, accompanied by primary ISS containment failure, would result in the maximum possible tritium release from the MARS reactor. However, a more likely source of tritium release is if one of the fuel pellet injectors fails, or a LOCA in the reflector/shield/direct converter H₂O coolant loops. Since these systems are not protected by gloveboxes, they are considered much more vulnerable to releasing tritium.

19.4 BARRIERS AGAINST RELEASE OF RADIOACTIVITY

There are three main sources of radioactivity in the MARS facility:

- Activated first-wall/blanket structure;
- Activated coolant and corrosion products contained in the coolant;
- Tritium in the reactor building and in the tritium processing facility.

We have included several barriers in the design of the MARS reactor system that act to prevent the release of these radioactivity sources:

- The reactor containment structure is concrete, and has a steel liner on the floor and part way up the walls for Li-Pb containment. The walls are epoxy-lined to reduce the tritium permeation rate.
- Double-walled heat exchangers significantly reduce the permeation rate of tritium from the primary Li-Pb and H₂O coolant loops to the secondary coolant loop.
- Gloveboxes are used in the tritium processing facility around major tritium containing components (such as the Isotopic Separation System), and contain a leak detection gas, such as argon. A schematic of a glovebox surrounding such a system is shown in Fig. 19-3.
- High-efficiency particulate air (HEPA) filters are used in the stack to reduce the number of released radioactive particulates. The HEPA filter is a strainer type of dry filter and removes most airborne matter in the size range of 0.2 to greater than 1 μm .⁴ It has particle collection efficiency of 99.97% for 0.3- μm particles when tested with monodisperse, 0.3- μm thermally generated dioctyl phthalate (DOP) aerosol.⁵ The filtering efficiency increases for smaller and larger particle sizes.
- The air tritium removal (ATR) system removes tritium from the reactor building atmosphere in case of an accidental release.

A number of engineered safety features of the MARS design work in conjunction with these barriers to reduce the probabilities of accidental radioactivity release and also to reduce the consequences of an accident. These features are described in the following section.

Fig. 19-3. Usage of glove boxes reduces the probability of accidental release.

19.5 ENGINEERED SAFETY FEATURES

The safety features of MARS range from those that are specific to the tandem mirror reactor concept to those that have been deliberately made a part of the MARS design. Examples of the former include:

- Simple emergency plasma shutdown is possible by interruption of the rf and/or neutral beam systems.
- No plasma disruptions analogous to those in tokamaks have been identified for tandem mirrors. Therefore, first-wall coatings are not required, and localized melt regions cannot occur, as they can in tokamaks.

Deliberately designed safety features include:

- HT-9 is used for the blanket yields a vulnerable structural activity content that is about a factor of three lower than SS316. This is due to the lower molybdenum content of the HT-9.
- The first wall, blanket, and reflector cannot melt in the event of loss of coolant and/or loss of coolant flow in the blanket and reflector, as was shown in Sec. 12-3.
- The lithium compound $\text{Li}_{17}\text{Pb}_{83}$ is used as the blanket coolant and tritium breeder, which provides additional safety because of its relatively low reactivity with air, water, and concrete.

- Negative pressure is maintained in the reactor building to reduce the chance of tritium outleakage.
- The air tritium removal (ATR) system reduces the amount of tritium gas and oxides that escape via the ventilation stack, in case of an accidental release of tritium into the reactor building atmosphere. Furthermore, maintenance personnel can reenter the reactor building within approximately 2 days of the accidental tritium release, providing that the activity levels from other sources (Li-Pb spills or exposed first wall areas) are low enough.
- Low pressure coolant water is used in the shield region. This, combined with the low volume of coolant water used in the reflector/shield and direct converter systems, results in very low overpressurization levels, in case of a line rupture that results in steam flashing.
- The efficient design of the magnet cryogenic system means that very low overpressurization levels in the reactor building can exist as a result of cryogenic helium leakage.
- The reactor building has an inert CO₂ atmosphere. This reduces the amounts of the radioactive species C¹⁴, N¹⁶, and Ar⁴¹ that would be produced in an air atmosphere.

19.6 RELEASE OF STRUCTURAL ACTIVATION

Because most of the radioactivity in MARS is in the first wall and blanket, it is important to assess the consequences of accident-initiating events that can mobilize and release radioactive structural material into the environment. In this (and the following two sections) we calculate some possible doses at the plant boundary, which we have taken to be 1000 m.

The only accidents that we identified as having the capability of releasing activated material from the HT-9 structure of the first wall and blanket is a LOCA or loss of coolant flow (LOCF). A LOCA can occur following a loss of flow without plasma shutdown or as a result of a pipe break. Pump or power failure could cause a LOCF. In Sec. 12 we showed that a meltdown cannot occur except in the unlikely event that the fusion plasma is not shut off for a long period after the accident commences. Here we estimate the consequences of partial volatilization of the first row of tubes following this unlikely meltdown event. It should be emphasized that it is not possible to release activated structure without an open path into the reactor building atmosphere. Recent experimental work on prime candidate alloy (PCA), which is an austenitic steel similar in mechanical properties to SS316, indicates that it will become volatile in an oxidizing atmosphere only at high temperatures >1000°C. Therefore, the release of activated structure requires both a breach of the plasma containment vessel, exposing the HT-9 to oxygen, and high temperatures. Only oxides of molybdenum were observed during the volatilization experiments on PCA.¹ In addition, theoretical volatilization models were developed for SS316 and HT-9 (as discussed in Ref. 1). A comparison of the volatilization rates of SS316 and HT9 in fact shows that they scale with the molybdenum content of the two steels. Although not conclusive, this observation lends credibility to our assumption that only molybdenum oxides are expected to be mobilized under high temperature conditions in an oxidizing atmosphere.

The sequence of events leading to release of radioactivity into the environment is shown in Fig. 19-4. First, an initiating event leads to LOCA. If the plasma is shut off (which is the most likely situation), then no volatilization of the structure occurs since the first-wall temperature remains well below 1000°C. However, we based our analysis on the consequences of an accident in which the first wall melts as the result of the plasma remaining on (which is extremely unlikely). In this case, melting of the first-wall in about 30 seconds. (The first wall is the first row of blanket pipes in MARS.)

We ruled out $\text{Li}_{17}\text{Pb}_{83}$ reactions with air, water, or concrete as energy sources capable of releasing any structural material, based on recent scoping studies by Hanford Engineering Development Laboratory (HEDL) on reactions of lithium materials with air, water, and concrete.⁶ The following are their conclusions:

- No conditions were observed that might lead to radioactive material release and transport as a result of Li-Pb/air reaction. The experiment involved discharging 165 kg of $\text{Li}_{17}\text{Pb}_{83}$ at 450°C to preheated reaction air; normal humidity air atmosphere was used.
- The only concrete reaction observed was hydrogen production.
- Hydrogen production accompanied a Li-Pb/water reaction when 3 g of water was added to the breeder material held at 600°C. The resulting peak temperature was 652°C.

The results for $\text{Li}_{17}\text{Pb}_{83}$ /air reaction are summarized and compared with lithium/air reaction in Fig. 19-5, and for reactions with concrete in Table 19-6.

There is no need to consider blanket melting because after the first wall melted it would immediately quench the plasma, removing the heat source; this would leave the blanket pipes and beams relatively much cooler. A first-wall meltdown must be accompanied by contact with oxygen, otherwise no release occurs. Given that oxygen has entered the containment vessel and is in contact with the molten first wall, one needs to know how much of the first wall has melted. If we know the number of affected modules we can calculate the time required for the affected first-wall area to cool to ~1000°C. Knowing this cooling time, we can calculate the amount of molybdenum oxides that are released.

We calculated the cooling time of the molten first wall for two meltdown scenarios, assuming that a single module and all modules were affected. Because we do not know the geometry of the molten first wall we carried out bounding calculations assuming a molten first wall that remains in the original position and one in which a molten layer settles at the bottom of the containment vessel. The cooling down time depends on the amount of molten first wall. When the first wall of one (or several) modules melts, the other modules are much cooler and become a heat sink, radiatively cooling the molten area. If the entire first wall were to melt, the outer blanket regions would cool the molten areas, mostly radiatively.

The evaluation of the cooling down time requires the solution of the following energy equations

From melt to solid:

$$\rho V Q_{pc} = [\epsilon A_p (T_w^4 - T_a^4) - 0.2V] t_{ms} \quad (19-1)$$

Fig. 19-4. Events leading to the atmospheric release of activated material from the HT-9 blanket structure.

Fig. 19-5. Time behavior of the peak mixture temperature for lithium/air and Li-Pb/air reactions (see Ref. 6).

Table 19-6. Lithium-lead and lithium reactions with concrete.

	Lithium/concrete reaction	Li ₁₇ Pb ₈₃ alloy concrete reaction
Pool temp. increase	To 1300 °C	None
Interface temp	> 1300 °C	Unknown
Aerosol produced	5% of Reacted Li ^a	None ^b
Tritium release	All	Unknown
Activation product release	Unknown	None ^b
Hydrogen release	1 mole H ₂ /mole water in reacted concrete	0.65 mole H ₂ /mole water in reacted concrete

^aIn air atmosphere.

^bExpected based on pool temperature.

and

$$\frac{dT}{dt} + \frac{\epsilon \sigma}{\rho C_p L} (T^4 - T_w^4) = \frac{0.2}{\rho C_p}, \quad (19-2)$$

where the 0.2 factor in the equation is the volumetric neutron heat source (MW/m³).

The other terms are defined as follows:

- ρ = mass density (kg/m³),
- V = volume of molten region (m³),
- Q_{pc} = latent heat of fusion (MW/kg),
- ϵ = emissivity ≈ 0.5 ,
- A = area of molten region (m²),
- σ = Stephan-Boltzmann constant (MW/m² -K⁴),
- T_w = temperature of nonmolten regions $\approx 500^\circ\text{C}$,
- t_{ms} = time to go from melt to solid (s),
- L = average depth of molten region (m).

Equation (19-2) has the following solution for t_{cd} , which is the time required to cool from an initial temperature T_o to temperature T :

$$t_{cd} = \frac{1}{2\phi^{1/4}\psi^{3/4}} \left[\frac{1}{2} \ln \left(\frac{\psi^{1/4} + \phi^{1/4} T \psi^{1/4} - T_o \phi^{1/4}}{\psi^{1/4} - \phi^{1/4} T \psi^{1/4} + T_o \phi^{1/4}} \right) + \tan^{-1} \left(\frac{\phi^{1/4} T}{\psi^{1/4}} \right) - \tan^{-1} \left(\frac{\phi^{1/4} T_o}{\psi^{1/4}} \right) \right], \quad (19-3)$$

where

$$\phi \equiv \frac{\epsilon \sigma}{\rho C_p L} \quad (19-4)$$

and

$$\psi \equiv \frac{0.2}{\rho C_p} \quad (19-5)$$

Here ψ represents the decay heating term of the first wall. It is seen to be numerically very small compared with the radiative cooling term.

Using Eqs. (19-1) and (19-3), we can calculate the total cooling down time $t_{ms} + t_{cd}$. For one module, the cooling down time to 1000°C is about 300 s; for all modules it is about 1000 s. During this time molybdenum oxides are released into the reactor building as a result of volatilization of the HT-9, which occurs at an average rate of 2.5×10^{-8} m/s.¹ This corresponds to the volume of steel volatilized per m² of the surfaces/s, and is the average value over the temperature range from 1000 to 1400°C. The particulate concentration released into the atmosphere can be reduced with an efficiency of 99.97% using a HEPA stack filter. The resultant doses at the plant boundary have been calculated using

$$DOSE_j = \frac{\chi}{Q_j} \sum_i A_i \times DCF_i, \quad (19-6)$$

where

$DOSE_j$ (mrem) = the external dose to the body at distance j from the reactor;

χ/Q_j = the atmospheric dispersion coefficient at distance j from the reactor (s/m³);

A_i = activity of nuclide i released (Ci);

DCF_i = total body dose conversion factor for nuclide i

$$\frac{\text{mrem} \cdot \text{Ci}}{\text{s} \cdot \text{ml}}.$$

The summation in Eq. (19-6) is over all molybdenum isotopes. Atmospheric dispersion coefficients were obtained from Regulatory Guide 1.145.⁷ A puff model was used, with radioactive material released uniformly from the reactor building and then dispersed in a Gaussian plume. A basic equation for dispersion from either continuous or "puff" point sources is the "generalized Gaussian plume formula," given by⁸

$$\frac{\chi}{Q} = \frac{1}{\pi \sigma_y \sigma_z \bar{u}} \exp \left[-\frac{1}{2} \left(\frac{y^2}{\sigma_y^2} + \frac{h^2}{\sigma_z^2} \right) \right], \quad (19-7)$$

where

χ = ground-level air concentration (Ci/m³), for continuous release, Ci-s/m³ for "puff";

Q = source strength (Ci/s for continuous release, Ci for "puff");

$\bar{u}$ = average wind speed (m/s);

h = height of the source above ground (m);

y = lateral (crosswind) distance of the receptor from the plume axis (m);

σ_y = standard deviation of the cloud centerline concentration in the horizontal direction (m);

σ_z = standard deviation of the cloud centerline concentration in the vertical direction (m).

Here, σ_y , σ_z , and π are determined by the meteorological conditions at the time of release. The parameters σ_y and σ_z are a measure of the extent of diffusion and can be determined experimentally. Six meteorological categories have been suggested by Pasquill,⁸ as shown in Figs. 19-6 and 19-7. Among the various categories, category F is the most conservative since it yields the least diffusion and the highest concentration. For the dose calculations we assumed worst-case meteorological conditions (Pasquill type-F atmospheric stability, wind velocity of 1 m/s, and ground-level release). For category F, the values of σ_z and σ_y at the 1000-m distance from the source are about 14 and 28 m, respectively. From Eq. (19-7), the value of (X/Q) is about $6 \times 10^{-4} \text{ s/m}^3$ for these conditions.

Fig. 19-6. Standard deviation of the Gaussian plume concentration in the horizontal direction as a function of the distance from the source. The meteorological category for the plume stability is shown as a parameter.

Fig. 19-7. Standard deviation of the Gaussian plume concentration in the vertical direction as a function of the distance from the source. The meteorological category for the plume stability is shown as a parameter.

The results are shown in Fig. 19-4. The maximum external dose possible is about 1 rem assuming that the entire first wall melts and the ventilation stack HEPA filter is inoperative. (Melting of the entire first wall leads to volatilization of about 1% of the first-wall material under the assumptions stated above.) Consideration of the internal dose from inhaled material in a ground-level puff, calculated as outlined below for tritium, would add 2 to 3 rem. These doses are well below guidelines for fission reactor siting, which is 25 rem.⁹

19.7 RELEASE OF CORROSION PRODUCT ACTIVATION

Figure 19-8 shows the accident sequence for the release of activated coolant corrosion products. The LOCA is the only accident capable of activated coolant corrosion product release. It is expected that only a few modules can be affected by a LOCA. However, for the sake of conservatism and bounding of the possible dose, we have assumed a scenario in which all of the coolant in the first-wall and blanket is lost to the reactor building. Since the activated corrosion products in the building atmosphere are expected to be in the form of particulates of about 0.1 to 1 μm , they will be filterable

Fig. 19-8. Accident sequence for the release of activated coolant corrosion products.

with an efficiency of greater than 99.97%. We calculated the resulting (external total body) plant boundary dose using Eq. (19-6), where the summation now refers to all the activated corrosion products. We assumed that the composition of the radioactive nuclides in the coolant is identical to the HT-9, as opposed to preferential selection. Figure 19-8 shows that the maximum possible dose is about 3 rem, assuming the loss of the primary coolant from the entire coolant loop and an inoperative HEPA filter stack. The dose with the HEPA filter in place is reduced to 8×10^{-4} rem.

The harmful activity of the Li-Pb itself is considerably greater than that of the activated corrosion products, and is mostly the result of ^{203}Pb , ^{210}Pb , ^{203}Hg , ^{202}Tl , ^{207}Tl , ^{108}Ag , ^{110}Ag , ^{110m}Ag and ^{122}Sb . We have estimated the additional public consequences of the Li-Pb activity by considering a LOCA in which all of the Li-Pb is deposited on the steel-lined reactor building floor. The steel liner eliminates the possibility of a Li-Pb/concrete reaction. We have then estimated the vapor pressure of the Li-Pb in the reactor building, assuming a Li-Pb temperature of 500°C (the maximum Li-Pb temperature in the blanket). The result is less than 10^{-4} mm of Hg. Release of all this vapor (~ 120 g of Li-Pb) without HEPA filtering gives an additional plant boundary dose of about 24 mrem.

We have not included several possible physical mechanisms for releasing greater amounts of Li-Pb. The Li-Pb/water reaction produces hydrogen gas and a peak temperature of about 650°C , and can be expected to drive a significantly greater amount of radioactive material from the Li-Pb than the above estimate based on vapor pressure. In addition, the inertial effects of an Li-Pb spill may result in a greater number of suspended particulates in the reactor building atmosphere than predicted by the equilibrium vapor pressure. Release of 100% of the activity in the Li-Pb would give rise to a plant boundary dose of about 3600 rem (assuming, for example, loss of reactor building integrity). This dose is unrealistically high for several reasons:

1. Given that the reactor building is intact, the HEPA filter would be expected to reduce this dose to about 1 rem.
2. A more likely accident scenario for a LOCA would involve only those blanket modules serviced by a single steam generator.

3. Any aerosols that are generated would mostly settle or plate out on surfaces. A recent experiment on a lithium pool/air reaction showed that 5 to 10% of reacted lithium forms an aerosol (released as Li_2O). The near-aerosol particle sizes were $8\text{ }\mu\text{m}$. Initial aerosol concentrations of 18 gm/m^3 were measured. Within 2 hours the aerosol concentrations had decreased to $< 0.01\text{ gm/m}^3$ (without external agitation), or a reduction by a factor of 1800. In the case of $\text{Li}_{17}\text{Pb}_{83}$, no aerosol production was observed for an air reaction. However, given that aerosol particulates are produced (by whatever means), similar settling fractions can be expected.

Therefore, realistic estimates of the maximum possible dose at the plant boundary can be expected to be much lower than 3600 rem.

19.3 RELEASE OF TRITIUM

The tritium inventories for MARS are shown in Table 19-5. The most severe tritium-related accident in the reactor building would involve a LOCA in the direct converter and reflector/shield coolant loop, or the failure of the fuel pellet injectors. Both of the accident scenarios result in the release of at most about 22 g of tritium into the reactor building. The ISS contains 162 g of tritium, but is doubly contained by the use of a glovebox enclosure. For this reason, the consequence of an accident involving the ISS is considerably lower than systems without the glovebox enclosure, despite the larger tritium inventory in the ISS.

Given that a release of tritium into the reactor building has occurred, engineered safety features of MARS would minimize environmental release. First, the ATR system removes tritium from the building atmosphere. Second, the negative reactor building pressure minimizes tritium leakage. The worst-case accident for MARS would involve a power failure accompanied by the release of the vulnerable tritium inventory of $\sim 22\text{ g}$ from either the fuel pellet injectors or the H_2O coolant loop. This would render the ATR system temporarily inoperative, producing the loss of the negative containment pressure. In this section, we estimate the doses due to several possible worst-case accident scenarios.

The worst-case accident sequences identified for tritium release into the environment and the consequences are shown in Fig. 19-9. The doses at the plant boundary (1000 m) are calculated using

$$\text{DOSE} = A_T \times \left(\frac{X}{Q}\right) \times \text{BR} \times \text{DCF} \quad , \quad (19-8)$$

where

- A_T = activity of tritium (Ci);
- X/Q = atmospheric dispersion coefficient at 1000 m = $6 \times 10^{-4}\text{ s/m}^3$;
- BR = breathing rate (m^3/s);
- DCF = total body inhalation dose conversion factor.

Fig. 19-9. Worst-case accident sequences for tritium release into the environment.

Regulatory Guide 1.4⁷ gives χ/Q at 1000 m of 6×10^{-4} s/m³, and a post-accident breathing rate of 3.75×10^{-4} m³/s. This breathing rate is characteristic of the active portion of a working day. A rate of 5×10^{-4} m³/s is considered a somewhat accelerated breathing rate. In addition to the breathing rate, Ref. 10 suggests an additional absorption rate of tritium through the skin of 2.5×10^{-4} m³/s, giving an effective breathing rate of 6.24×10^{-4} m³/s. This effective breathing rate is about 50% of the rate due to inhalation, and assumes the tritium is in oxidized form. The DCF value used is 1.077×10^{-4} rem/ μ Ci.

Assuming the release of 22 g of tritium into the reactor building, and assuming that no overpressurization occurs, leads to a plant boundary dose of 6×10^{-2} mrem. We used volumetric building leak rate of 10 vol%/day. To put this leak rate into perspective, fission reactors built today are designed not to exceed a leak/rate of 0.1 vol%/day at overpressure levels of 20 to 60 psi. We have reduced the class of construction so that we would not exceed 10 vol%/day. This is a consequence of the inherent safety advantage of fusion reactors over fission reactors. A more serious accident would involve a power failure accompanied by the loss of negative pressure. We have assumed 15 min taken to restore power. Utility estimates for starting up auxiliary power diesel engines are approximately in the range of 1 min. This scenario leads to higher tritium leak rates until power is restored. The leak rate was again taken to be 10 vol%/day. The resultant dose is about 16 mrem.

A catastrophic accident involving failure of the reactor building followed by the release of all the tritium in the reactor building is about 53 g. Using the same dosimetry assumptions and 1000-m distance, the total resulting dose is 21 rem.

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Radioactivity, Activation, and Waste Disposal

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20. Radioactivity, Activation, and Waste Disposal

20.1 INTRODUCTION

We have investigated several activation-related issues as they pertain to the MARS reactor study. They include the activity, afterheat, and biological hazard potential of the central cell, waste disposal of the various reactor systems, and ^{210}Po production in the coolant.

A knowledge of the amount of central cell afterheat is needed to estimate the cooling required after shutdown and also to determine the consequences of a pump failure or pipe rupture. The biological hazard potential (BHP) of the blanket is used to calculate the potential effects of the release of some radioactive material in the event of an accident.

A long-term analysis of the radioactivity in the blanket is also needed to evaluate methods for disposing of reactor components. If the wastes contain mostly short-lived radioactive isotopes, it is possible to dispose of them by near-surface burial. Because this is a more economical method of burial, our studies have been aimed at finding materials that can be disposed of in this manner. At least as important is the removal of the potential detriment to fusion power generation if it were perceived to require deep geologic burial of high-level radioactive waste.

The activation of the coolant and transport of activated corrosion product are two other important issues. Although we have not considered corrosion product transport in this study, we have evaluated the coolant activation problem, specifically concentrating on the production of ^{210}Po from the lead and the bismuth impurity in the Li-Pb.

Other activation-related issues have been discussed in previous sections. These include the end cell dose (Sec. 11.2 of this volume) and the dose at the edge of the shield (Sec. 13.6 of this volume).

20.2 ACTIVATION ANALYSIS

20.2.1 Introduction

We used the neutron fluxes from Sec. 12.6 and 13.4 of this volume along with the DKR code to calculate the activity in the blanket and shield regions after 2 FPY. The results of these calculations are summarized in Figs. 20-1 through 20-3.

20.2.2 Activity

The activity vs time curve of the blanket and shield is given in Fig. 20-1. A list of the isotopic activities is given in Table 20-1. There is a fairly rapid initial decrease corresponding to the decay of ^{56}Mn ($\tau_{1/2} = 2.58$ hours). After this, the activity is dominated by ^{54}Mn ($\tau_{1/2} = 312$ days) and ^{55}Fe ($\tau_{1/2} = 2.7$ years). The activity at long times is dominated by the radioisotopes resulting from the activation of molybdenum, specifically ^{93}Mo ($\tau_{1/2} = 3.5 \times 10^3$ years), ^{92}Nb ($\tau_{1/2} = 3.2 \times 10^7$ years), and ^{99}Tc ($\tau_{1/2} = 2.14 \times 10^5$ years).

Fig. 20-1. Activity per thermal watt of the blanket and shield vs time after shutdown for 2 FPY of operation.

Fig. 20-2. Biological hazard potential of the blanket and shield vs time after shutdown for 2 FPY of operation.

Fig. 20-3. Percent afterheat (in units of MW afterheat per MW thermal x100) of the blanket and shield vs time after shutdown for 2 FPY of operation.

Table 20-1. Isotopic activities in the MARS blanket, reflector, and shield at shutdown (2-full power years operation).

Isotope	$\tau_{1/2}$	Blanket tube zone (Ci/cm ³)	Blanket beam zone (Ci/cm ³)	Reflector (Ci/cm ³)	Shield (Ci/cm ³)
¹⁶ N	7.1 s	3.9×10^{-4}	5.4×10^{-5}	2.3×10^{-4}	1.0×10^{-8}
²⁴ Na	15.02 h	--	--	--	7.7×10^{-7}
²⁷ Mg	9.45 m	--	--	--	3.5×10^{-7}
²⁶ Al	7.3×10^5 y	--	--	--	1.9×10^{-14}
²⁸ Al	2.24 m	--	--	--	1.6×10^{-6}
⁵¹ Ti	5.75 m	1×10^{-3}	1.4×10^{-4}	4.0×10^{-7}	2.0×10^{-11}
⁴⁹ V	331 d	1.3×10^{-2}	1.8×10^{-3}	4.7×10^{-6}	2.1×10^{-10}
⁵² V	3.76 m	0.29	0.042	3.0×10^{-3}	1.4×10^{-7}
⁴⁹ Cr	42 m	5.4×10^{-3}	7.0×10^{-4}	1.8×10^{-6}	6.7×10^{-11}
⁵¹ Cr	27.7 d	1.1	0.17	6.6×10^{-3}	2.6×10^{-7}
⁵⁵ Cr	3.55 m	7.4×10^{-3}	1.1×10^{-3}	4.3×10^{-3}	2.1×10^{-7}
⁵³ Mn	3.8×10^6 y	1.1×10^{-7}	1.5×10^{-4}	2.2×10^{-9}	9.3×10^{-14}
⁵⁴ Mn	312 d	0.52	0.076	0.058	2.6×10^{-6}
⁵⁶ Mn	2.58 h	2.6	0.44	13.	1.1×10^{-4}
⁵⁷ Mn	1.59 m	0.037	5.4×10^{-3}	8.3×10^{-4}	4.2×10^{-8}
⁵³ Fe	8.53 m	0.02	2.4×10^{-3}	3.3×10^{-4}	1.1×10^{-8}
⁵⁵ Fe	2.7 y	4.1	0.54	0.16	3.7×10^{-6}
⁵⁹ Fe	44.6 d	4.6×10^{-5}	9.9×10^{-5}	6.1×10^{-3}	4.2×10^{-8}
⁵⁷ Co	271 d	0.012	1.7×10^{-3}	1.0×10^{-3}	4.4×10^{-8}
⁵⁸ Co	70.8 d	0.039	5.8×10^{-3}	3.6×10^{-3}	1.9×10^{-7}
⁶⁰ Co	5.27 y	3.1×10^{-3}	4.2×10^{-4}	2.5×10^{-4}	1.0×10^{-8}
^{60m} Co	10.48 m	8.2×10^{-3}	1.1×10^{-3}	6.0×10^{-4}	2.0×10^{-8}
⁶¹ Co	1.65 h	2.1×10^{-4}	3.0×10^{-5}	1.9×10^{-5}	8.8×10^{-10}
⁵⁷ Ni	36.1 h	2.7×10^{-3}	3.6×10^{-4}	2.2×10^{-4}	7.7×10^{-9}
⁵⁹ Ni	8×10^4 y	2.4×10^{-8}	3.9×10^{-9}	1.9×10^{-7}	1.4×10^{-12}
⁶³ Ni	100 y	1.4×10^{-5}	4.0×10^{-6}	2.7×10^{-4}	1.5×10^{-9}
⁶⁵ Ni	2.52 h	4.1×10^{-6}	7.3×10^{-6}	5.8×10^{-4}	4.9×10^{-9}
⁸⁹ Zr	78.5 h	9.7×10^{-4}	1.4×10^{-4}	--	--

Table 20-1. (continued).

Isotope	$T_{1/2}$	Blanket tube zone (Ci/cm ³)	Blanket beam zone (Ci/cm ³)	Reflector (Ci/cm ³)	Shield (Ci/cm ³)
⁹³ Zr	1.5 x 10 ⁶ y	3.3 x 10 ⁻⁷	4.8 x 10 ⁻¹¹	--	--
⁹⁵ Zr	64 d	2.6 x 10 ⁻⁴	3.7 x 10 ⁻⁵	--	--
⁹⁷ Zr	16.8 h	5.4 x 10 ⁻⁵	7.7 x 10 ⁻⁶	--	--
⁹² Nb	3.2 x 10 ⁷ y	9.3 x 10 ⁻¹³	1.2 x 10 ⁻¹³	--	--
^{92m} Nb	10.13 d	2.1 x 10 ⁻³	3.1 x 10 ⁻⁴	--	--
^{93m} Nb	13.6 d	2.0 x 10 ⁻⁶	2.1 x 10 ⁻⁶	--	--
⁹⁴ Nb	2 x 10 ⁴ y	5.3 x 10 ⁻⁸	7.4 x 10 ⁻⁹	--	--
⁹⁵ Nb	3.50 d	7.7 x 10 ⁻⁴	1.1 x 10 ⁻⁴	--	--
^{95m} Nb	3.61 d	3.5 x 10 ⁻⁴	5.1 x 10 ⁻⁵	--	--
⁹⁶ Nb	23.4 h	5.3 x 10 ⁻⁴	7.5 x 10 ⁻⁵	--	--
⁹⁷ Nb	73.6 m	2.6 x 10 ⁻⁴	3.9 x 10 ⁻⁵	--	--
^{97m} Nb	54 s	1.1 x 10 ⁻⁴	1.6 x 10 ⁻⁵	--	--
⁹⁸ Nb	51.1 m	2.5 x 10 ⁻⁴	3.5 x 10 ⁻⁵	--	--
¹⁰⁰ Nb	3.1 s	3.2 x 10 ⁻⁵	4.5 x 10 ⁻⁶	--	--
⁹¹ Mo	15.5 m	9.7 x 10 ⁻³	1.3 x 10 ⁻³	--	--
⁹³ Mo	3.5 x 10 ³ y	4.7 x 10 ⁻⁵	4.9 x 10 ⁻⁵	--	--
⁹⁹ Mo	66 h	0.053	0.036	--	--
¹⁰¹ Mo	14.6 m	0.042	8.0 x 10 ⁻³	--	--
⁹⁹ Tc	2.14 x 10 ⁵ y	3.5 x 10 ⁻⁷	2.3 x 10 ⁻⁷	--	--
^{99m} Tc	6.02 h	0.053	0.036	--	--
¹⁰¹ Tc	14.2 m	0.042	8.0 x 10 ⁻³	--	--
¹⁸¹ Hf	42.4 d	1.5 x 10 ⁻⁵	2.0 x 10 ⁻⁶	--	--
¹⁸³ Hf	64 m	1.4 x 10 ⁻⁵	1.9 x 10 ⁻⁶	--	--
¹⁸² Ta	115 d	3.8 x 10 ⁻⁵	5.2 x 10 ⁻⁶	--	--
¹⁸³ Ta	5.0 d	4.0 x 10 ⁻⁵	5.5 x 10 ⁻⁶	--	--
¹⁸⁴ Ta	8.7 h	3.3 x 10 ⁻⁵	4.5 x 10 ⁻⁶	--	--
¹⁸⁵ Ta	49 m	5.0 x 10 ⁻⁷	1.3 x 10 ⁻⁷	--	--
¹⁸⁶ Ta	10.5 m	1.8 x 10 ⁻⁵	2.4 x 10 ⁻⁶	--	--
¹⁸¹ W	121 d	0.022	3.3 x 10 ⁻³	--	--

Table 20-1. (continued).

Isotope	$\tau_{1/2}$	Blanket tube zone (Ci/cm ³)	Blanket beam zone (Ci/cm ³)	Reflector (Ci/cm ³)	Shield ³ (Ci/cm ³)
¹⁸⁵ W	75.1 d	0.039	0.022	--	--
¹⁸⁷ W	23.9 h	0.011	0.044	--	--
²⁰³ Hg	46.6 d	0.016	2.7×10^{-8}	--	6.4×10^{-9}
²⁰⁵ Hg	5.2 m	0.017	2.8×10^{-3}	--	6.1×10^{-9}
²⁰⁴ Tl	3.77 y	4.4×10^{-4}	8.8×10^{-5}	--	2.1×10^{-10}
²⁰³ Pb	52.1 h	2.1	0.25	--	4.9×10^{-7}
²⁰⁵ Pb	1.4×10^7 y	1.5×10^{-6}	1.8×10^{-7}	--	3.5×10^{-13}
<hr/>					
Total zone activity per unit volume (Ci/cm ³)		9.07	1.28	13.2	1.21×10^{-4}
Volume of zone per cm of central cell length (cm ³)		8.396×10^3	1.068×10^4	4.246×10^4	5.116×10^4
Total zone activity per cm of central cell length (Ci/cm)		7.62×10^4	1.37×10^4	5.60×10^5	6.19
Total zone activity for a 130 m long central cell (Ci)		9.9×10^8	1.78×10^8	7.28×10^9	8.05×10^4

20.2.3 Biological Hazard Potential

The isotope ^{56}Mn produces most of the BHP at shutdown. The balance is produced mainly by ^{55}Fe and ^{54}Mn . Since ^{56}Mn has such a short half-life (2.58 hours), the BHP drops about 85% after 1 day as shown in Fig. 20-2. The BHP remains at that level until the decay of the ^{54}Mn ($t_{1/2} = 312$ days) causes it to decrease about 1 year after shutdown. During the period from 15 to 500 years, the BHP is dominated by ^{63}Ni and later by ^{93}Nb .

The case of the lithium-lead (Li-Pb) coolant is a special one. Approximate calculations performed after the completion of the MARS study indicate that contact dose rates from the activated Li-Pb may be as high as several hundred rem/hour.¹ The biological hazard comes from impurities in the Li-Pb as well as from activation of the Li-Pb itself. Primary contributors to the dose rate one day after removal from the neutron flux are the ^{110}mAg , ^{122}Sb , and the ^{124}Sb from impurities and ^{202}Tl , ^{203}Pb , and ^{207}Bi from the lead. Activity at long times is dominated by radioisotopes resulting from silver impurities (^{108}Ag and ^{108m}Ag) and from the lead (^{207}Bi and ^{208}Bi).

20.2.4 Afterheat

The afterheat vs time curve after shutdown is shown in Fig. 20-3. The initial primary contributing isotopes are the same as for the BHP and activity ^{56}Mn , ^{54}Mn and ^{55}Fe . In this case, however, the percentage of the afterheat (MW afterheat per MW thermal) resulting from ^{56}Mn is even greater (~99%). The long term afterheat is governed by the decay of ^{63}Ni and ^{93}Mo .

20.3 WASTE MANAGEMENT

20.3.1 Introduction

We have investigated long term waste disposal and management for the MARS blanket, direct converter, normal magnet, and Li-Pb coolant. Because there are currently no specific regulations governing fusion wastes, we have assumed that it can be treated using the same requirements for fission waste disposal. Future regulations can be expected to depend on the waste form, isotopic composition, and levels of activity, and should be independent of whether the waste is fission or fusion based. We have therefore investigated the waste problem using the federal regulations found in 10CFR61² as a guideline. This regulation assumes that the site will be under administrative control for 100 years. It requires that the dose to an inadvertent intruder on the site (after the 100-year institutional period) be less than 500 mrem/year. This means that isotopes with half-lives of less than about 5 years pose no hazard since they will have decayed to negligible levels by the time institutional control is removed. Isotopes with half-lives longer than 5 years can be hazardous after 100 years.

The isotopes listed in 10CFR61 that are of concern in fusion waste disposal are tritium, ^{14}C , ^{59}Ni , ^{63}Ni , ^{60}Co , ^{94}Nb , and ^{99}Tc . In 10CFR61 these isotopes are given limits for the various classes for waste disposal. Other long-lived isotopes are produced in a fusion reactor and the method by which these are to be treated is currently a topic of controversy. The 10CFR61

regulations specify that all isotopes not mentioned should be classified as Class A waste. This, however, is not the way that these isotopes are treated in most waste disposal studies. Instead, the limits on the isotopes that are not listed are determined by using the limit of the listed isotope that most closely duplicates its decay mode, gamma spectrum, and half-life.

Because reactor materials are composed of a wide range of elements and because they will transmute to a large number of radioisotopes, an index system, called the Waste Disposal Rating (WDR), has been developed³ as a guide to designers in evaluating how easily a particular material can be disposed of. The WDR assigns an index to each material by calculating

$$WDR = \sum_i \frac{\text{specific activity}(i)}{\text{limit}(i)},$$

where the summation is over all isotopes i and $\text{limit}(i)$ is the maximum allowable concentration of the i th isotope as specified in 10CFR61 or as interpreted by the investigator. A list of the limits used in this study is given in Table 20-2. The definition of the WDR was suggested by the regulation that governs the determination of allowable concentrations of isotopes in mixtures. If the WDR is greater than 1, the material cannot be disposed of by near-surface burial (unless diluted). If the WDR is greater than 0.1 but less than 1, the waste is termed Class C intruder waste. This waste must be packaged and buried with the idea that it will still pose a hazard after the 100-year institutional period is over. The specific packaging and burial requirements are given in 10CFR61. If the WDR is less than 0.1, the waste is Class A segregated waste. The major hazard of this waste is to the people who are concerned with processing and burying it. It must meet less stringent packaging and burial requirements and is not considered to be a hazard to an inadvertent intruder after the 100-year institutional period is over.

The WDR gives a guideline to meet the goal of near surface disposal. If materials with WDRs greater than 1 are used to enhance the performance of the reactor, then the waste must somehow be processed if Class C near-surface burial is desired. The magnitude of the WDR gives the dilution factor required to allow Class C near-surface disposal. It is possible, by inspecting the list of isotopes that contribute to the WDR, to alter the composition of materials and therefore lessen the overall WDR (e.g., by substituting V for Mo). This alteration of the composition must be critically examined because it can affect the reliability, lifetime, and other properties of the material. The search for these materials would force the manufacturers to make trade-offs related to their chemical, mechanical, or magnetic properties. For example, the developer of these materials must make certain that in the process of minimizing long term wastes he does not create severe short-term activation problems that could cause safety problems and difficulty in regular maintenance.

Another option for lowering the WDR is the isotopic tailoring of existing materials. The absence of the natural isotopes that produce the long-lived radioisotopes would ensure that the WDR would be less than 1. For example, if ⁹⁶Mo were the only isotope of molybdenum used in an alloy, the long-lived activation produced (for ⁹³Mo, ⁹²Nb, ⁹⁴Nb, ⁹⁷Tc, ⁹⁸Tc, and ⁹⁹Tc)

Table 20-2. Maximum specific activity limits for common nuclides.

Isotope	$T_{1/2}$ (years)	Decay mode	Specific activity limits for Class-C near-surface burial (Ci/m^3)	
			Old limit	New limit
^3H	12.33	$\beta^- (0.019)$, no γ	TMSA ^a	TMSA ^a
^{14}C	5730	$\beta^- (0.156)$, no γ	8	80
^{53}Mn	3.8×10^6	EC, no γ	22	A/700 ^b
^{59}Ni	7.5×10^4	EC, no γ	22	220
^{60}Co	5.26	β^- , $\gamma(1.17, 1.33)$	TMSA ^a	A/700 ^b
^{63}Ni	100.1	$\beta^- (0.06)$, no γ	700	700
^{93}Zr	1.5×10^6	$\beta^- (0.04)$, $\gamma(0.03)$	0.02	A/700 ^b
^{93}Mo	3.5×10^3	EC, $\gamma(0.03)$	22	A/220 ^b
^{94}Nb	2×10^4	$\beta^- (0.47)$, $\gamma(0.87)$	0.02	0.2
^{90}Sr	29	$\beta^- (0.54)$, no γ	7000	7000 ^b
^{99}Tc	2.13×10^5	$\beta^- (0.293)$, $\gamma(0.09)$	3	3
^{129}I	1.59×10^7	$\beta^- (0.15)$, $\gamma(0.04)$	0.08	0.08
^{135}Cs	2.3×10^6	$\beta^- (0.21)$, no γ	840	A/700 ^b
^{137}Cs	30.17	$\beta^- (0.5, 1.17)$, $\gamma(0.66)$	46,000	4600

^aTheoretical maximum specific activity.

^bNot mentioned specifically. Taken to be either Class A (as in the regulations) or, more conservatively, Class C with a specific activity limit of 700 or 220 Ci/m^3 .

would be small enough that the WDR for the molybdenum alone would be low enough for near-surface burial. Isotopic tailoring will have little or no effect on the application of the material, but the process will require careful assessment regarding its overall economic viability. A preliminary assessment indicates that isotopic tailoring of low-percentage alloying elements such as molybdenum (1%) in HT-9 is both feasible and relatively low cost.⁴ It would result in a 13% increase in the cost of the blanket, but would make a compacted MARS blanket suitable for near-surface burial as Class C waste.

If neither of the above options is viable for reducing the WDR to less than 1, it is possible to dilute the waste by filling voids and/or encasing the material to a point where the overall WDR is less than 1.

If the dilution method also fails, it is possible to chemically separate the long-lived isotopes and dispose of them in deep geologic burial sites while retaining most of the bulk volume for near-surface burial.

20.3.2 Blanket

Table 20-3 contains our calculated specific activities for the principal long-lived blanket isotopes. We calculated them by first doing a one-dimensional ANISN⁵ calculation to obtain a flux. We then used this flux with the DKR⁶ code to calculate the activity. Figure 13-7 in this volume shows the geometry that was used for the calculation. Note that two values are given for the ⁹³Mo partial WDR. This is because of uncertainties in the interpretation of 10CFR61. We decided to use the more conservative value, and our interpretation of the results is based on that value. The overall waste disposal rating (if crushed) is 8.89 (4.35 if ⁹³Mo is not included). If the blanket is filled with concrete, the WDR is 0.614 because the structure occupies only 7.14% of the volume. Because the WDR is less than 1, the waste would be Class C. Isotopic tailoring of the waste could result in a WDR less than 0.1, and would therefore become Class A waste.

Table 20-3. Specific activities for HT-9 in Li-Pb blanket.

Isotope	Tube zone	Beam zone	Overall blanket	Class C specific activity limit
⁵³ Mn	3.20	0.21	1.02	Class A
⁵⁹ Ni	0.70	0.053	0.0226	220
⁶³ Ni	628	54	208	700
⁹³ Zr	0.0094	6.4×10^{-4}	0.003	Class A
⁹³ Mo	1360	660	860	Class A (220)
⁹⁴ Nb	1.5	0.099	0.478	0.2
⁹⁹ Tc	9.86	3.16	4.98	3

20.3.3 Direct Converter

We also considered the disposal of the direct converter. The WDRs for the principal contributing isotopes are shown in Table 20-4 for a 24-year irradiation period and in Table 20-5 for a 12-year irradiation period. The calculation was done by first using a Monte Carlo (MCNP) code to calculate the flux and then using DKR to obtain the activity. In the case of 12-year irradiation period, the waste would be Class A. For the 24-year irradiation period, the waste for the inner collector alone would be Class C. However, it is easily converted to Class A by mixing the inner and outer collector material without any dilution.

Table 20-4. Disposal of the direct converter after the 24-year irradiation period.

Isotope	Inner collector (Ci/m ³)	Outer collector (Ci/m ³)	Halo scraper (Ci/m ³)	Class C legal limit (Ci/m ³)	Legal limit (%)
⁹³ Mo	1.31 x 10 ¹	1.05 x 10 ⁰	7.41 x 10 ⁻¹	None (220)	0 (5.9)
⁹⁴ Nb	1.5 x 10 ⁻²	4.25 x 10 ⁻⁵	7.07 x 10 ⁻⁵	0.2	7.5
⁹⁹ Tc	1.04 x 10 ⁻²	1.75 x 10 ⁻³	1.33 x 10 ⁻³	3	0.3
				Total	7.8 (13.7)

Table 20-5. Disposal of the direct converter after the 12-year irradiation period.

Isotope	Inner collector (Ci/m ³)	Outer collector (Ci/m ³)	Halo scraper (Ci/m ³)	Class C legal limit (Ci/m ³)	Legal limit (%)
⁹³ Mo	6.53 x 10 ⁰	5.25 x 10 ⁻¹	3.72 x 10 ⁻¹	None (220)	0 (2.9)
⁹⁴ Nb	7.50 x 10 ⁻³	2.13 x 10 ⁻⁵	3.54 x 10 ⁻⁵	0.2	3.75
⁹⁹ Tc	5.20 x 10 ⁻³	8.74 x 10 ⁻⁴	6.61 x 10 ⁻⁴	3	0.15
				Total	3.9 (6.8)

20.3.4 Lithium-Lead Coolant

We have also investigated the WDR of the Li-Pb coolant by running a one-dimensional ANISN code to calculate the flux in the Li-Pb regions and then using DKR to obtain the total activity. This number was then multiplied by the fraction of the Li-Pb resident in the blanket at any time (i.e., 13.6% of the total Li-Pb in the primary loop). A list of the impurity isotopes of the lead and their weight percent is given in Table 20-6. Table 20-7 shows the principal contributing isotopes to the WDR of the Li-Pb. The total WDR is 0.17; therefore, the waste would be Class C. Note that in the calculation we estimated the specific activity limit of ^{108}Ag to be 70 Ci/m^3 based on consideration of the limits for ^{137}Cs , ^{63}Ni , and ^{94}Nb . Because this is the largest contributor to the WDR, the value chosen for this limit plays a large part in the determination of the overall WDR. Subsequent calculations indicate that it may be necessary to remove the silver isotopes and the bismuth isotopes from the Li-Pb in order to meet the Class C burial requirements.¹

Table 20-6. Electrolytic lead and its impurities (in wt%).

Lead	99.994
Bismuth	0.0042
Silver	0.001
Antimony	0.0003
Copper	0.0002

20.3.5 Normal-Conducting Insert Coil

A one-dimensional ANISN run followed by a DKR run was performed to calculate the WDR of the normal magnet. The principal contributing isotopes are given in Table 20-8. We calculated the WDR to be 12.5. The lifetime of the normal magnet is about 3.3 FPY and therefore about 16 irradiated coils will need to be disposed of at the end of the reactor life (24 FPY) and each will require a dilution of about 12.5. Fortunately, the volume of each coil is quite small. The WDR is so high because of the ^{63}Ni produced from the ^{63}Cu in the magnet.

20.3.6 Conclusions

In our waste disposal studies we have determined the WDR of several sections of the reactor. The direct converter and Li-Pb coolant do not appear to present any waste disposal problems; both can be disposed of using near-surface burial. The blanket does not inherently meet the near-surface burial criteria. However, if it is encased and filled with concrete, the WDR will be less than 1. Alternatively, isotopic tailoring of the molybdenum will allow the blanket to meet the near-surface burial criteria. The normal coil is different. It is highly activated and may require deep geologic burial if the dilution factor requirement of about 12.5 cannot be met.

Table 20-7. Disposal of impurities found in the Li-Pb coolant.

Isotope	Parent element	$\tau_{1/2}$ (years)	Specific activity (Ci/m ³)	Specific activity limit (Ci/m ³)
⁶⁰ Co	Cu	5.26	0.565	TMSA/700
⁶³ Ni	Cu	125	0.328	700
^{108m} Ag	Ag	130	5.60	70 ^a
^{110m} Ag	Ag	0.69	8.43	TMSA/700 ^b
²⁰⁵ Pb	Pb	1.4×10^7	1.25	TMSA/700 ^c
²⁰⁸ Bi	Bi	3.68×10^5	1.52×10^{-2}	0.2 ^d

^aBased on consideration of limits for ¹³⁷Cs, ⁶³Ni, and ⁹⁴Nb. Biggest cause of uncertainty in WDR.

^bBased on ⁶⁰Co.

^cNo γ emission even though long $\tau_{1/2}$.

^dBased on ⁹⁴Nb.

20.4 POLONIUM PRODUCTION

20.4.1 Introduction

The polonium produced in the MARS reactor arises from two principal reactions: (1) $^{209}\text{Bi}(n,\gamma)^{210}\text{Bi} \xrightarrow{\beta} ^{210}\text{Po}$, and (2) $^{208}\text{Pb}(n,\gamma)^{209}\text{Pb} \xrightarrow{\beta} ^{209}\text{Bi}(n,\gamma)^{210}\text{Bi} \xrightarrow{\beta} ^{210}\text{Po}$. We considered these two reactions separately. We also separated the calculation into beam and tube zones.

20.4.2 Tube Zone

The first reaction in the tube zone (closest to the plasma) produces 3.53×10^{15} atoms of ²¹⁰Po/s, assuming that a state of equilibrium has been reached, i.e., the ²¹⁰Bi concentration has increased to its saturated level (this occurs several hours after irradiation begins). The number of atoms of Po produced/s is equal to 5.54×10^{-3} Ci of ²¹⁰Po activity added/s in the entire tube zone. In other words, this is the amount of ²¹⁰Po produced by the Bi impurity in Li-Pb in the tube zone.

We split the second reaction into two parts. If the first part of the reaction ($^{208}\text{Pb}(n,\gamma)^{209}\text{Pb} \xrightarrow{\beta} ^{209}\text{Bi}$) is calculated, information from the first reaction can be used to calculate the second part of the second reaction

Table 20-8. Transmutation of the normal coil.

Isotope	Specific activity (Ci/m ³)	$\tau_{1/2}$ (years)	Specific activity limit (Ci/m ³)	WDR
⁵³ Mn	0.0324	3.8×10^6	700	∞
⁵⁹ Ni	0.32	220	220	∞
⁶³ Ni	8665.	125	700	12.4
⁹³ Mo	7.42	3.5×10^3	TMSA/700	∞
⁹⁴ Nb	6.04×10^{-3}	2×10^4	0.2	0.03
⁹³ Zr	2×10^{-3}	1.5×10^6	TMSA/700	∞
⁹⁹ Tc	0.03	2.13×10^5	3	0.01
²⁶ Al	0.04	7.3×10^5	TMSA/700	∞
⁶⁰ Co	4.3×10^4	5.26	TMSA/700	0
⁹⁰ Sr	0.02	29	7000	∞
^{108m} Ag	5.0	130	70	0.07
			Total WDR	12.5

(²⁰⁹Bi(n,γ) ²¹⁰Bi β → ²¹⁰Po). In other words, if we determine the amount of additional ²⁰⁹Bi that is produced, we can then determine the amount of Po produced (by both reactions) by multiplying the initial production rate of ²⁰⁹Bi by one plus the ratio of ²⁰⁹Bi produced to the ²⁰⁹Bi impurity initially present in the Li-Pb.

We calculated that 2.784×10^{18} atoms of ²⁰⁹Bi were produced/s in the tube zone. Because 1.056×10^{26} atoms of ²⁰⁹Bi are original in the tube zone as an impurity in the Li-Pb, the equation for curies of ²¹⁰Po produced/s,

$$Ci(^{210}\text{Po}) \text{ added/s} = Ci \text{ from reaction 1} + Ci \text{ from reaction 1}$$

$$\times \frac{\text{number density of added } ^{209}\text{Bi}}{\text{number density of } ^{209}\text{Bi initially}} \times \text{the fraction of } ^{209}\text{Pb being irradiated}$$

and

number density of added ^{209}Bi = production rate of ^{209}Bi

x time of irradiation .

In the case of the tube zone, this yields

$$\text{Ci}(^{210}\text{Po}) \text{ added/s} = 5.54 \times 10^{-3} \left[1 + \frac{2.784 \times 10^{18} t(0.136)}{1.056 \times 10^{26}} \right],$$

where t is the time of irradiation in seconds, and (0.136) = fraction of Li-Pb being irradiated. This equation indicates that the length of time required for the ^{210}Po production rate to double is 8.8 years.

20.4.3 Beam Zone

In the beam zone, reaction 1 produces 2.567×10^{14} atoms ^{210}Po /s or 4.4×10^{-4} Ci of ^{210}Po added/s. Using the same methodology as in the tube zone, we arrive at the equation

$$\text{Ci}(^{210}\text{Po}) \text{ added/s} = 4 \times 10^{-4} + 4 \times 10^{-4} \left(\frac{4.916 \times 10^{18}}{1.5 \times 10^{26}} \right) (0.136)t .$$

The time required for the production rate of the ^{210}Po to double is 18 years.

20.4.4 Polonium Inventory and Cleanup

The T recovery system can also be used as the Po cleanup system. The vapor pressure of Po is 2 Torr at 500°C . Therefore, the evaporation process will remove Po from the coolant.

The Po decay and evaporation rate should be equal to the Po production rate which is within a factor of a few of 6×10^{-3} Ci/s, or 1.34×10^{-6} g/s, or

$$\frac{dm}{dt} + \frac{v n A}{4} = 1.34 \times 10^{-6} \text{ g/s} , \quad (20-1)$$

in which dm/dt is the decay rate, v is molecular velocity for Po vapor, n is particle density in vapor, and A is surface area for evaporation. Since the half-life of Po is 138 days, then

$$\frac{dm}{dt} = m \left(\frac{0.693}{138 \times 3600 \times 24} \right) \text{ g/s} . \quad (20-2)$$

The $\text{Li}_{17}\text{Pb}_{83}$ flow rate is 1.4×10^4 kg/s, particle radius is 0.1 cm, the residence time is 0.5 s, and the total surface area of evaporation is $2.2 \times 10^7 \text{ cm}^2$.

The Po velocity can be calculated by

$$v = \left(\frac{8 RT}{\pi M} \right)^{1/2} = 2.8 \times 10^4 \text{ cm/s} , \quad (20-3)$$

in which R is the gas constant, T is the temperature, and M is the molecular weight.

The particle density can be calculated by

$$n = \frac{MP}{RT} = \frac{Mx \gamma P_o}{RT} = \frac{M(m/M)}{(m_o/M_o)} \gamma P_o / RT, \quad (20-4)$$

in which P is the partial pressure of Po in Li-Pb, P_o is the vapor pressure of pure Po, γ is the activity coefficient of Po in Li-Pb, x is the mole fraction, m is the Po mass, M is the Po molecular weight (=210), m_o is the Li-Pb mass ($=1.46 \times 10^{10}$ g), and M_o is the Li-Pb molecular weight (=173). By combining Eqs. (20-1) through (20-4), we obtain

$$m [5.8 \times 10^{-8} + 3.8 \times 10^{-5} \gamma P_o] = 1.3 \times 10^{-6}.$$

If Po in Li-Pb is an ideal solution ($\gamma = 1$), then $m = 0.02$ g, or 90 Ci. However, the activity coefficient of Po in bismuth has been measured⁷ and can be calculated by

$$\log_{10} \gamma = -\frac{2728.3}{T} + 1.1176.$$

If the activity of Po in Li-Pb is the same as that in Bi, then $\gamma = 3.9 \times 10^{-3}$. Then, $m = 4.5$ g or 2×10^4 Ci.

Therefore, we have estimated the Po inventory in Li-Pb to be between 90 to 20,000 Ci depending on the activity coefficient. Early in the reactor lifetime, the tritium recovery system will receive 6×10^{-3} Ci/s because the decay rate of Po is small compared with the evaporation rate. This value will increase as the reactor operates; it will double in 8 to 9 years and triple in 16 to 18 years.

20.4.5 Conclusions

The total (tube + beam zone) amount of activity produced from reaction 1 is 6×10^{-3} Ci of $^{210}\text{Po}/\text{s}$. The total (tube + beam zone) amount of activity produced through reaction 2 could be expressed as a function of the amount produced from reaction 1, the time of irradiation and the ratio of ^{209}Bi produced to that present as an impurity in the Li-Pb. This yields $\approx 6 \times 10^{-3}$ (t/8) Ci of Po added/s (t is the time of irradiation in years). The total amount of polonium activity added per second throughout the entire blanket is

$$\text{Ci}_{\text{Po}} \text{ added/s} \approx 6 \times 10^{-3} + 6 \times 10^{-3} \times \frac{t(\text{years})}{8}.$$

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21 Instrumentation and Control

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21. Instrumentation and Control

21.1 INTRODUCTION

The MARS instrumentation and control (I&C) system will meet many of the same requirements as I&C systems in nuclear fission power plants because it will have some similar safety and functional licensing requirements. It will also have significant differences from fission and fossil plant I&C systems because fusion reactor control, including startup and shutdown, will be entirely automatic and will include more complex subsystems. Control of a fusion power plant must shift from the current analog systems to digital control because of the extremely short time constants associated with plasma startup and control.

Development of the I&C systems for MARS will require the design, verification, validation, testing, and licensing of diagnostic, control, and safety systems using digital controls that do not yet have standards against which design, reliability, and redundancy requirements can be established. In addition, diagnostic and control instruments and sensors have not been demonstrated in the high energy neutron fluxes produced by the DT-burning plasma, nor have feedback control algorithms or requirements been developed for interfacing the plasma and its auxiliary systems. Modes of plasma operation for a fusion reactor system are not well defined. It is clear, however, that plasma startup, hot standby, turbine/generator startup, transient run, steady-state, emergency shutdown, and normal shutdown must be accommodated by the I&C system. Work needs to be done to define the control system interaction with plasma parameters.

The overall plant instrumentation and control systems fall into three general categories: reactor control, balance-of-plant (including cryogenic and tritium plants), and computer systems to integrate local control at components and subsystems with the overall plant supervisory control. The I&C system for the balance of a fusion plant is not significantly more complex or sophisticated than that of current fission plants (except for startup). The MARS I&C is particularly similar to the I&C systems for liquid-metal fast breeder reactors. Therefore, the remainder of this section will focus on reactor control and instrumentation and supporting computer requirements. Instrumentation and control (I&C) for the reactor will be discussed in the general categories of structures, magnets, rf equipment, neutral beams, cryogenics, heat transport, vacuum impurity control, and direct conversion systems.

21.2 OVERALL I&C REQUIREMENTS

The overall requirements of the I&C system for a commercial fusion power station will be to provide for the safe and economical operation of the reactor, balance-of-plant, and system diagnostics, as well as to integrate the reactor into the power distribution grid. From the standpoint of functional operation, the I&C system can be divided into safety and non-safety-related systems.

Safety systems are those whose actions are: (1) necessary to protect the integrity of radioactive material barriers and/or prevent the release of radioactive material; (2) necessary to mitigate loss of coolant accidents; and

(3) necessary to protect the health and safety of the public and the plant operating personnel. These subsystems may be components, groups of components, or groups of such systems. A list of these systems is shown in Table 21-1.

Non-safety-related systems (including power generation) are those systems whose functions are not required to perform the three actions listed above. The I&C portions of these systems may, however, prevent the plant from exceeding limits that could activate the safety-design-basis systems.

These systems, listed in Table 21-1, are the same as or analogous to those required in current fission power plants and can be adapted to fusion with minimal development. However, in the case of the reactor protection system and the reactor portion of the nuclear steam supply system, the fusion reactor requirements are significantly more complex than those of fission reactor.

Figure 21-1 shows the diagnostic, information, and control portions of the reactor I&C system integrated into an overall power plant control system. The term plasma diagnostics includes all instruments that provide data related to properties or parameters of the plasma; all instruments that measure the behavior of the plasma and its interaction with heating and fueling systems and in-vessel components; and all instruments that provide information required for feedback control of the plasma parameters.

21.3 PLASMA DIAGNOSTICS AND CONTROL

The diagnostic systems for MARS must measure the plasma parameters over a wide operating range in both the central and end cells. The I&C system provides the instruments with appropriate operating parameters such as gain settings, voltages, timing intervals, and marks. It also provides for storing the incoming data in real time. Finally, it must collect the data, perform the appropriate reduction, analysis, and display, and archive the data for future examination.

The plasma characteristics to be measured are the ion and electron energy spectra, ion and electron densities, fusion power, potentials, and impurity levels. In principle, each of these characteristics should be measured in each region of the machine; moreover, the parameter set should be determined as a function of time and space--in one, if not two, dimensions. However, this is precluded both by practical access and cost limits, and some compromises will be necessary. For example, minimum instrumentation may be placed in one of the end cells, enough only to ensure that the cell is operating correctly.

To define the control requirements of the MARS plasma, one has to know its dynamic behavior, the time constants involved, and the areas where possible instabilities can occur. In the MARS baseline design we have defined the steady-state plasma parameters, but no dynamic analysis has yet been performed. If it becomes necessary to constantly monitor and control one or more of the plasma parameters, then new radiation-hardened diagnostics may be needed.

A detailed listing of each of the transducers, sensors, and control instruments is beyond the scope of the MARS study. However, a broad examination of sensor requirements is presented in Table 21-2 for the wide variety of diagnostic and control instrumentation necessary for a machine of

Table 21-1. Instrumentation and control functional classification for safety-related systems.

- Reactor protection system (RPS)
- Nuclear steam supply shutoff system (NSSSS)
- Service water system--neutron, gamma, and tritium monitoring system:
 - Local power range monitor (LPRM) subsystem
 - Intermediate range monitor (IRM) subsystem
 - Average power range monitor (APRM) subsystem
- Reactor vessel instrumentation
- Standby vessel instrumentation
- Direct converter isolation cooling system
- Leakage detection system
- Rf heating control
- Recirculation pump trip (RPT) system
- Primary containment atmospheric control (PCAC) system
- Reactor building closed-loop cooling water (RBCLCW) system
- Emergency diesel generators
- Control room air conditioning (CRAC) system
- Chiller-equipment-room ventilation system
- Diesel-generator-room emergency ventilation systems
- Relay room, emergency switchgear rooms, and computer room
 - air conditioning system
- Battery room ventilation systems
- Reactor building standby ventilation system (RBSVS)
- Screenwall pumphouse ventilation system
- Remote shutdown system
- Residual heat removal system
- Motor-control-center (MCC) room ventilation system (reactor building)
- Motor-generator (MG) room ventilation system (reactor building)
- Compressed air system--portion of
 - MSIV leakage control system
- Reactor vacuum and impurity control
- Magnet power supply control
- Reactor water cleanup (RWCU) system
- Reactor manual control system
- Reactor recirculation system
- Li-Pb control system
- Feedwater control system
- Pressure regulator and turbine generator controls
- Process computer system
- Neutron monitoring system (NMS):
 - Source range monitor (SRM) subsystem
 - Rod block monitor (RBM) subsystem
 - Traversing in-core probe (TIP) subsystem
 - Intermediate range monitor (RM) subsystem
 - Average power range monitor (LPRM) subsystem
- Reactor vessel instrumentation
 - (except the instrumentation in safety program and system)

Table 21-1. (Continued).

Radwaste offgas control system
 Liquid radwaste control systems
 Turbine building closed-loop cooling water (TBCLCW) system
 Residual heat removal (RHR) system
 Refueling interlocks
 Emergency diesel generators
 Control room air conditioning system
 Tritium processing control
 Helium refrigeration control
 Nitrogen refrigeration control

Fig. 21-1. Conceptual overview of power plant control system.

Table 21-2. MARS reactor I&C requirements.

Subsystem	Parameter/component	Measurement type
1. Plasma diagnostics	Plasma initial ionization $\langle n_e \rangle$	Interferometer
	Plasma current I_p	Rogowski coil
	Plasma position	Position sensors
	Electron temperature T_e	Synchrotron
	Electron density $\langle n_e \rangle$	Interferometer
	Neutron flux n	Neutron counters
	Radiated power P_r	Bolometer
	Hot spot location	IR camera
	Ion densities n_D, n_T	Charge exchange
	Plasma beta β	Diamagnetic loops
	Electrostatic potential	Ion-beam probes
	Φ_c, Φ_b	
	Background gas density	Residual gas analyzer
2. Neutral beam injection	Power supplies and ion source controls	
	Main accel, with 5 outputs	Not regulated
	Pre-accel	
	Electron collector	Regulated to conform to accel voltage
	Arc discharge	
	Filaments	
	Converter	
	Focusing	Regulated to conform to accel voltage
	Cesium heater	Temperatures
	D ₂ gas feed controls	Cesium reservoir level
		Heater currents
	Cryopumps	
	Seven cryopumps for each injector system, some of which float at negative high voltage	Gas pressures
		LN reservoir levels
		LHe reservoir levels
		Recycling without shutdown
	Laser neutralizer (1 at each end of MARS)	Shielded from radiation except mirrors and windows in the laser resonator
	Laser power supply	
	Catalytic plant and compressor	
	Electrolysis plant and compressor	
	Metastable oxygen generator	

Table 21-2. (continued).

Subsystem	Parameter/component	Measurement type
	Separator and boiler	
	Laser gain medium	
	Laser resonator	
	Beam targets	Exposed to radiation
	Residual ion beam	Current, voltage, calorimetry
	Neutral beam collimators	Calorimetry
	Neutral beam dump	Calorimetry
3. Rf heating	Gyrotron cooling	Temperature water (pressure, flow, temperature)
	Gyrotron magnet cooling	Temperature cryogenic (pressure, flow, temperature)
	Transmission line	Temperature nitrogen (pressure, flow, K^2)
	Rf Power	Wave guide (power, exit phase) Power (input output absorbed)
4. Impurity control	Drift pumping and halo scraper	Temperature Water (pressure, flow, temperature) line x-ray detector spectrometers
5. Fueling/ Tritium Processing	Components	Inventory (calculated, measured mass flow, residual heat)
	Cleanup	Remote handling
	Leak detection	Radiation
	Liquids	Pressure Temperature Flow Valves Pumps
	Gasses	Pressure Temperature Flow Valves Pumps
	Fueling	Injector Quality

Table 21-2. (continued).

Subsystem	Parameter/component	Measurement type
6. First wall/blanket	Wall	Cooling water (pressure, flow, temperature) Integrated flux (measured, calculated) Temperature
	Blanket	Structural loading Water coolant (pressure, temperature, flow, calorimetry) Breeding (inventory, ratio) Temperature Decay heat Leakage
7. Vacuum	Pumps	Flow Pressure Coolant Saturation (cryo) Regenerator control
	Valves	Position Control
	Impurities	Residual gas analysis Leakage
8. Shield	Shield	Radiation Temperature Leakage
9. Radiation waste/disposal	Radiation monitoring	Area monitoring Influent levels Effluent levels
	Fluid leak detection	Mass flows (calculated) Chemical detection (fluid samples)
10. Magnets	Coolant	Cryo (level, pressure, flow, temperature)
	Coils	Temperature Voltage Current
	Power supplies	Structural loads Quench detection Temperature Current voltages

Table 21-2. (continued).

Subsystem	Parameter/component	Measurement type
11. Electrical	Breakers/transformers	Open/close Voltages (waveforms) Currents (waveforms)
	Supplies	Trim plasma shape/ position
12. Support structure		Stress Position Temperature
13. Cryogenic	Cryo fluid	Pressure Flow Temperature Calorimetry
	Nitrogen Water	(Same) (Same)
14. Heat transport	Water	Pressure Flow Temperature
	Steam generator	Heat balance Leak detection
	First wall/blanket	Temperature
	Auxiliary	Calorimetry
15. I&C	System status	Power (primary, standby) Temperature Hardware availability
	Sensor status	Reasonability/redundancy check

this complexity. In this respect, it should be noted that whereas the status of instrumentation for both general control monitoring and neutron diagnostics has been demonstrated in some fusion experiments, the same is not true of plasma diagnostic instrumentation. This is because few of the current plasma diagnostic techniques can be successfully integrated with the severe reactor environment of a fusion device such as MARS. Thus, there appears to be a requirement to develop fully engineered and hardened plasma instrumentation capable of sustained operation in a high-power DT environment.

In addition to the high-energy neutron and gamma fluxes experienced by the instrumentation, plasma access is required in MARS, as well as access for remote maintenance. Modular instrumentation is desirable for maintainability; however, modularity complicates access to devices that need to look directly into the plasma.

21.4 SUBSYSTEM INTERFACES

Several major subsystems interface with the plasma to create the operational environment for the plasma and to control the transport of energy to the balance of plant. Each of these subsystems will have its own local-control computer system to oversee the safe operation of the subsystem and to transfer data to the reactor's supervisory control system.

We prepared a survey form and sent it to each subsystem designer to determine instrumentation and data requirements. The results indicated 22,500 data channels, shown in Table 21-3, for the magnets, equipment, neutral beams, plasma diagnostics, heat transport, and cryogenic and vacuum systems. We assume that the plasma diagnostics and rf equipment will require a sampling frequency of milliseconds because of short time constants. The remaining systems will have data sampling frequencies of about 1 s. For the high frequency data we have assumed triple redundancy, whereas for the lower frequency we have assumed double redundancy. An overview of the I&C requirements for these subsystems is presented in the following paragraphs.

Table 21-3. Subsystem data requirements.

Subsystem	Channels	Frequency
Plasma diagnostics	1100	1 ms
ECRH	2400	1 ms
ICRH	1800	1 ms
Neutral beam	1800	1 ms
Heat transport	1300	1 ms
Halo scraper	1200	50 ms
Shield	200	1 s
Magnets	4000	1 s
Tritium processing	700	1 s
Cryogenics	500	1 s
Vacuum	400	100 ms
Direct converter	1600	100 ms
Radiation monitoring	500	1 s
Balance of plant	5000	
Total	22,500	

21.4.1 Rf Systems

The rf equipment consists of the electron-cyclotron resonant heating (ECRH) and ion-cyclotron resonant heating (ICRH) systems that provide power to be used for localized heating of electrons and ions. Instrumentation requirements for the ECRH include:

- Gyrotron--voltage and current for the cathode, gun anode, filament, heater, focus magnets, gun anode magnets, collector, filament hours, arc detection, and fault location;
- Series tube--voltage and current;
- General--microwave injected power and input distributions, gyrotron coolant conditions, gyrotron magnet cooling, waveguide temperatures and cooling conditions, and vacuum conditions.

Instrumentation for the ICRH system includes:

- Launcher and transmission--power monitoring via directional couplers as well as timing information and phase adjustment for each launcher;
- Amplifier chain--current and voltage for final, intermediate, and driver amplifier plates, directional coupler for power monitoring;
- General--flow and temperatures of all major water paths;
- Status indicators for crowbars, HV ready, HV on, filaments bias supplies, crowbar lockout, flow and temperature of secondary cooling paths, rf on, and emergency off.

Approximately 1600 channels of data will be required for the ECRH and ICRH systems.

21.4.2 Neutral Beams

A neutral beam system is required at each end of MARS, each with a power supply that will measure and control output currents and voltages. The high-voltage terminals will have I&C for the ion source, focus voltages, pre-acceleration, and gas pumping. The high-voltage terminals must be shielded from radiation and magnetic fields but will undergo voltage transients as a result of spark breakdown. The major components of the neutral beam system requiring I&C functions are listed in Table 21-4.

Local control and monitoring for the neutral beam system will consist of independent local controls for each source power supply and supervisory control to coordinate groups of source power supplies. Approximately 1800 channels of data will be required for the neutral beams.

21.4.3 Magnets

The magnet system includes 42 central cell magnets, 18 end cell magnets (9 each end), and all superconducting coils except one resistive choke coil at each end. The control system monitors magnet coil operation and controls the power supplies that drive the magnets. The system controller senses problems such as a low level of liquid helium and acts as an interlock to prevent the

Table 21-4. Neutral beam system components requiring instrumentation.

-
1. Power supplies and ion source control
 - Main accel, with 5 outputs (not regulated)
 - Pre-accel
 - Electron collector
 - Arc discharge (regulated to conform to accel voltage)
 - Filaments
 - Converter
 - Focusing (regulated to conform to accel voltage)
 - Cesium heater (temperatures)
 - D₂ gas feed (cesium reservoir level--heat currents)
 2. Cryopumps
 - Gas pressure, Ln levels, LHe levels, etc.
 3. Laser neutralizer
 - Power supply
 - Catalytic plant
 - Electrolysis plant
 - Separator and boiler
 - Laser gain medium
 - Laser resonator
 4. Beam targets
 - Residual power in beam (current, voltage, and calorimetry)
 - Neutral beam collimators (calorimetry)
 - Neutral beam dump (calorimetry)
-

magnets from charging. During magnet operation the controller provides for a simultaneous energy dump of all the magnets in the event of a fault.

The magnet protection subsystem will be operated by a local control computer. The subsystem elements include quench detectors, helium level monitors, and vapor-cooled lead temperature monitors. The quench detectors interface with the superconducting magnets via five voltage taps. Additional instrumentation will include case temperatures and strain gages, copper coil temperatures, and strains. The magnet subsystem will use approximately 4,000 channels.

21.4.4 Heat Transport System

The heat transport system includes the blanket, reflector, and shield. Instrumentation for the shield and reflector will include temperature profiles, neutron/gamma leakage, tritium leakage, and induced radioactivity. Instrumentation for the blanket will include temperature profiles, neutron flux and gamma levels, Li-Pb coolant conditions, power flow, tritium breeding

ratio, tritium containment and leak detection, and induced radioactivity. Velocity temperature and pressure profiles will be monitored as follows:

- Velocity--at feed tubes, blanket tube, blanket beam, reflector, and discharge tubes;
- Temperature--at exit of blanket tubes, beam, first wall, and reflector;
- Pressure--at entrance of the feed heater and entrance and exit of the blanket tube.

The blanket, reflector, and shield will have approximately 3,000 sensor channels to maintain control and monitoring requirements.

21.4.5 Cryogenics

The cryogenic system must provide LHe, LHe II, and LN₂ for full-load steady-state operation. Instrumentation and control will be accomplished at both the subsystem process control level and at the local control computers located throughout the subsystem. Status information will be supplied to the reactor supervisory control system. The liquid helium refrigeration plant interfaces with the magnets, direct converter, cryopanel, ECRH gyrotrons, and neutral beams. The liquid nitrogen system interfaces with the magnet shields, cryopanel shields, cryopumps, and LHe refrigeration.

The instrumentation for the cryogenic system will monitor overall system performance, measure performance of components, and sense fault conditions. Instrumentation requirements will include flow rates, temperatures, pressures, and liquid levels to determine system performance. Inlet and outlet conditions of specific components such as the liquifier will indicate component performance. Coolant flow rates, pressures, vibration transducers, and temperature sensors will indicate faults. Approximately 250 data channels will be required by the cryogenic system.

21.5 REACTOR INSTRUMENTATION AND CONTROL SYSTEM

The overall I&C system is a complex hierarchical system of supervisory minicomputers, local control minicomputers, and component instrumentation and control microprocessors for distributed sensors and control. The supervisory I&C system coordinates the activities of all reactor interface subsystems including rf, neutral beam, magnet, vacuum, plasma diagnostic, power, balance of plant, and safety interlocks. The supervisory system monitors the status of the cryogenic and tritium recovery processing plants. The overall hierarchical system with the reactor, cryogenic, tritium processing, and power system balance-of-plant is assumed to have 30,000 signals. This number is approximately an order of magnitude greater than current fission and fossil power plants. The system will process approximately 30% of the signals on a millisecond basis, 20% of the signals on a 50- to 100-millisecond basis, and 50% of the signals on a 1-s to 1-min basis. Because of the need for feedback control on signals requiring millisecond monitoring, the system requirements are beyond the capabilities of present-day process control systems.

The lowest level of subsystem control is provided by the CAMAC (Computer Aided Measurement and Control) crates used for each component or subsystem element. This local intelligence is assumed to be part of the design of the individual components or subsystems. Local control computers link CAMAC crates to the supervisory control computer, thus providing the interface extension of hierarchical control structure of MARS. Each subsystem provides data communication and control paths between the MARS hardware and the supervisory information and control system. Each system will be capable of automatic or manual control and remote data acquisition for the MARS control room.

Safety will be an essential part of the MARS operational system and a specific management function. Each subsystem has a number of unique safety aspects, the control of which is an integral part of the design process of that particular subsystem. In addition, certain aspects of safety, such as controlling personnel access to potentially hazardous areas, are so comprehensive that they must be given separate recognition at the subsystem level.

The primary purpose of the MARS personnel and safety interlock system is to ensure that personnel access to areas of high hazard is controlled in a manner that will not endanger health or safety. The system provides for post-shutdown radiation monitoring, safety interlocks on personnel barriers, run/safe switches, and personnel warning. These monitors and interlock switches are integrated into a personnel safety control system that is capable of actuating the personnel warning systems and initiating control signals to remove or isolate the hazards.

22 Balance of Plant

22. Balance of Plant

We have designed the balance of plant to accommodate the MARS reactor facility. These facilities have much in common with standard, current, power plant technology, while still providing those aspects uniquely required by a mirror fusion reactor.

As a rule, the site plan and general plant arrangement were developed to achieve the most economic arrangement of system components taking into account their various functions. Certain unique features of a mirror reactor, notably the long central cell length of 130 m and the need to extract thermal power at the ends of the reactor, require certain facilities' design features. In addition, the use of two coolants in MARS, as noted in Sec. 15 of this volume, led to the adoption of two independent power cycles, which also dictate certain other unique features of the general plant arrangement.

22.1 SITE PLAN AND GENERAL ARRANGEMENT

The plot plan for the mirror advanced reactor is shown in Fig. 22-1. The reactor building is arbitrarily oriented in an east/west direction toward the southern end of the facility complex. The site chosen for MARS is "Middletown," USA. For the purpose of labor statistics, Middletown is assumed to be outside of Kansas City, Missouri. We also assume that the site is close enough to major roads and an existing rail line to eliminate rail-spur construction as a factor in our economic evaluation. The topography is level to gently rolling, with adequate water available for makeup. The site is located within UBC Seismic Zone 2, which provides the basis for seismic design of the building structures.

The site is reached via an access road with actual admittance to the site controlled by a 3-m-high fence, a guardhouse, and an inner perimeter fence also 3 m high. The site is large enough to provide for on-site burial of removed blanket modules should that option be chosen.

The MARS complex is surrounded by a loop road allowing access for personnel and equipment to all major facilities. The rail spur leading to the site has terminals at both ends of the reactor to ship and receive the large end cell coils and major equipment for the turbine generator units.

The southern end of the complex includes the reactor building and those structures required for direct support of the reactor, including:

1. The tritium building;
2. The receiving area for new blanket modules;
3. A hot cell;
4. The spent-reactor-blanket-module storage pool.

In addition, a magnet fabrication building is provided just inside the southern loop road.

Just north of this area (and closely coupled with the reactor building) is the steam generator building, which houses the heat exchangers for both the lithium-lead (Li-Pb) and water-cooling loops. The two power supply buildings are located adjacent to the steam generator buildings (one at each end of the reactor). An auxiliary boiler is provided for plant services and unit startup.

The two turbine-generator buildings, one for the 900-MW unit 1 and another for the 350-MW unit 2, are north of the steam generator building and perpendicular to the axis of the reactor. This perpendicular orientation

22-1

Fig. 22-1. Plot plan for units 1 and 2.

LEGEND

1. TURBINE GENERATOR BUILDING
2. TRITIUM BUILDING
3. STEAM GENERATOR BUILDING
4. REACTOR BUILDING
5. SWITCHYARD
6. WAREHOUSE
7. NATURAL DRAFT COOLING TOWER
8. CHLORINE CONTAINER BUILDING
9. PUMP STRUCTURE
10. ADMINISTRATION BUILDING
11. PLANT MAINTENANCE BUILDING
12. MAIN TRANSFORMER
13. START UP TRANSFORMER
14. AUXILIARY TRANSFORMER
15. WATER TREATMENT BUILDING
16. CONDENSATE STORAGE TANK
17. AUXILIARY BOILER BUILDING
18. DIESEL GENERATOR BUILDING
19. MECHANICAL DRAFT COOLING TOWER (CIRC WATER SYS)
20. MAGNET FABRICATION BUILDING
21. CONTROL COMPLEX
22. CIRCULATING WATER PIPE
23. HEAT EXCHANGER AREA (AUX CLG WATER)
24. NEW PARTS AND ASSEMBLY AREA
25. SPENT BLANKET STORAGE POOL
26. HOT CELLS AREA
27. POWER SUPPLIES AREA
28. CRYOGENICS LIQUEFYING AREA
29. CRYOGENICS STORAGE TANKS
30. MECHANICAL DRAFT COOLING TOWER (AUX CLG WATER)
31. AUXILIARY CIRCULATING WATER PIPE
32. LOADING DOCK

GENERAL NOTES

1. UNIT NO. 1 IS A 900 MW SUPERHEATED STEAM CYCLE
2. UNIT NO. 2 IS A 350 MW SATURATED STEAM CYCLE
3. NOT ALL EQUIPMENT AND FACILITIES ARE SHOWN

precludes the need for structural shielding of the reactor and tritium buildings from turbine missiles.

An administration building and a plant maintenance building (for general plant maintenance) are provided with sufficient parking for plant staff and potential visitors. The natural-draft cooling tower for unit 1, the mechanical-draft cooling tower for unit 2, and the extra-high voltage (EHV) switchyard complete the facilities to the north of the turbine-generator units.

22.1.1 Reactor, Tritium, and Steam-Generator Buildings

The reactor, tritium, and steam-generator buildings have been arranged in a close-coupled fashion to minimize distances between the reactor and the operational- and maintenance-support function areas. The close-coupled arrangement also minimizes the areas of the plant facility that are exposed to potential tritium release and airborne radiation. The plan and section details of these buildings are shown in Fig. 22-2.

The portion of the reactor building that houses the central cell and includes the choke coils is approximately 138 m long by 24 m wide. The buildings, located directly to the south of the reactor building and sharing a common wall, are the tritium building, the new parts and assembly area, a hot cell area and adjacent maintenance gallery, plus the spent-blanket storage pool. Thus, the essential services of tritium recovery, fuel fabrication, and tritium cleanup are provided adjacent to the central cell hall. The hot cell and receiving areas provide space for maintenance and replacement of blanket modules on a periodic schedule, as discussed in Sec. 18 of this volume.

Removable shielding walls at each end of the central cell area separate that section from the end cells. These removable walls provide for maintenance, either hands-on or semiremote, to the end cell and the end cell heating systems, whereas blanket modules are replaced remotely in the central cell hall. A separate area is provided around the direct converter for direct converter maintenance and vacuum pumping at each end of the reactor. This area is also serviced using a railroad siding to transport and receive the end cell coils.

Because of the machine's linear nature and the use of helium II for cooling certain end cell coils, we provide two cryogenic refrigeration areas, one at each end of the machine. This arrangement minimizes cryogenic piping and maintains economic compressor sizes.

Just to the north of the central cell is the steam generator building, which contains all the heat transport equipment for the Li-Pb loop supplying turbine-generator unit 1 and also contains the steam generators associated with the water-cooled reflector and direct converter supplying unit 2. The central location on the wall adjacent to the central cell minimizes piping runs and reduces pumping power in the energy conversion system. The reactor, tritium, and steam-generator buildings are maintained at a negative pressure to preclude tritium leakage.

At each end of the reactor, adjacent to the steam generator building, are buildings that house the power supplies, in addition to the power conditioning and switching equipment for electron-cyclotron resonant heating (ECRH), ion-cyclotron resonant heating (ICRH), and neutral beams. Coil power supplies are also located in this building.

PLAN EL 0.200

SECTION A

Fig. 22-2. General arrangement of the reactor, tritium, and steam-generator buildings, plus plans and sections for units 1 and 2.

LEGEND

1. REACTOR
2. REMOTE TOOL OPERATION GALLERIES
3. SPENT BLANKET STORAGE POOL
4. NEW PARTS AND ASSEMBLY AREA
5. CHOKE COIL AND END CELL MAINTENANCE AREA
6. DIRECT CONV SERVICE AND VACUUM PUMP AREA
7. NEW MODULE PREPARATION AREA
8. LOADING AREA
9. CRYOGENICS LIQUEFYING AREA
- 9A. CRYOGENICS STORAGE TANKS
10. STEAM GENERATOR - UNIT NO. 2
11. ECONOMIZER - UNIT NO. 2
12. PUMPS - UNIT NO. 2
13. SUPERHEATER - UNIT NO. 1
14. STEAM GENERATOR - UNIT NO. 1
15. ECONOMIZER - UNIT NO. 1
16. REHEATER - UNIT NO. 1
17. PIPE CHASE AND MAINTENANCE AISLE
18. PUMPS - UNIT NO. 1
19. POWER SUPPLY AREA ECRH, ICRH,
20. NEUTRAL BEAM, S/C COILS, NORMAL
21. CHOKE COIL, DRIFT PUMP COIL
- 22.
23. LITHIUM LEAD HEAD TANK
24. CONTROL ROOM
25. OFFICE AREA
26. MAINTENANCE PIT FOR GYROTRON (END CELL ONLY)
27. PIPE PIT (CENTER CELL)
28. HVAC
29. LOCKERS
30. SHOWERS AND LAVATORIES
31. ANALYTICAL LAB
32. MAINTENANCE
33. HVAC
34. TRITIUM STORAGE
35. DEUTERIUM AND NITROGEN STORAGE
36. SOLID WASTE DISPOSAL
37. TRANSFER PUMP UNIT
38. ISOTOPE SEPARATION SYSTEM
39. GAS TANK
40. TRITIUM WASTE TREATMENT
41. ATMOSPHERE TRITIUM RECOVERY UNIT
42. TRITIUM WATER RECOVERY UNIT
43. H₂O TANK
44. FUEL BLENDER UNIT
45. HOT CELLS AREA
46. FUEL CLEANUP UNIT
47. LOADING DOCK
48. DRAIN AND MELTING TANK AND FILL-UP PUMP
49. RAILROAD
50. SPENT BLANKET LOADING PIT

UPPER LEVEL
TRITIUM BUILDING

SECTION B

22.1.2 Turbine-Generator Buildings: General Arrangement

Figure 22-3 shows the plan and section for the turbine buildings for units 1 and 2. Unit 1 is the 900-MW, 3600-rpm superheated steam unit, with heat input from the Li-Pb energy conversion system. Unit 2 is the 350-MW, 1800-rpm unit powered by the cooling water from the reflector and from the direct converter/halo scraper.

The turbine buildings abut and are oriented with the turbine generator axis perpendicular to that of the reactor and steam generator buildings. This orientation minimizes the amount of steam-power piping and precludes any turbine-generated missiles from impinging on either the steam generator or reactor buildings.

Abutting the steam generator building and between the two turbine generator buildings is the control building. This building houses the control room, a support services area, separate computer rooms, electronic equipment rooms, and cable vaults for each unit.

22.2 BUILDING DESIGN

The MARS reactor facility includes buildings designed to meet safety-related conditions peculiar to fusion reactor systems as well as buildings of conventional construction. Safety-related structures include the reactor building, steam-generator building, hot cell, spent-blanket pool, tritium building, control building, and diesel-generator building. The buildings are designed in accordance with the design parameters described below.

22.2.1 Design Basis

We created the structural design in accordance with the following criteria to satisfy fusion reactor safety requirements and to allow comparison with other fusion reactor studies:

1. Codes and standards (Regulatory requirements, governing codes, and industry standards)--
 - a. Code of federal regulations;
 - b. NRC regulatory guides;
 - c. State and local building laws;
 - d. American National Standards;
 - e. AISC specification for buildings;
 - f. ACI building code;
 - g. ASTM specifications.
2. Site parameters--
 - a. Site location: Kansas City, Missouri;
 - b. Seismic requirements:
 - Operating basis earthquake (E_o), uniform building code, Zone 2;
 - Design basis earthquake (E), maximum horizontal ground acceleration: 0.13 g, maximum vertical ground acceleration: 0.09 g;
 - c. Soil bearing capacity--49,000 kg/m²;
 - d. Tornado wind and tornado-generated missiles (Nuclear Regulatory Guide 1.76 and ANSI Standards);
 - e. Low-trajectory turbine missiles (Nuclear Regulatory Guide 1.15).

MEZZANINE FLOOR EL 6.300

LEGEND

1. TURBINE GENERATOR
2. CONDENSER
3. MOISTURE SEPARATOR REHEATER
4. IP HEATERS
5. TURBINE DRIVEN STEAM GENERATOR FEED PUMPS
6. HP HEATERS
7. LP HEATERS
8. STEAM GENERATOR START-UP AND STAND-BY PUMP
9. BATTERY ROOM
10. ELECTRICAL EQUIPMENT ROOM
11. COMPUTER ROOM
12. CONTROL ROOM
13. SUPPORT SERVICES AREA
14. GLAND STEAM CONDENSER
15. SWITCHGEAR AND POWER CENTERS AREA
16. ELECTRONIC EQUIPMENT ROOM
17. COMPRESSORS, RECEIVERS AND INSTRUMENT AIR DRYER
18. COOLING WATER HEAT EXCHANGERS AND PUMPS
19. CABLE VAULT
20. UNLOADING BAY
21. CONDENSATE POLISHERS
22. LUBE OIL RESERVOIR, CONDITIONER AND PUMPS
23. ELECTRO HYDRAULIC UNIT
24. VACUUM PUMPS
25. GENERATOR MAIN LEADS AND AUXILIARY EQUIPMENT
26. CONDENSATE PUMPS AND PIT
27. CHEMICAL FEED EQUIPMENT
28. SPARE
29. BATCH OIL TANK
30. RAILROAD
31. HEATER DRAIN PUMPS
32. DEAERATOR HEATER AND STORAGE TANK
33. FEED PUMP TURBINE EXHAUST
34. FEEDWATER BOOSTER PUMPS
35. MOTOR DRIVEN STEAM GENERATOR FEEDPUMPS
36. CIRCULATING WATER INLET
37. CIRCULATING WATER OUTLET
38. BRIDGE CRANE
39. ROOF VENTILATORS

SECTION A
(F5, F14, 05)

KEY PLAN

SECTION B
(F9, F19, 09)

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3. Loads--
 - a. Accident pressure: 12 kPa (reactor building);
 - b. Accident temperature: 500°C (reactor building floor);
 - c. Operating pressure: -5 cm H₂O (reactor and steam-generator buildings);
 - d. Equipment loads: as required;
 - e. Live loads:
 - Steam generator building: 1220 kg/m²;
 - Tritium building: 733 kg/m²;
 - Roof: 98 kg/m²;
 - Others: as required by appropriate codes.
4. Materials--
 - a. Concrete: c = 27.6 MPa at 28 days;
 - b. Reinforcing steel: fy = 413.7 MPa;
 - c. Structural steel: fy = 248.2 MPa.
5. Load combinations--

In the design we consider the following load combinations based on the code requirements for nuclear-safety-related concrete structures (ACI standard 349-76).

 - a. $U = 1.4 D + 1.7 L + 1.7 E_o$;
 - b. $U = D + L + DBE$;
 - c. $U = D + L + W_t$;
 - d. $U = D + L + T_a + 1.25 P_a$;

where

U = required strength,
 D = dead loads,
 L = live loads,
 E_o = operating basis earthquake,
 DBE = design basis earthquake,
 W_t = loads generated by tornado winds and missiles,
 T_a = thermal loads under accident conditions,
 P_a = pressure under accident conditions.

22.2.2 Design Description

We formed the conceptual design of the MARS facility on the basis of the above criteria. In general, the fusion reactor safety-related buildings were designed to sustain earthquake forces, tornado-generated forces and missiles, accident temperatures and pressures, and operating load conditions. In the design, safety-related structures are protected from turbine-generated missiles by the proper orientation of the turbine building.

The reactor building and the structure enclosing the end cells have 60-cm concrete walls and roof, stiffened with concrete frames that are 180-cm deep, 100-cm thick, and are spaced 10 m on centers.

The steam-generator building and tritium building have a 70-cm concrete roof, 60-cm walls, and a structural steel interior framing. The foundation-mat floors of these buildings and the surrounding walls (to a height of 60 cm above the floor) are lined with a 0.6-cm-thick carbon-steel plate. Additionally, these buildings are operated at the subatmospheric pressure of -5 cm of water.

The reactor building, steam generator buildings, and tritium building interior surfaces are sealed with epoxy paint. The hot cell roof and walls plus certain walls of the reactor building require a minimum thickness of

90 cm for radiation shielding. The 60-cm spent-blanket pool walls and slab are designed to resist earth pressure plus earthquake effects.

The control and diesel-generator buildings have 60-cm concrete roofs and walls, plus steel interior framing as required.

The building structures are designed to resist horizontal loads by diaphragm action of roof and floor slabs and shear wall action of exterior walls. Interior steel framing is basically provided to support vertical loads.

The buildings are constructed on a foundation mat. Buildings of conventional construction are designed in accordance with the following governing codes and industry requirements: state and local building laws, American National Standards, AISC specification for buildings, ACI building code, and ASTM specifications.

The structure of the turbine-generator building is supported by spread footings and grade beams. The turbine building is an all-steel frame structure with roof-truss construction. The operating floor is a reinforced concrete slab supported on permanent metal decking and steel framing. The mezzanine floors have grating steel framing, except for a concrete slab under the electrical equipment.

The structure is designed to support live electrical equipment and pipe loads. In addition, the design includes laydown of turbine-generator parts and construction loads. Wall framing is designed with appropriate wind and earthquake bracing. The turbine pedestal consists of reinforced concrete beams and columns supported on a common foundation mat. The pedestal is designed to minimize deflection of the pedestal and preserve turbine-generator alignment. The pedestal is isolated from adjacent structures. The exterior wall of the turbine-generator building consists of insulated steel siding and an insulated metal-deck roof.

Other plant buildings (such as the power-supply administration, warehouse, maintenance, coil fabrication, water treatment, auxiliary boiler, heat exchanger, new parts and assembly, and cryogenic liquefying) are all provided with structural steel framing, insulated metal-siding exterior walls, and an insulated metal-deck roof. We assume that all structures are supported on spread footings and grade beams. Lateral loads such as wind and earthquake will be resisted by bracing the exterior-wall framing.

22.3 MECHANICAL SYSTEMS DESIGN

22.3.1 Steam and Power Conversion Systems

Steam and power conversion systems convert steam energy generated in reactor heat-transport systems to electric energy. Two types of steam-turbine-generator power-conversion cycles are used in MARS. Unit 1 is a superheated-steam-turbine cycle that interfaces with the Li-Pb system. Unit 2 is a saturated-steam-turbine cycle that interfaces with the reflector and direct-converter cooling system. Both cycles are proven conventional designs typically used in large fossil-fuel and nuclear-fuel central-power generation stations.

As shown in the simplified flow diagrams for turbine-generator units 1 and 2 (see Figs. 22-4 and 22-5), major balance-of-plant mechanical equipment for MARS includes the turbine generator, steam piping, cooling tower, circulating water pumps, steam-turbine condenser, feedwater heat exchangers, and feedwater pumps. Descriptions and major design parameters for this equipment are in Sec. 22.7.

Fig. 22-4. The 900-MW superheated-steam-cycle and power-conversion-system flow diagram for unit 1.

LEGEND

- TURBINE STEAM
- EXTRACTION STEAM
- FEEDWATER, CONDENSATE
- CIRCULATING WATER
- EXHAUST STEAM
- HEATER DRAIN
- HP - HIGH PRESSURE
- LP - LOW PRESSURE

Fig. 22-5. The 350-MW saturated-steam-cycle and power-conversion-system flow diagram for unit 2.

22.3.1.1 Steam-Turbine Generator. Steam-turbine generator unit 1 consists of one high-pressure, one intermediate-pressure, and three low-pressure steam turbine cylinders, connected in tandem to a hydrogen-cooled electric generator. Superheated steam expands through the high-pressure turbine, is reheated in the Li-Pb system, and expands through the intermediate- and low-pressure turbines. The high-pressure, intermediate-pressure, and low-pressure cylinders have nozzles for bleeding extraction steam to feedwater heaters and to steam-generator feed-pump turbines.

Steam-turbine generator unit 2 consists of high-pressure and low-pressure steam-turbine cylinders connected in tandem to a hydrogen-cooled electric generator. Saturated steam expands through the high-pressure turbine, passes through a moisture separator/reheater, and expands through the low-pressure turbines. Extraction nozzles supply steam to feedwater heaters.

Units 1 and 2 both have electric generators that are sized for 105% of rated output as shown in the heat balances (Fig. 22-6 and 22-7, respectively). The steam turbine's performance is outlined in Table 22-1.

22.3.1.2 Steam Piping. Steam generated in the Li-Pb system and in the reflector and direct-converter heat-transport system is conveyed to the unit 1 and unit 2 steam turbines via main-steam and reheat-steam piping. Unit 1 main-steam and hot-reheat-steam piping material is a chromium-molybdenum steel alloy suitable for high temperature. Unit 1 cold-reheat and low-pressure steam piping plus all steam piping for unit 2 are carbon steel.

22.3.1.3 Cooling Tower. Heat rejected from the steam-turbine condenser and turbine auxiliary-heat exchangers is dissipated to the atmosphere through an evaporative-type cooling tower. Separate cooling towers are provided for each turbine-generator unit--a hyperbolic natural-draft type for unit 1 and a rectangular-mechanical-draft type for unit 2. These selections are typically the preferred economic choice for the respective turbine-generator sizes. Makeup water added to the cooling-tower basin replaces losses from evaporation, drift, and blowdown.

22.3.1.4 Circulating Water Pumps. These pumps circulate cooling water in a closed loop between the steam-turbine condenser, turbine-auxiliary heat exchangers, and cooling tower. The pumps, located adjacent to the cooling tower basin, develop sufficient head to overcome piping and condenser-hydraulic-friction losses, plus the differential static height between the cooling-tower-basin water level and cooling-tower-fill hot-water-distribution piping.

22.3.1.5 Steam-Turbine Condenser. Low pressure turbine exhaust steam is condensed in a surface-type condenser located below each turbine cylinder. Unit 1 has a multipressure zone condenser with backpressures of 6.08, 8.46, and 11.85 kPa, respectively. Unit 2 has a single-pressure zone condenser with a backpressure of 8.46 kPa. The number of pressure zones is equal to the number of low-pressure turbine cylinders. In addition to condensing main-turbine exhaust steam, the condenser receives the following: feedwater-heater drains and vents, unit 1 feed-pump turbine-exhaust steam, turbine-bypass steam, and turbine-cycle makeup water. Noncondensable gases are removed from the condenser through mechanical vacuum pumps.

LEGEND

- Q - FLOW-KG/HR
- P - PRESSURE-MEGAPASCAL, ABS.
- C - TEMPERATURE-CELSIUS
- H - ENTHALPHY-KJ/KG
- FTD - TERMINAL DIFFERENCE (C)
- ELEP - EXPANSION LINE END POINT, KJ/KG
- UEEP - USED ENERGY END POINT, KJ/KG
- D.C. - DRAIN COOLER APPROACH (C)
- S.C. - SUBCOOLING (C)
- HTR - FEEDWATER HEATER
- SGFP - STEAM GENERATOR FEED PUMP
- SGFPT - STEAM GENERATOR FEED PUMP TURBINE
- GSC - GLAND STEAM CONDENSER
- KPA - KILOPASCAL, ABS.
- B.P. - BACK PRESSURE
- ML - MECHANICAL LOSSES
- EL - ELECTRICAL LOSSES
- STEAM
- - - WATER
- · - · - EXTRACTION STEAM

PERFORMANCE

GROSS GENERATION
KW

892024.

CROSS PLANT EFFICIENCY
%

42.04

NET TURBINE HEAT RATE
KJ/KW·hr

8563.

Fig. 22-6. The 900-MW superheated-steam-cycle heat balance for unit 1.

LEGEND

- Q - FLOW kg/HR
- C - TEMPERATURE - CELSIUS
- P - PRESSURE - MEGAPASCAL, ABS
- H - ENTHALPY kJ/kg
- FTD - TERMINAL DIFFERENCE (C)
- D.C. - DRAIN COOLER APPROACH (C)
- S.C. - SUBCOOLING (C)
- ELEP - EXPANSION LINE END POINT, kJ/kg
- UEEP - USED ENERGY END POINT, kJ/kg
- STEAM LEAKAGE
- WATER
- EXTRACTION STEAM
- MAIN AND TURBINE STEAM
- HTR - FEEDWATER HEATER
- SGFP - STEAM GENERATOR FEED PUMP
- GSC - GLAND STEAM CONDENSER
- KPA - KILOPASCAL, ABS.
- BP - BACK PRESSURE
- ML - MECHANICAL LOSSES
- EL - ELECTRICAL LOSSES

GROSS GENERATION
(kW)

346903.

GROSS PLANT EFFICIENCY
(%)

33.83

NET TURB. HEAT-RATE
kJ/kW-hr

10642.1

Fig. 22-7. The 350-MW saturated-steam cycle heat balance for unit 2.

Table 22-1. Steam-turbine performance for MARS.

	Unit 1	Unit 2
Throttle flow (kg/hour x 10 ⁶)	3.15	1.83
Throttle temperature (°C)	479.4	277
Throttle pressure (MPa)	16.65	6.21
Reheat temperature (°C)	482	263
Gross generation (MW _e)	892.8	346.9
Gross efficiency (%)	42.04	33.83
Net turbine heat rate (kJ/kWh)	8563	10642.1

22.3.1.6 Feedwater Heaters. Turbine cycle thermodynamic efficiency is improved by use of a regenerative cycle, employing feedwater heaters to raise the temperature of feedwater to the steam generators. The heaters use steam from various turbine extraction locations depending on the required feedwater outlet temperatures, as shown in the heat balance (see Figs. 22-6 and 22-7). Units 1 and 2 both use shell- and tube-type heat exchangers. Unit 1 also features a deaerating contact-type, heat-exchanger stage that helps minimize the corrosion of high-pressure, high-temperature, steam-generator equipment.

22.3.1.7 Feedwater Pumps. Feedwater pumps for unit 1 consist of condensate, booster-feed, and main-feedwater pumps. The condensate pump head accounts for heat losses through the condensate piping, condensate polisher, gland-steam condenser, low-pressure feedwater heaters, and deaerator pressure. The booster feed pump receives feedwater from the deaerator and pumps it through an intermediate feedwater heater to the suction of the feed pumps at the pressure required for net positive-suction head. The main feed pump, driven by the auxiliary steam turbine, pumps the feedwater through the high-pressure heater to the Li-Pb, steam-generator equipment. In parallel with the turbine-driven feed pumps is an electric motor-driven pump provided for startup and standby service.

Unit 2 feedwater pumps consist of condensate and electric-motor-driven feed pumps. The condensate pumps discharge through the low-pressure feedwater heaters to the suction of the feed pumps. The feed pumps convey the feedwater through the high-pressure heater to reflector and direct-converter steam-generator equipment.

22.3.2 Reactor Auxiliary-Cooling-Water System

The reactor auxiliary-cooling-water system (RACWS) is designed to dissipate miscellaneous waste-heat loads generated in the reactor's auxiliary equipment including coils and shields, ECRH and ICRH, neutral beam injectors, tritium-recovery equipment, and cryogenic plant. Heat loads and cooling-water flows are given in Table 22-2. As shown in the flow diagram in Fig. 22-8, the RACWS consists of two separate, closed-loop, cooling-water pump trains (A and B), which are cooled by a common-service, water-cooling loop that uses a mechanical-

Table 22-2. Major cooling loads for MARS.

Component	Heat load (MW)	Flow (kg/s)
Magnet coil shield (central cell)	31.0	185
Magnet coil shield (end cell)	42.0	251
Blanket module seal shield	22.0	131.4
Normal choke coil	81.0	224
Air tritium-recovery units	10.0	54
Li-Pb cold trap	9.0	48
Drift pump coil	20.2	56
Li-Pb pump	4.4	75
Neutral beam injector	11.8	30
ECKH	32.2	157
ICRH	15.2	82
Cryogenic plant	18.0	214
Other	5.6	45
Total	302.4	1552.4

draft, evaporative-cooling tower to dissipate the heat to the atmosphere. Trains A and B contain demineralized-quality treated water. Train A is dedicated to serve potentially radioactive equipment. The piping is constructed of stainless steel material to minimize the generation of corrosion products. Service-water heat exchangers are double tubed to preclude tritium transport to the environment. A side-stream portion of the flow is discharged to the tritium building for electromagnetic filtration, ion exchange, and tritium removal. Because of the high pressure loss through the normal choke-coil inserts, a separate booster pump is provided.

Train B cooling loads are non-radioactive and can, therefore, use carbon steel piping. A sidestream flow to the water treatment facility is provided for chemical control.

22.4 RADWASTE SYSTEMS

During normal operation, the reflector cooling-water system and the direct-converter, cooling-water system of MARS will become radioactive primarily from activated corrosion products produced by neutron bombardment. To maintain water quality within these systems, cleanup capability is provided in the system design for removal of particulate and dissolved species by electromagnetic filters and ion exchangers, respectively. These devices remove the radioactive material from the water stream and accumulate it within the process vessels. When predetermined process limits are reached, the accumulated radioactivity, in the form of sludge on the filters and bead resin on the demineralizers, is handled in a safe and efficient manner. To accomplish this objective, suitable radioactive waste handling systems for

Fig. 22-8. Auxiliary cooling-water-system flow diagram for units 1 and 2.

LEGEND

— — — — — DEMINERALIZED WATER
 - - - - - CIRCULATING WATER
 NEUT - NEUTRAL
 GEN - GENERATOR
 COMP - COMPRESSOR

NOTE: THE EQUIPMENT, HEAT EXCHANGERS AND COOLERS ARE TYPICAL. TYPE, SIZE AND QUANTITIES ARE DEPENDENT ON THEIR DESIGN. SOME MAY BE ADDED, DELETED OR CHANGED.

these process wastes are provided for MARS. In addition, we have provided the capability to handle the waste produced from the cold trap, which removes corrosion products from the Li-Pb system.

Figure 22-9 represents an integrated system to handle filter sludges and bead resins from the reflector-cooling and direct-converter-cooling systems. The concept can be divided into four subsystems: (1) spent resins, (2) filter sludges, (3) waste solidification, and (4) water reuse. The radwaste system design is based on proven techniques used for handling similar type wastes in nuclear power plants. We discuss each of these subsystems separately.

22.4.1 Spent-Resin Subsystem

The purpose of the spent-resin subsystem is to collect, sample, mix, and transfer spent ion-exchanger bead resins from the reflector-cooling and direct-converter-cooling systems. The spent-resin storage tank is 3,000 gallons, which is based on handling resins generated from the cooling-water-system sources and water-reuse-system demineralizer for a period of one year. We base this figure on 10 gallons-per-minute continuous ion-exchange units, each with an assumption of resin replacement every two months. Resin is hydraulically sluiced to the storage tank with excess sluice water returned to the water-reuse system through the screen-dewatering connection on the spent-resin storage tank. The mixing and transfer pump is sized to provide adequate mixing flow, plus flow for recirculating and transferring resins. Depending on the nature of the solidification system, resin transfer can be done either on a direct batch basis, or by a bleed from the recirculation line. We have also provided the capability to sample the spent resin.

22.4.2 Filter-Backwash Subsystem

The purpose of the filter-backwash subsystem is to collect, sample, mix, and transfer filter sludges generated by the operation of the electromagnetic filters located in the reflector-cooling and direct-converter-cooling systems. We assume that each filter system has its own backwash tank that transfers the waste to the filter-backwash water-collector tank located in the radwaste system. Because very little information is available regarding source terms and the number of filter backwashes needed, the collector tank is sized at 25,000 gallons, which is the largest shop-fabricated tank available. The mixing and transfer pump is sized to provide adequate mixing and flow for recirculation and transfer of sludges. Depending on the nature of the solidification system, sludge transfer can either be done on a direct-batch basis or on a slip stream of the recirculation line. We provide sampling capability of the filter sludges.

22.4.3 Solidification Subsystem

For the purpose of this study, we assume that solid wastes, namely spent resins and filter sludges, are solidified before final disposal. This system includes: pretreatment tanks, chemical adjustment capability, dewatering capability, waste metering equipment, a solidification agent storage and addition system, plus waste/solidification-agent, mixing-container, filling equipment and material handling equipment. The final waste product from this system meets the requirements of 10CFR61.

Fig. 22-9. Radwaste system.

An alternative to the assumed approach is to use high integrity containers (HIC) for the packaging of resins and filter sludges. If HICs are used, the waste does not need to be solidified as long as it can be adequately dewatered.

22.4.4 Water-Reuse Subsystem

The purpose of the water-reuse subsystem is to collect water resulting from dewatering and flushing operations, purify this water, and store it for reuse in sluicing and flushing operations or, in the event of excess water, environmental release. The excess-water collector tank and the water-reuse tank are 25,000 gallons, the largest practicable shop-fabricated tank. The pump cartridge filter and ion exchanger are sized for 500 gallons-per-minute, a flow rate that permits processing a tank of water during an eight-hour shift. The water-supply pump serving the water-reuse tank is sized for 100 gallons-per-minute, a rate that should be sufficient for sluicing and transfer operations. The environmental release line should have a radiation monitor and automatic isolation capability in the event that radiation levels exceed applicable limits. Capability is also provided for sampling both the collector and reuse tanks and reprocessing the reuse tanks. Spent ion-exchanger resins are sluiced to the spent-resin storage tank, and the cartridge filter elements are changed.

22.5 BUILDING SERVICES DESIGN

The building services design includes the heating, ventilation, and air-conditioning systems (HVAC), plant water systems, drainage systems, lighting systems, communication system, fire-protection system, and compressed-air system.

22.5.1 Heating, Ventilation, and Air-Conditioning Systems

The heating, ventilation, and air-conditioning (HVAC) systems are designed to provide suitable environment and air quality to ensure personnel comfort, health, safety, plus efficient equipment operation and integrity. Ventilation zones are arranged so that potentially contaminated areas are separated from clean areas, letting air flow from clean areas to areas of potentially greater radioactive contamination before final exhaust.

The HVAC systems have the following localized ventilation systems throughout the plant during both normal and emergency operating conditions:

1. Reactor and steam-generator HVAC systems,
2. Tritium-building HVAC,
3. Turbine-building ventilation system,
4. Diesel-generator building ventilation systems,
5. Control-room air-conditioning system,
6. Fuel-handling area ventilation system,
7. Personnel-access building air-conditioning system,
8. Switchgear-areas cooling systems,
9. CVCS-area ventilation system.

The HVAC systems for the reactor, tritium, and steam-generator buildings, although independent, are designed to maintain negative pressure (~5 cm of H₂O for the reactor, tritium, and steam-generator buildings) relative to ambient atmosphere to preclude or minimize any environmental release of tritium or airborne nuclides. During normal operation, building gases are processed by air-detritiation units. Under accidental tritium release conditions, additional air-detritiation units are employed (see Sec. 16 of this volume). Building air is never vented directly to the atmosphere.

As closed-loop systems, these HVAC installations are capable of maintaining an inert atmosphere in the reactor building should that option be desired based on future analysis.

22.5.2 Plant Water Systems

The freshwater supply system maintains the water reserve for the fire-protection system, plus an operating reserve to supply the demineralized-water makeup system and the potable-water system. The total water volume is stored in freshwater and fire-protection supply tanks located in the yard.

The chilled-water systems provide a cooling medium for air-cooling units that require low-temperature water to maintain the ambient space temperatures of the design. The chilled-water systems consist of two separate systems, the normal chilled-water system and the essential-services chilled-water system. Each system provides closed-loop recirculation and mechanical refrigeration of the cooling water.

22.5.3 Drainage Systems

The equipment and floor drainage system collects and removes liquid waste from floors, tanks, equipment, and all areas of the plant during normal operating or accident conditions, and conveys this effluent in pipes to disposal or treatment facilities without affecting the safety or operation of the plant.

The sanitary drainage and vent system consists of the following: plumbing fixtures, drains, and other related equipment for the collection of sanitary and other nonradioactive waterborne wastes; a piped gravity drainage system at atmospheric pressure for the removal of waterborne wastes to treatment and/or disposal facilities; and a vent pipe system connected to the drainage piping that will limit fluctuation of air within the drainage pipes.

The interior storm-water drainage system consists of various types of rainwater collecting appurtenances for storm-water capture from plant roads, plant grounds, and structure roofs, plus an interconnected network of piping for conveyance. It provides the plant site with the means to effectively collect accumulations of rainwater, and under controlled conditions, creates the flow path for offsite disposal. The system maintains the integrity of plant access and prevents flooding of structure interiors.

22.5.4 Lighting Systems

Lighting systems provide illumination throughout the plant during normal and emergency plant operation. The normal lighting system extends throughout non-safety-related areas. The normal emergency-lighting system extends

throughout the entire plant, but is primarily located in the safety related areas. During a loss of offsite power, the normal emergency system is powered by the standby diesel generators. The dc emergency lighting system provides emergency illumination for the control room and other safety related areas. Power for the dc lighting system is supplied by storage batteries.

22.5.5 Communication System

The communication system comprises wireline and radio subsystems to furnish reliable facilities for establishing communication links within the plant, and to connect with off-site communications networks for disseminating information and signals.

22.5.6 Fire-Protection System

The fire-protection system provides the capability to limit fire risk and to detect and extinguish fires with means consistent with the protection of plant personnel and equipment.

The fire-protection system supplies water for firefighting to plant fire hydrants, sprinkler and water-spray systems, and hose racks in the various areas of the plant. Portable fire extinguishers are provided at key locations including the control rooms, new and spent-fuel storage areas, cable vaults, switchgear rooms, and the reactor auxiliary building.

22.5.7 Compressed-Air System

The compressed-air system is a process auxiliary system. Though it does not perform a process function, the system supports every power generating system. As such, it can affect plant availability and is designed for high reliability. Instrument air and station air are supplied to run pneumatic instruments and to service pneumatic tools.

22.6 ELECTRICAL SYSTEMS DESIGN

Electrical systems for MARS are designed to interface the two generating units with the electric utility system, as well as to provide auxiliary electric power for startup and normal operation of the mirror reactor, its support systems, plus both turbine generator units and their auxiliary loads. The system follows current technology and uses state-of-the-art equipment for most electrical systems. It includes the EHV switchyard, the main step-up transformers, and the unit auxiliary transformers, together with startup/standby transformers for each unit and common services. An onsite standby power system is provided for safe shutdown and tritium cleanup in case all offsite power is lost.

22.6.1 Main EHV Switchyard

Although not included in the cost estimate, the EHV switchyard is described here because of its importance to the layout of the electrical system for MARS. The 345-kV switchyard consists of four bays in a one and one-half breaker scheme, with two outgoing EHV transmission lines. The two main transformers from units 1 and 2 are connected to different bays in the

switchyard, as are the two outgoing EHV transmission lines. A third bay in the switchyard provides startup power through three startup/standby transformers to the reactor and the two turbine-generator units. This bay also accepts power from the dc/ac inverters from the direct converters. The last bay in the switchyard provides power to common auxiliaries and to the main ECRH power-conversion equipment.

22.6.2 Auxiliary Power System

A simplified main one-line diagram for MARS is shown in Fig. 22-10. As noted above, we employ commercially available equipment in a configuration similar to that of current light-water reactor technology. The major electrical loads for MARS are shown on Table 22-3.

The unit 1 auxiliary system is supplied from two auxiliary transformers. The first steps form the generator voltage of 26 kV to a double-winding secondary, each supplying a 13.8 kV bus. The second unit 1 auxiliary transformer is also a three-winding transformer (26 to 4.16 kV). The medium voltage switchgear is the indoor type with drawout air-blast circuit breakers. The medium voltage distribution system for unit 1 supplies all of the unit 1 turbine-generator loads and provides power to the heat transport system for unit 1, including the Li-Pb. The unit 2 auxiliary system is supplied by a single three-winding transformer (26 to 13.8 to 4.16 kV). Like the unit 1 auxiliary system, the unit 2 medium-voltage switchgear supports all of the unit 2 turbine-generator auxiliary loads and provides power to the heat transport system associated with that unit.

Each bay of the unit auxiliary system is provided with startup/standby power from transformers connected to a single bay in the EHV switchyard. Common services, such as the magnet power supplies, the plasma heating power supplies, the tritium system, and all building services are fed from medium voltage buses that are common to both units. These common services are fed via a unit auxiliary and a startup/standby transformer connected directly to the EHV switchyard. The common-services auxiliary transformer and the common-services startup/standby transformer are connected to different bays in the EHV switchyard. The main power converter for the ECRH plasma heating system can also be connected to either of two bays in the EHV switchyard. The onsite standby power system fed by an onsite emergency generator is connected to the common-services 4.16-kV bus.

22.6.2.1 Medium- and Low-Voltage Distribution System. The medium-voltage and low-voltage electrical-distribution systems (EDS) use auxiliary startup/standby power transformers and station service transformers to step down switchyard, generator, and medium voltages to usable levels to provide power to all plant electrical equipment. Air circuit breakers and combination starters are used to distribute power through the plant.

The medium-voltage, electrical-distribution system is a dual-voltage, dual-power-train design shown in Fig. 22-10. Dual-voltage levels of 13.8 and 4.16 kV are used to feed motors greater than 250 hp and also low-voltage (480 V) switchgear via station service transformers. The medium-voltage system does not normally feed the 4.16-kV medium-voltage system.

Table 22-3. Major electrical loads for MARS.

Equipment description	Operating load (MW)
ECRH-B power converter	106.2
ECRH-A power converter	9.1
Neutral beam injection	14.1
Anchor ICRH	28.8
Magnets	81.5
Drift pump	20.0
Helium I compressor	5.2
Helium II compressor	3.7
Nitrogen reliquifiers	6.3
Li-Pb pumps	21.0
Other plant auxiliaries	38.2
Total	334.1

The low-voltage electrical-distribution system supplies power to 480 V safety and nonsafety loads. All nonsafety 480-V loads are supplied through station service transformers connected to the 13.8-kV medium-voltage EDS. All safety-related 480-V loads are supplied from station service transformers connected to the 4.16-kV medium-voltage EDS safety-related buses.

22.6.2.2 The 120-V AC Systems. The 120-V ac systems provide power to instrumentation and control circuits and to miscellaneous small loads. These systems include the 120/208-V normal-instrument power systems; the 120/240-V uninterruptible power system, which feeds non-safety-related loads, like plant communications and the plant computer; and the 120/208-V instrument and reaction protection system, which supplies the safety-related loads and is fed from a 480-V bus capable of being supplied from the onsite emergency generator.

22.6.2.3 Electrical Systems (DC). The dc electrical systems provide uninterrupted stored power for control, instrumentation, and miscellaneous small loads throughout MARS during all normal or emergency plant operating conditions. The system includes the batteries, battery racks, battery chargers, switchgear, and distribution system complete with controls and instrumentation.

Safety-related 125-V dc systems supply operating and control power to safety-related switchgear, emergency diesel-generator field flashing and control, and to safety-related dc motor-operated valves. They also supply power to dc safety-related instrumentation and selected emergency lighting. Redundant supply is provided for all critical loads.

Nonsafety-related 125-V dc systems distribute operating and control power to turbine-generator loads, non-safety-related switchgear, plant non-safety-related loads, and non-safety-related instrumentation.

22.6.3 Grounding System

The grounding system, consisting of copper or copperweld cable and rods, covers the whole station area in a mesh-type configuration. The system arrangement provides protection for personnel, the public, and the equipment. This is accomplished by achieving safe touch and step potentials and by taking necessary measures to provide safe transfer potentials. The EHV switchyard is tied to and forms an integral part of the plant grounding system.

22.6.4 Lightning Protection System

Each plant structure for MARS has a separate lightning protection system to intercept lightning strokes before they strike the building structure. It provides a low-resistance path to the ground for the high currents associated with lightning discharges.

The system is primarily composed of air terminals and lightning conductors placed at specific intervals and locations along the uppermost part of the structure. The entire network of conductors and air terminals is electrically connected to a grounding cable loop and ground rods installed around the structure, which in turn connects to and forms a part of the plant grounding system.

22.7 MAJOR MECHANICAL COMPONENTS SUMMARY

The major balance-of-plant mechanical components for MARS units 1 and 2 are as follows:

<u>Unit 1 equipment</u>	<u>Major parameters</u>
Steam-turbine generator	TC6F 850-mm LSB, 3,600 rpm, 901,200 kW, 3,151,000-kg/hour throttle steam at 16.65 MPa/479°C/482°C.
Main steam pipe	1 1/4% chromium, 1/2% molybdenum steel, 762-mm o.d. x 89-mm wall.
Hot-reheat steam pipe	1 1/4% chromium, 1/2% molybdenum steel, 991-mm o.d. x 38-mm wall.
Cooling tower	Evaporative hyperbolic natural draft, 1,266-MW heat load, 119 m diam x 142 m high.
Circulating water pumps	Two vertical wet pit, each 6,730-kg/s capacity, 30-m total head, 2,611-kW motor power.
Steam-turbine condenser	Three-shell, three-pressure-zone steam condenser, 36,251-m ² surface area.

Feedwater heaters	Seven stages of shell and tube-type feedwater heat exchangers, 19,100-m ² surface area; in addition there is one open type deaerating heater.
Feedwater pumps	Two auxiliary steam-turbine-driven pumps, each 1,600,000-kg/hour capacity, 16.1-MPa head.
Booster feed pumps	Two electric-motor-driven pumps, each 1,600,000-kg/hour capacity, 1.7-MPa head.
Condensate pumps	Two motor-driven pumps, each 1,200,000 kg/hour, 2.1 MPa.

● Unit 2 equipment

Major parameters

Steam-turbine generator	TC2F 1020-mm LSB, 1,800 rpm, 345,800 kW, 1,832,000-kg/hour throttle steam at 6.2 MPa, 278°C 0.35% M.
Main steam pipe	A-106 grade-C carbon steel, 752-mm o.d. x 25-mm wall.
Cooling tower	Seven-cell rectangular mechanical draft, 699-MW heat load, 187-kW/fan motor.
Circulating water pumps	Two vertical wet pits, each 4,465-kg/s capacity, 24-m total head, 1,492-kW motor power.
Steam-turbine condenser	One-shell, two-pass steam condenser, 18,400-m ² surface area.
Feedwater heaters	Five stages of shell and tube type feedwater heat exchangers, 8,600-m ² surface area.
Feedwater pumps	Two electric-motor-driven pumps, each 1,010,000-kg/hour capacity, 4.8-MPa head.
Condensate pumps	Two electric-motor-driven pumps, each 670,000-kg/hour capacity, 3.9-MPa head.

23 Plant Startup and and Operation

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23. Plant Startup and Operation

23.1 INTRODUCTION

In this section we describe the operational aspects of the MARS reactor and its major systems. In particular, we consider the engineering startup of the machine. An examination of startup requirements from a plasma physics viewpoint is presented in Sec. 2 of this volume. The design analysis of the MARS reactor system thus far in this report has primarily focused on operation at full power. Experience with power plants of other types indicates that startup, staged power increases to full power, testing at fractional power levels, and shutdown are important operational aspects of a reactor and often determine the feasibility of various subsystem designs and their performance. The analysis and conclusions in this section are the result of a general study of startup and fraction power operation of tandem mirror fusion reactors where particular reference was given to MARS. Further details may be found in Ref. 1.

23.2 STARTUP PROCEDURES FOR MARS

The operation of a fusion plant can be conveniently divided into an initial commissioning phase and four phases that repeat in sequence as the plant is shut down and subsequently restarted. The first phase is the shutdown mode of the reactor. Two types of shutdown are possible. Phase IA, or cold shutdown mode, is expected to occur infrequently and would require that the blanket and other power-related components be completely shut down. In the case of the Li-Pb liquid eutectic, the coolant would be drained and stored separately. Phase IB is a hot shutdown mode in which the power conversion components and their coolants are maintained near ready operating conditions of temperature and pressure, but there is no plasma operation.

Phase II is for system testing, plasma initiation, and the standby power mode in which major engineering system performance tests are made, the plasma is started, and the reactor is brought to a low power standby operating state. Typically, the fusion power generated in Phase II is less than 5% of full rated power.

In Phase III, staged power operation, the reactor power is raised in stages until rated power operation is achieved. For purposes of system testing and licensing requirements, the reactor will operate for an extended period at fractions of full reactor power. Finally, Phase IV, rated power operation, is when the reactor produces power continuously at its rated design or licensed power level.

For purposes of further analysis and discussion, it is helpful to divide the reactor engineering systems into two general classes, as listed in Fig. 23-1. These two classes, plasma technology/support systems, and heat transport systems, encompass all subsets of individual systems. Figure 23-1 contains examples of subsets in each class. From this, one can readily identify subsystems that can be operated and tested independently of other subsystems and those where simultaneous operation is required. As an example of such interactions during initial commissioning or during a cold startup, testing of the Li-Pb-cooled blanket is likely to require operation of the magnet system. Testing of the first wall cooling system in the end cell region may require plasma operation to establish the surface heat loading and appropriate thermal and pressure stresses in the wall. These interactions and the various phases of plant operation of MARS are discussed in the following subsections.

Fig. 23-1. Division of MARS engineering systems into appropriate subsystems for startup analysis.

23.2.1 Phase 0: Initial Commissioning

23.2.1.1 Plasma Technologies: Pre-Plasma Operation. Plasma technologies include those support systems directly related to plasma operation. They therefore include the various magnet systems and their refrigerators, power supplies, and controls; the plasma heating systems including neutral beams, rf, their power supplies, and controls; the vacuum, gas processing, and refueling systems, including tritium processing and control; and the direct converter system.

The operation of these systems can generally be tested before a plasma is actually produced. The possible exceptions are the rf and direct converter systems. In particular, coupling of the rf power into the plasma depends on the performance of the launching structure in the presence of plasma. By contrast, a neutral beam injection system can be tested by taking power on the beam dumps, and plasma absorption of neutral beam injection power is straightforward relative to rf. Performance of the direct converter depends on establishing a particular potential drop from the plasma to the collector, thereby also requiring plasma for complete testing. Thus, in the pre-plasma stage, system performance tests will be carried out on the magnets and the gas handling systems, and partially on the plasma heating and direct conversion systems.

23.2.1.2 Heat Transport System: Pre-Plasma Operation. The heat transport system includes in-vessel components such as beam dumps, halo collectors, antennas, and the direct energy converter; the first wall-blanket system, including the structure and the Li-Pb coolant; the reflector-shield system and coolant; the tritium recovery and recycling system; the primary loop, including piping, pumps, and valves; and the secondary loop, including the heat exchanger, steam generator, and turbine.

The subsystems of the heat transport system are tested at various levels. First, many of the components such as pumps, valves, and beam dumps can be tested before final assembly. After plant construction is completed, the subsystems are tested in the pressurized cold condition (PCC) where leaks are detected at rated pressures. The completion of this phase is followed by testing in the pressurized hot condition (PHC). As a result of stress redistribution around cracks, we expect that leak rates can vary considerably at rated temperatures and pressures in the PHC mode of testing. Even though the majority of tests on the heat transport subsystems are done to ensure good performance at rated parameters, a variety of tests are designed at various power levels to guarantee the safe operation of the plant in anticipated accidents. Of course, accident conditions are not usually simulated during such tests, but rather, mild transients are induced and the test results are extrapolated to more accident-like conditions. A detailed discussion on specific tests is given later.

23.2.1.3 Initial Plasma Operation. During initial commissioning and following all system tests in a pre-plasma stage, the first plasma is produced. Details of a specific plasma initiation and startup sequence to bring a TMR plasma to a standby power mode are given elsewhere in this

chapter. Further plasma physics details are discussed in Sec. 2 of this volume. The objective during this initial commissioning phase is to establish plasma performance parameters with a minimum production of fusion power (perhaps even zero). The purpose is to test all plasma support technologies, such as direct converters and halo pumping systems, in the presence of plasma, and to establish the plasma performance characteristics of the full MARS plasma, including the proper potential distribution, confinement parameters, and plasma parameters in each individual cell. This should all be accomplished while keeping to a minimum the generation of induced radioactivity. Operating with ordinary hydrogen is favored and will allow direct, hands-on maintenance of reactor components, diagnostics, and controls. In MARS this testing is expected to take one to two months, much less than in present experiments.

For tandem mirrors, the use of hydrogen leads to a lower nT in the plugs but this can readily be overcome using the heating systems. It may be more difficult to operate the plasma when ion-cyclotron resonant frequency (ICRF) ω_{CD} heating is used. In MARS, ICRF is used in the central cell for startup. Since the ICRF system is generally tuned to $2\omega_{CD}$ (which is equal to ω_{CH}), it will not heat a pure hydrogen plasma. Using deuterium during this phase will lead to some neutron production and a careful tradeoff is required. (In tokamaks with ICRF as the dominant heating source, hydrogen operation may be precluded except for tests using only ohmic heating.)

At the end of this initial commissioning phase, all subsystems will have been tested and plasma performance verified. The plant can then be commissioned for power operation.

23.2.2 Phase I: Shutdown/Zero Power Mode

This mode of operation encompasses Phase IA, cold shutdown, and Phase IB, hot shutdown. During a fresh startup, and in the infrequent case of drained blanket modules, startup commences from "cold-ion" conditions. The primary piping as well as blanket modules must be gradually brought to operating temperature. This heat-up phase may take 1 to 2 days, depending on available auxiliary heating power and thermal stress limitations. In the MARS design, we have estimated a heat-up period of 30 hours using a helium gas preheat system.

Once the entire primary system is conditioned to a uniform temperature of 350°C, the Li-Pb coolant is pumped out of its storage tank and allowed to fill the primary system. This is accomplished by first filling half of the primary coolant piping system, then the blanket modules bottom-to-top, and finally the remaining half of the piping. This process continues at low pump speeds to minimize pump-induced vibrations. The Li-Pb coolant is subsequently kept at the uniform temperature of about 350°C by pump heat. At this stage, the plant is ready for low power physics testing.

In the hot shutdown mode (Phase IB) the startup period is expected to be much shorter than for Phase IA. The hot shutdown mode can occur frequently as a result of various system failures. When total fusion power is lost, the coolant outlet temperature decreases at the rate of 10°C/s without pump speed compensation. A primary temperature regulation and preheat system (PTR&PS) that respond to fusion power fluctuations has been developed.¹ This system consists of a gas heater, gas blower, electric resistance strap heaters, coolant overflow surge tank, coolant dump tank, and associated instrumentation and control (I&C). In case of a total loss of fusion power,

pump speeds (and coolant flow rates) are adjusted to keep a uniform coolant temperature of 350°C everywhere in the primary. The time variation of decay afterheat is also taken into account. If pump failure occurs, off site power is lost (or shutdown is extended), the electric resistance strap heaters are energized to maintain the coolant temperature at 350°C, and the entire coolant volume is drained into an external dump tank. The Li-Pb coolant is kept molten in the tank by an external immersion-type electric heater, or allowed to freeze if shutdown is expected to be long.

23.2.3 Phase II: Standby Mode; Phase III: Staged Power Increases

At standby power, the reactor plasma operates using only deuterium fuel and the heat transport system is maintained by heat from the pumps. The steam generator and turbine are likely to be unstable if run below 10 to 20% of rated power. We have assumed that no electricity is generated from the blanket thermal cycle when the fusion power is less than 20% of rated power. During standby mode, plasma parameter tests are made to ensure proper plasma operation. Key parameters include:

- Density, temperature, and potential, both axially and radially in the systems;
- Plasma power;
- Plasma beta limit;
- Plasma potential control (and plasma size control) with bias on the direct converter.

To establish whether the plasma performs according to design, performance tests with plasma will have to be carried out for many of the plasma technology support systems. These again include the neutral beams, ICRF and ECRH (and the coupling efficiencies of each), the barrier pumping systems, and the direct converter.

Power ascension tests are expected to last for 20 to 30 weeks, including approximately 3 weeks at full power. There are five major plateaus for power ascension, at 20, 50, 80, post 80, and 100% of full power. System equilibrium is established at each power plateau by regulating the primary coolant and feedwater mass-flow rates. Proper regulation will result in a constant ΔT across the blanket and steam generator at all power levels. Following low power physics tests, engineering tests are performed at the aforementioned power plateaus. Generally, three tests are performed on the heat transport system: turbine trip, loss of load, and loss of flow. These three tests are performed to demonstrate (1) that the models used for design and safety analysis adequately represent the system at those high power levels, and (2) that the entire fusion system performs as designed under these transient conditions.

The parameters and system performance to be monitored include:

- Pressure, level, and flow in the Li-Pb expansion tank;
- Heat transport coolant system (HTCS) temperatures and pressures;
- Steam generator (SG) pressure level;
- Performance of the main and auxiliary feedwater control system;
- Plasma power;
- Charging and letdown flows;
- Pump speeds and pressure drops;
- Strain gauge measurements for fatigue and vibration analysis at critical components.

Following initial commissioning and after long shutdowns, tests are performed at various power levels to verify design and ensure safety during power escalation. The primary tests expected to be performed in a TMR are:

- 0 to 20% power: Plant data are recorded for selected parameters during given intervals ranging from minutes to hours. The feedwater control system (FWCS) is actuated at a power level of 5% to ensure that there is adequate water compensation and that a balanced flow to the steam generators occurs at about 20% power. The ΔT measurements are calibrated and correlated to fusion power through the enthalpy rise across the blanket and coolant flow rate.
- 20% power plateau: During this power plateau, initial turbine roll is established, as well as turbine warmup and loading and checking for noises, vibrations, control system operation, etc. Also, manual trip is initiated and transient parameters are recorded. In addition, the steam bypass control system (SBCS) capacity is checked for safety reasons. Plant chemistry and primary coolant radioactivity are monitored, and baseline chemistry and corrosion data are gathered at the 20, 50, 80, and 100% power plateaus. Calorimetric measurements for power are also performed at each plateau. Offsite power is removed at 40% power by isolating the startup transformers. The turbine is tripped and blackout conditions are maintained for 30 min. Ability to achieve hot shutdown mode (Phase IB) is verified, and selected parameters are measured to ensure that emergency power systems function properly.
- 50% power plateau: Although the dose behind the biological shield is continuously monitored, its effectiveness is critically evaluated by verifying neutron leakage rates. The ability to perform a shutdown from outside the control room is established at this power level. Adequate initiation and performance will be demonstrated at power levels ranging from 50 to 100% power. This is accomplished by activating the cutback system and demonstrating that the resulting power reduction and distribution are as designed.
- 80% power plateau: All plant system pumps are tripped at 80% power, and selected parameters are monitored to verify natural circulation.
- 100% power plateau: A loss of load transient is initiated at 100% power. Selected parameters are monitored to verify proper automatic plant response to the transient.
- Warranty Run: The warranty run is performed as part of initial commissioning and fresh plant start. Generally, one can expect the plant to operate at rated thermal output for about two weeks. During the run, calorimetrics are performed daily.

Figure 23-2 illustrates the various power plateaus and testing operations at each plateau for MARS startup.

To limit the deleterious effects of thermal transients caused by a rapid change in power, the blanket flux level and its rate of change are monitored by the plant I&C system. As a performance requirement, the response time of sensors should be short enough to ensure that rapid flux changes are monitored. In addition, there should only be a minimal delay between the flux/flux change measurement requiring plant shutdown and the time it takes for a trip action. This includes the response time for flux sensors and associated circuitry. The flux sensors could be fission counters or

Fig. 23-2. Power plateaus and corresponding testing procedures for MARS startup. Tests are performed to verify designs and ensure safety during power escalation.

B-10-compensated ionization chambers located in thimbles at the back edge of the blanket. Neutron flux leakage at this location can be taken as an indication of blanket power. This should be calibrated with ΔT measurements in both the primary and secondary for various power levels.

At high power operation, the I&C system should also ensure that sustained operation will not occur with the blanket running dangerously close to its design limit. Fusion system shutdown is initiated, for example, when the signal from the flux monitoring system exceeds a predetermined setpoint that is dictated by acceptance criteria. Furthermore, sufficient I&C

redundancy is required in the system design to ensure that single, random, sensor failures are prevented. The I&C system should also permit periodic testing and calibration of sensors, since extended operation in a radiation environment degrades the efficiency, sensitivity and response time of sensors due to radiation hardening. Details of the I&C system are presented in Sec. 21 of this volume.

23.3 GENERAL CONSTRAINTS FOR STARTUP AND FRACTIONAL POWER OPERATION

The MARS power plant will be a complex system made up of numerous subsystems and components. The physics and engineering aspects of these subsystems together with their dynamic interaction will pose various constraints on the transient operation of the power plant. The startup procedures, constrained in this fashion, should be devised such that transient operation does not result in any major degradation of the economics of the power station. This argument underlines the basic interaction of startup procedures and baseline design. On the one hand, startup scenarios that do not require special performance from the plant's subsystems are preferable. On the other hand, the startup requirements should be incorporated in the baseline design.

Startup constraints can be divided into three general categories. The first consists of those constraints brought about by the design of the plant components. These subsystems are generally constructed to operate most efficiently at full power. As such, the plant may encounter severe difficulties operating even steadily at fractional power. For example, the turbines may not be operational below 20% of their rated design parameters. Careful baseline design of components reduces the scope of these constraints. An example is a neutral beam injector system with ion currents that are increments of the number of independent sources and accelerator units. The beam current can then be easily adjusted. This injector system operates much more efficiently at fractional power than an injector unit with one ion source. This relation between the performance of the subsystems and the power level should be carefully incorporated in any design.

A second set of constraints arises as a result of the dynamic interaction of the plant's subsystems. Each system has its own response time, which can vary greatly. Usually, the dynamic behavior of the reactor is dictated by the subsystem with the slowest response time. In a tandem mirror reactor the fusion core has a relatively fast response time. It is basically determined by the ion-ion collision time and the rate at which the alpha particles in the system slow down. Typical plasma response times are in the range of 0.5 to 2 s. The engineering components such as blanket and thermal conversion systems have relatively long response times (minutes to hours or days). The rate of change in power is then dictated by the response time of these components. The fusion core should be programmed to follow this time path. Moreover, the time the reactor spends in each phase of the startup procedure is generally determined by the response time of the subsystems involved in that phase.

Finally, there are some systems that are solely needed for plasma startup. Experience with today's tandem mirror experiments has shown that plasma initiation and heating is a major task by itself. Special equipment is generally needed. This equipment usually dominates the experiment and

sometimes even dictates experimental procedures. It is clear that this is not acceptable in a commercial reactor. Thus, startup procedures should be chosen that require a minimum of special startup equipment. The auxiliary heating systems required for full power operation should be utilized as fully as possible during startup, whereas the total input power for startup should also be minimized for economic reasons. If the input power is small enough, it can be tapped directly from the electric grid system, eliminating special startup power storage and supply systems.

One can apply these general constraints to the various startup phases. To minimize the startup equipment and input power, the plasma initiation and heating phase (Phase II) should be implemented rapidly so that plasma can also be heated by fusion-born alpha particles. On the other hand, the staged power increase phase to full power (Phase III) should be implemented slowly because of the slow response time of the engineering subsystems.

The standby phase (Phase II) is therefore especially important. The plasma is producing 1 to 20% of full power but the thermal conversion systems are not operational. Input power is needed to maintain the plasma in this phase. The plasma parameters in the standby phase should be carefully chosen to minimize this input power. In a tandem mirror reactor, direct converter units can be operational during this phase and their use can reduce the required input power. One can envision scenarios where the direct converter output is enough to sustain the plasma in the standby phase. This self-sufficient standby phase is unique to tandem mirror reactors.

The longest time scales appear to be dictated by engineering constraints rather than physics performance. In short, the rate of fusion power variation must be slow enough to meet the following limitations: (1) Stress and material compatibility limits--very fast power variations are prohibited to avoid shock waves in the blanket system and to avoid rapid differential thermal expansion of the coolant-breeder-structure system; (2) auxiliary power requirement limits--it has been shown, based on available auxiliary power, that electric resistance preheating requires a prohibitively long time, and that gas preheating for the primary blanket system takes about 1 to 2 days; (3) mechanical limits--the inertia of pumps and valves is responsible for the slow response of the primary coolant system to rapid power variations. Pump startup, for example, must provide extra torque for fluid acceleration, acceleration of rotating parts, and magnetohydrodynamic (MHD) startup losses. The pump and valve response times are important because the coolant flow velocity is the variable controlled to maintain a given value of ΔT through the blanket at different levels of output power. Plasma operation will therefore track the slower time scales dictated by the engineering constraints. In general, the blanket and piping system design must accommodate differential expansion caused by temperature excursions. A primary temperature regulation and preheat system should be incorporated as a part of the overall power plant. Finally, design tests must show that the system can adequately respond to a limited number of induced power transients without major component failures.

23.3.1 Plasma Startup and Heating

The initial plasma startup in MARS should be based on particle and power balance constraints that are consistent with general MHD and plasma microstability criteria. A model based on a set of appropriate particle and power balance equations has been solved analytically for the dominant terms to

yield bounds on temperatures and densities as functions of the trapped particle pumping frequency, the rf power, and machine-dependent parameters (see Sec. 2.11). The rf absorption calculations have been added to the particle and power balance constraints. In particular, a model of ICRF coupling and absorption together with a simple antenna-plasma circuit model have provided a more accurate estimate of central cell plasma heating. The startup scenario can be summarized as follows:

1. Initiate the plasma in the end cell with electron-cyclotron resonant heating (ECRH) on the background gas. Both the fundamental frequency in the plug and second harmonic in the barrier are required to form a population of hot mirror-trapped electrons. A neutral gas density of approximately 10^{16} m^{-3} is required to initially form this plasma. The achievable hot electron density is in the range of 5×10^{18} to $5 \times 10^{19} \text{ m}^{-3}$ from power balance considerations. This procedure builds β in regions of good curvature for MHD stability.
2. Heat the central cell ions with ion-cyclotron resonant heating (ICRH). This reduces the thermal barrier filling rate and establishes longer confinement times. The ions are initially produced from collisions with the hot electrons leaking in from the barrier. An ion temperature of 200 eV can reduce the pumping frequency below 10^4 s^{-1} (for $g = 2$). This is achievable with an open circuit source power of 2 MW for a plasma with $r_c = 40 \text{ cm}$, $n_{ic} = 10^{18} \text{ m}^{-3}$, and a neutral density $n_0 = 10^{16} \text{ m}^{-3}$. The power requirement can be reduced, or the achievable temperature increased, by decreasing the plasma radius (and to a lesser degree its density).
3. Drift orbit pumping is activated concurrently with the ICRH. Whereas ion temperatures in the 100-eV range are required to reduce trapping from ion-ion collisions, halo shielding must occur to bring the pumping frequency below 10^3 s^{-1} (for $g = 2$).
4. Sloshing ions are generated in the end cell to build the electrostatic plug potential. Drift orbit pumping and ECRH (at both the plug and barrier) continue to operate as the proper space potential is built up.

23.3.2 MARS Plasma Support Technologies

The plasma support technology system, specifically the ECRH, ICRF, neutral beam, and direct converter units, is examined for operation during the startup and transient operation of a tandem mirror device. Three critical areas are: (1) operation of plasma technology systems at below full design parameters; (2) interaction of these systems with the plasma during transient or fractional power operation; (3) constraints imposed by these systems on MARS plasma startup scenarios.

23.3.2.1 Electron-Cyclotron Resonant Heating System. Two ECRH systems are required at each end of MARS. One located in the plug serves to heat the warm electrons trapped in the plug potential. The other is used to create and sustain the mirror-trapped hot electron population required in the barrier region. Either or both systems can be used to initiate the target plasma in the plug-barrier region for the sloshing ion beams.

Various levels of microwave heating power are required at different power plateaus. For fast time scales (microseconds) used for fine tuning, the output power of each gyrotron can be changed by changing the gun anode voltage or the gun magnet coil current. In the case of slow time (~ 1 s) variations, this can be achieved by adjusting the heating voltage (current) of each tube. For cold startup of the tube, a complicated procedure is required and the heater voltage must be changed slowly to avoid thermal stress damage to the gun.

To fulfill the requirements of high power (~ 1 MW) and high frequency (~ 100 GHz) in a MARS reactor, we adopted a quasi-optical ECRH system (see Sec. 4). In particular, the quasi-optical gyrotron offers the possibility of variable frequency operation that is attractive in terms of heating during startup. With closed-cavity gyrotrons, an ECRH system arrangement for transporting microwave energy to the plasma is shown in Fig. 23-3. This arrangement formed the basis of an interim design for the MARS ECRH configuration and is discussed in detail in Ref. 2. The beam waveguide basically consists of strategically spaced parabolic and hyperbolic mirrors that serve as phase transformers directing the beam while maintaining high energy concentration along its axis. The favorable feature of this system is the presence of the offset Cassegrain mirror system that allows the gyrotrons to be shielded from neutron flux by the intervening hyperboloid, which is in turn protected by a neutral absorber. The drawback is the complexity of the mode converters placed between the source and the horn-reflector antenna. However, in terms of startup operation, the system did not provide the flexibility of variable frequency mainly as a result of the choice of gyrotrons.

The final design of the MARS ECRH system is discussed in detail in Sec. 4 of this volume. This system employs quasi-optical gyrotrons capable of variable frequency operation and, therefore, facilitates heating at startup where resonance frequencies are determined by the time-dependent beta.

23.3.2.2 Ion-Cyclotron Resonant Heating System. Radio frequency heating in the ion-cyclotron range of frequencies serves several important functions in MARS, including: central cell ion heating at startup; plasma initiation; ion heating in the anchor region for MHD stability; and driving the reactor from one power plateau to another.

Because the plasma beta may change as a TMR is taken through the various fractional power stages, the ICRF system should be able to operate at different frequencies. This requires a substantial technological advance in power tube designs so they are capable of changing frequency while operating. Of course, this problem can be avoided if constant plasma beta is maintained throughout the fractional power operating stages. However, for the ICRF central cell heating system, there may be the need to change from fundamental to second harmonic heating as the plasma density increases during plasma startup. One possible solution is to introduce a minority hydrogen component at plasma initiation and heat the central cell ions throughout startup at $\omega = \omega_{CH} = 2\omega_{CP}$ resonance. A study is needed of the tradeoff between a single frequency system with a complicated startup process and a multiple frequency system with a simpler startup procedure.

Fig. 23-3. An interim design of the MARS ECRH launching system. This design did not provide the flexibility of variable frequency required for startup.

A second issue concerns matching the source to the antenna load during plasma transients. If changing the plasma radius is used to vary the central cell power, this issue becomes even more acute, since the loading resistance is highly sensitive to the plasma-launcher distance. Also, if the density in front of the launcher is high enough ($>10^{18} \text{ m}^{-3}$), the evolving core plasma parameters are likely to change the loading resistance. Changing frequency also alters the plasma load. An advanced stub tuning system needs to be developed that can respond to the changing plasma load so that a satisfactory match can be maintained throughout startup.

23.3.2.3 The Neutral Beam System. Although neutral beams play several roles in present tandem mirror experiments, only the beam needed to produce the sloshing ion distribution in the plug-barrier cell is retained in MARS. The sloshing ion beamline must produce about 6 MW of 475-kV neutral deuterium power on-target in the end cell during full power operation. The constraints that affect the ability to produce neutral beam powers above or below this level, as required during fractional power operation, and the startup characteristics of the neutral beam systems have been examined.¹

Ion sources using multigrid accelerators operating at nearly optimum perveance deliver a current to the target proportional to $V^{3/2}$. These systems are useful at energies below 200 keV and are capable of higher current densities at the accelerator than are possible with "focusing" systems. Focusing accelerators for use above 200 keV operate at reduced current density (which is nevertheless expected to exceed that of a negative ion source in use today). The beam current available from these systems is somewhat independent of the total accelerator voltage above a minimum preaccelerator voltage. Startup scenarios that require variable voltage and current can be implemented with the help of the focusing accelerators at the expense of complicated power supplies and control systems. Operation at reduced power is best achieved by operating at reduced current and constant voltage.

The beam power deposited in the plasma depends on the beam current and voltage and the plasma density and temperature. For fractional power operation, the plasma radius (or density) is reduced below that of full power, leading to greater beam shine-through and possibly resulting in a larger heat load on the beam dumps. However, this is not a serious problem since the required beam power in the fractional power phase is smaller than that of rated power. A more serious problem arises during the plasma initiation phase when the sloshing ion beams are injected in a low-density target plasma (produced by ICRH breakdown of background gas), resulting in large beam shine-through and heat load on the dumps. As such, the beam dumps designed to withstand the load during plasma initiation probably will not encounter any difficulties during other phases.

23.3.2.4 Direct Converter. The direct converter recovers energy from the charged-particle end loss. During startup and testing of the reactor components, the direct converter can supply some fraction of the power required by the auxiliary technologies such as the neutral beams and heating systems. Indeed, during the extended testing period that will be required before fusion power is generated, the direct converter can decrease the amount of power purchased from the power grid. Using the direct converter throughout the different startup phases is important to plant economics.

The startup and operation of the direct converter is also closely coupled to the density and potential profile evolution. The core plasma potential relative to the grounded halo plasma is ultimately determined by the direct converter potential. Accurate biasing of the direct converter relative to ground is required so that the radial electric fields in the core, and consequent enhancement in radial transport, do not impede operation of the reactor. The barrier and end plug potentials are established relative to the central cell potential. The direct converter must collect the end-loss plasma flux for varying plug parameters during startup and still ensure that proper central cell potentials are maintained. Constraints on the direct converter operation under these two separate requirements have been examined.

The plasma potential in the central cell relative to the halo plasma is related to the bias of the end collector plate and not to any grid structure. Establishing the proper central cell potential for the minimum radial electric fields is achieved by directly biasing the collector plate relative to the halo plasma dump. Without the formation of the plug potentials, the electrons have only their thermal energy, which cannot be efficiently recovered by a single-stage system. Practical energy recovery is based on collecting the energy of central cell electrons as they are accelerated through the potentials of the plug region. Since the plug potentials are generated by the

ECRH, ICRF, and sloshing ion neutral beams, and because the collector is biased to maintain the correct central cell potential, energy is automatically recovered. Complementary biasing of the collector in a manner that tracks the plug or central cell potential is necessary. This coupling for proper TMR operation does not impose any serious physics constraints on the electron energy recovery, as is the case for the ion direct converters where space-charge is a problem. Once the axial potential profile and collector bias are established, energy recovery will occur at near constant efficiency over a large range of end-loss electron currents that can occur during startup.

During startup of the MARS end cells, establishing the final plug potentials quickly results in a constant particle energy at the converter entrance. During the time it takes to establish the axial potential profile, the collector of the electron converter is biased to minimize the radial electric field in the central cell. Energy recovery efficiency will be the highest for the largest particle energy because space-charge effects are minimized.

The direct converters automatically recover energy as the plug potentials form and the central cell potential is tracked for stability. The efficiency will be nearly constant over the startup range of parameters because of the lack of charge exchange and secondary electron emission. The formation of the axial potential profiles will require that the radial ion drift pump be operational, and the necessary charge separation for direct converters to work will be achieved. Space charge is not a problem in this system because the electron energy is high and there are no grids.

23.3.3 Thermal Hydraulics of Lithium-Lead-Cooled Blankets During Startup and Shutdown

A blanket module for the MARS tandem mirror reactor uses lithium-lead in the form $\text{Li}_{17}\text{Pb}_{83}$ as discussed in Sec. 12. Under full power operating conditions, the coolant enters the top at 350°C and exits the bottom at 500°C. The following specific thermal hydraulic issues relating to startup have been examined: (1) initial blanket flooding with liquid Li-Pb and a determination of the system response; (2) the design of a blanket preheating system to avoid problems with hot flooding of a cold structure; (3) the thermal hydraulic response of the blanket at shutdown, specifically whether natural circulation is sufficient to remove the radioactive afterheat; and (4) blanket operation at fractions of full power.

23.3.3.1 Initial Blanket Flooding and the Preheat System. If a cold blanket is initially loaded with hot coolant, some freezing is found to occur for a short time (2 s), after which the frozen film remelts. The maximum blockage of a first-wall tube cross section is only about 15%. Thus, the degree of freezing is not large enough to cause blockage. However, thermal shock is found to exceed the yield stress, indicating that blanket preheating will be required.

The largest thermal shock is predicted to occur at the tube entrance where an initially cold tube is suddenly exposed to hot coolant from the inside. The transient thermal strain distributions through the tube wall are obtained numerically by solving the time-dependent heat conduction equations. The thermal strain history of the tube wall is subsequently determined with use of plate theory. The thermal strain is plotted in Fig. 23-4 as a function of time for various locations across the tube wall. It is shown that on the

Fig. 23-4. Thermal strain of the tube wall plotted as a function of time for various locations across the wall.

coolant side of the tube, the compressive strain exceeds the yield strength of the material for short periods of time, leading to plastic deformation and strain ratcheting over several cycles. Although fresh startup is not likely to be a frequent event, we believe that any stresses that exceed the yield stress should be avoided in light of problems associated with fatigue, radiation embrittlement, and uncertainties in the HT-9 data. We have concluded that the blanket needs to be preheated to a temperature higher than the ambient (20°C). Similar results have been obtained for flooding the tubes with hot Li-Pb having different initial temperatures. At preheating temperatures of 300°C , the ratio of peak strain to yield strain drops to 0.384. From the standpoint of fatigue failure, however, it is desirable to lower this ratio as much as possible.

Electrical and gas preheating systems have been analyzed in terms of cost and efficiency. Helium is found to be suitable for preheating. Having a gas supply temperature of 500°C and desiring the blanket temperature to

reach 350°C, we estimate that 30 hours of preheating is necessary. Pump transients have also been analyzed. We have estimated the inertia of the moving parts of the pump from the manufacturer's data on a similar pump and have found, rather surprisingly, that the response time is relatively rapid (order of seconds).

23.3.3.2 Natural Circulation and Shutdown. In the event of loss of coolant flow, the fusion power may be shut off immediately but heat deposition will continue in the blanket structure in the form of decay afterheat (v1% of full power)--see Sec. 12.13. Can the decay afterheat be removed by natural circulation? Natural circulation is caused by buoyancy forces and has long been considered in pressurized and boiling water reactors, where the water vaporizes and creates a large density difference (large buoyancy forces), which results in natural circulation. In the MARS blanket, where the coolant is Li-Pb, there is phase change. Therefore, the buoyancy forces are relatively small and are due only to thermally induced density differences.

For MARS, natural circulation can be effected by passing the coolant through a steam generator or a special water tank. Figure 23-5 shows the schematic of such an arrangement. It should be noted that the position of heat sink is critical and should be at the highest elevation to generate natural circulation.

The assumptions for the physical model are that the blanket tubes and beams can be approximated by an average straight tube (H_b , D_b), the heat deposition rate is constant, the heat sink is located at an elevated point, and the heat is removed at the rate generated. Full details are given in Ref. 1, and results are summarized in Fig. 23-6. For this type of blanket, a decay afterheat level up to 2.3% of full power can be removed by natural circulation without exceeding a maximum wall temperature of 500°C. Since the actual afterheat level is less than 2 to 3% of full power, there appears to be no afterheat problem with this configuration.

23.3.3.3 Engineering Strategy for Blanket Operation at Fractions of Full Power. There are two distinct ways to deal with operating the reactor at fractional power levels: (1) maintain constant coolant flow rate and (2) adjust the coolant flow rate in proportion to the fusion power. The first approach will result in a variable coolant exit temperature, which in turn changes the blanket (structure) temperature. This can result in problems associated with transient thermal conditions, particularly through thermal stresses and fatigue. In addition, components in the balance of the plant (such as the heat exchangers) would have to be adjusted to deal with variations in the maximum coolant outlet temperature. In the second method, when the fusion power changes, the coolant flow rate is adjusted to maintain an essentially constant coolant exit temperature. The blanket structure thermal condition remains essentially unchanged.

We have focused on this second approach. It is a method that is particularly well-suited to liquid-metal-cooled blankets since most of the heat is generated directly in the coolant.

As for the procedures for implementing changes in power level, the issue is how fast such a transition can occur. Studies of physics considerations have shown that plasma response times are short, on the order of a second. Therefore, the transition is limited by engineering considerations. The procedure is to adjust the coolant flow rate such that the same coolant temperature rise in the blanket is obtained. We have examined factors

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Fig. 23-6. Temperature of the blanket structure and volume flowrate of Li-Pb under natural circulation conditions as a function of the blanket afterheat level. Since the MARS afterheat level is <3%, the maximum wall temperature will always remain <550°C.

affecting the coolant flow rate, such as time constants for controls, pump inertia, inertia of the coolant, pressure drop, and so forth. In particular, we examined the transient behavior of the coolant loop including the pumps. We found the time constant of the pump due to pressure drop, coolant inertia, and pump inertia to be less than 10 s.¹

Therefore, the appropriate scenario to maintain the coolant exit temperature is to first adjust the pump power (which in turn affects the coolant flow rate) and then to alter the fusion power by tracking the coolant exit temperature. Because the volumetric distribution of heat in the blanket remains unchanged at fractions of full power (only the amplitude changes), this approach will result in minimal thermal and stress changes.

REFERENCES

1. R. W. Conn et al., START-TM, A Study of Startup and Fractional Power Operation of Tandem Mirror Fusion Reactors, University of California-Los Angeles, Report PPG-764 (January 1984).
2. L. J. Perkins, S. A. Freije, and W. S. Neef, "Engineering Design of the Quasi-Optical ECRH Injection System for the Mirror Advanced Reactor," Nucl. Tech./Fusion 4, 1407 (1983).

24 Plant Availability

24. Plant Availability

24.1 INTRODUCTION

For this analysis, we define availability as that fraction of the reactor's lifetime when it is producing electricity at full capacity. We have examined the MARS availability with two primary goals: first, to ensure that we have obtained a self-consistent system design. For example, we anticipate that the blanket modules will be replaced at the rate of 25% of the reactor total per year, while the reactor first-wall damage rate (displacement-per-atom, specifically) indicates that the blanket lifetime exceeds 4 years. These two requirements are compatible for a reactor that is 80% available, i.e.,

$$\text{annual replacement fraction} = \frac{\text{plant availability}}{\text{module lifetime (years)}} .$$

Second, to make certain that the conceptual design provides a balance between systems hardware design, plus operations and maintenance plans. These two factors combined produce the maximum reactor availability.

In our analysis, we have estimated hardware availability when real data were not available. In the same manner, we have postulated the way in which each of the MARS systems may fail as well as the probable effect of such failures. Table 24-1 summarizes these estimates and defines each system's unavailability because of equipment failure. The table also contains the mean time between failure (MTBF) and mean time to repair (MTTR) for each subsystem together with comments on how the values were derived.

Similarly, we have compiled scheduled maintenance activities for each system, anticipating a once-a-year planned reactor downtime. At that time, 25% of the reactor blanket modules will be replaced and the bulk of the routine maintenance activities for the reactor and balance-of-plant that cannot be performed on-line (because of redundancy) will be accomplished. Table 24-2 summarizes, by subsystem, the scheduled maintenance activities for the MARS system.

24.2 METHODOLOGY

24.2.1 General

We have chosen to assess the equivalent of MARS steady-state availability. This choice affects the results in the following ways: (1) The startup phenomena (i.e., startup at beginning of reactor lifetime) is not considered, which could include low-probability events like magnet failure during the acceptance testing process, which would delay reactor startup until another magnet was procured. (2) The difficulties in scheduling maintenance activities are not directly considered; the analysis assumes resource (including manpower, equipment, and space) availability to accomplish all repairs or, conversely, that the unavailability of repair resources has been considered in the repair parameter definition. (3) The degraded operating states that are caused by partial failures in supplemental heating systems are approximated here (i.e., the power producing capability of the plasma has been

Table 24-1. System unscheduled outage data.

Code of accounts designation	System/subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
22.01.01.01	Blanket	Li-Pb leak	3 ^c	11	Reactor outage during changeout
22.01.01.02	Reflector	H ₂ O leak	15 ^c	40	Conservative design, simple structure
	Bellows seal	Vacuum leak	5 ^c	10	Based on bubble chamber experience
22.01.01.03	Service station closure	Vacuum leak at cover	5	0.5	Reactor can operate until scheduled downtime
		Vacuum leak in wall	20	7	Reactor can operate until scheduled downtime
	Service station cover	Vacuum leak at penetration	2	6	Reactor can operate until scheduled downtime
	Removable reflector shield	Vacuum leak at interface	5	7	Reactor can operate until scheduled downtime
		Water leak in service station	10 ^c	7	Reactor shutdown required
	Turbo-molecular pump	Fails to start	10	4	No steady-state impact
		Bearing failure	5	4	Reactor can operate until scheduled downtime
		Failure of vacuum valve	5	4	Reactor can operate until scheduled downtime

Table 24-1. (continued)

Code of accounts designation	System/ subsystem	Failure mode	MTBF (years) ^a	MTTR (days)	Comments
22.01.01.04	Omega bellows	Li-Pb leak	20 ^c	5	Based on bubble chamber experience reactor operation permitted until normal shutdown
	Lower seal	Vacuum leak	4 ^c	5	Based on bubble chamber experience, reactor operation permitted until normal shutdown
	Upper bellows	Vacuum leak	4 ^c	5	Based on bubble chamber experience, reactor operation permitted until normal shutdown
	Lower bellows	Vacuum leak	4 ^c	5	Based on bubble chamber experience, reactor operation permitted until normal shutdown
	Upper seal	Vacuum leak	4 ^c	5	Based on bubble chamber experience, reactor operation permitted until normal shutdown
	Clamp drive, clamp mech- anical	Inoperable	30	5	Secondary impact; important only during a mainten- ance activity
	Solenoid	Normally open, stuck open	5	1	Replace at scheduled downtime; may have manual override

Table 24-1. (continued)

Code of accounts designation	System/ subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
	Cooling/ heating lines	Large H _e leak	10 ^c	5	Reactor shutdown required
		Small H _e leak	5 ^c	5	Reactor operation may continue until scheduled shutdown
22.01.02.01	Central cell shield	Water leak	25 ^c	40	Shutdown required
22.01.02.02	Choke coil shield	Water leak	30	20	Shutdown required
22.01.02.03	Transition coil shield	Water leak	30	20	Shutdown required
22.01.02.04	Anchor coil shield	Water leak	30	20	Shutdown required
22.01.02.05	Plug coil shield	Water leak	30	20	Shutdown required
22.01.02.06	Recircu- larizer coil shield	Water leak	30	20	Shutdown required
22.01.03.01	Central cell magnets	Conductor layer-to-layer or turn-to-turn short	~100	30	Estimate of MTBF. Assume no failure intensity during lifetime of 30 years
		Insulator deteriora- tion	~50	30	Radiation-induced damage or defective materials. Materials selection, shielding selected result in negli- gible failure intensity during lifetime

Table 24-1. (continued)

Code of accounts designation	System/subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
		Vapor-cooled leads burn-up	>100	30	Related to loss of He gas
		Collector leads collapse	--	30	Detectable during acceptance test. No failure intensity during reactor lifetime
22.01.03.02.01	Superconducting choke coils	Availability data similar to central cell magnets			
22.01.03.02.02	Normal choke coils	Coolant leakage at joint	10	10	Leakage must be of sufficient magnitude to warrant shutdown
		Broken/damage insulation	--	10	Defective materials detectable during acceptance testing. Radiation damage controlled through shield design; replacement at scheduled downtime acceptable
		Insufficient cooling	--	10	Preventable through interlocks
22.01.03.03	Transition coils	Availability data similar to central cell magnets			
22.01.03.04	Anchor coils	Availability data similar to central cell magnets			
22.01.03.05	Plug coils	Availability data similar to central cell magnets			

Table 24-1. (continued)

Code of accounts designation	System/subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
22.01.03.06.01	C-shape recirculator coils	Availability data similar to central cell magnets			
22.01.03.06.02	Recirculating solenoid	Availability data similar to central cell magnets			
22.01.04.01	Sloshing ion beam	Gun filament failure	>400	4 hours	Other components neglected; filament is clearly the weak link
22.01.04.02	Thermal barrier ECRH	Similar availability data to above			Active redundancy of gyrotrons provided to permit on-line changeout
22.01.04.03	Potential peak ECRH	Similar availability data to above			Active redundancy of gyrotrons provided to permit on-line changeout
22.01.04.04	Anchor ICRH	Similar availability data to above			Same comment as sloshing ion beam
22.01.04.05	Startup ICRH	Not considered in steady-state assessment			
22.01.07.01	Supplemental heating power supply	Non-specific; availability of comparable dc transmission power supply = 0.9985			
22.01.07.02	Magnet power supplies	Shorted thyristor	10	2	Estimate for failure for water-cooled, solid-state device. Replacement item
		Shorted transformer	725	2	Replacement item

Table 24-1. (continued)

Code of accounts designation	System/ subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
		Controls	--	--	High availability, arbitrarily redundant
22.01.07.03	Startup ICRH power supply	See 22.01.04.05--no impact			
22.01.08	Drift pump coils and power supplies	Availability data similar to normal choke coils			
22.01.09	Direct converter	Channel burn through- collectors and halo scrapers	20	10	Reactor down; repair is sector replacement
22.02.01	Heat transfer (Li-Pb coolant)	Pump failure	7	<14	Treat as non-repair- able but inter- changeable during reactor operation; redundancy provided heat exchangers, piping assumed not to fail
22.02.02	Heat transfer (reflector H ₂ O)	Pump failure	7	<14	Treat as non- repairable but interchangeable during reactor operation; redund- ancy provided heat exchangers, piping assumed not to fail
22.02.03	Heat transfer (end tank)	Pump failure	7	<14	Treat as non-repair- able but inter- changeable during reactor operation; redundancy provided heat exchangers, piping assumed not to fail

Table 24-1. (continued)

Code of accounts designation	System/ subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
22.03.01	Auxiliary cooling (cryogenics)	Compressor bearing failure or loss of lubrication	3	52 hours	Some redundancy and over-capacity provided
		Turbo-expander engine failure	3	52 hours	Some redundancy and over-capacity provided
		Dewar and gas storage failure	>30	8 hours	Very low failure probability
		Structural failure of turbine or vacuum jacket	>30	8 hours	Very low failure probability
22.05.01	Fuel handling and storage (tritium extraction)	Cover seal leak	20	24 hours	Neglect nozzle and vacuum pump failures arbitrary redundancy
22.05.02	Fuel handling and storage (fuel preparation)	Valve sticks plug, etc.	10	2 hours	Fully redundant; neglect sensor failure. Operation continues during repair
22.05.03	Fuel handling and storage (fuel purification)	Plugged or leaking lines	~10	16 hours	No impact on availability. Fully redundant components allow operation for ~2 days during which failed units can be repaired

Table 24-1. (continued)

Code of accounts designation	System/subsystem	Failure mode	MTBF (years) ^a	MTTR (days) ^b	Comments
22.05.04	Fuel handling and storage (storage)	Valve or piping failure	~10	~8 hours	Full redundancy installed, low frequency of occurrence. Negligible impact (spare tankage also provided) tank failure unlikely
22.05.05	Fuel handling and storage (fuel injection)	Gas puffer, valves stick	10	8 hours	Fully redundant hardware
		Pellet fabricator malfunction	5	8 hours	Fully redundant hardware
		Railgun electrical failure	5	16 hours	Fully redundant hardware
22.05.06	Fuel handling and storage (air detritiation)	Highly redundant system--no direct availability impact			
23 and 24	Turbine plant and electric plant equipment	Non-specific	--	--	Use historic data for nuclear systems

^aData are in years except where hours are indicated.^bData are in days except where hours are indicated.^c"Whole reactor" specification.

Table 24-2. Scheduled outage data.

Code of accounts designation	System/ subsystem	Activity	Comments
22.01.01.01	Blanket	Replacement of 25% each year	25% replacement inti- mately tied to module lifetime of >4 years at 80% availability. No maintenance require- ment other than replace- ment
22.01.01.02	Reflector	--	No specified mainten- ance activity
22.01.01.03	Service station	--	No maintenance require- ment besides those stations at which module replacement occurs
22.01.01.04	Piping and seals	Routine inspection continuous monitor- ing during opera- tion	Inspection and some tests required at annual shutdown; continuous monitoring during operation
22.01.02	Shields	--	No anticipated maintenance on a regular basis
22.01.03	Magnets		
22.01.03.01	Central magnets	No requirements	No annealing requirement; de- signed for 24 FPY
22.01.03.02.01	Supercon- ducting choke coil	Removal every 4 years at scheduled outage	Routine inspection during removal; de- signed for 24 FPY without anneal
22.01.03.02.02	Normal choke coil	Replacement every 4 years at scheduled outage	Replacement item; roughly 2 day re- placement time, each end--may be done in parallel

Table 24-2. (continued)

Code of accounts designation	System/ subsystem	Activity	Comments
22.01.03.03	Transition coil	Anneal (warmup)	Required anneal after more than 1 FPY; can be accommodated within 30 day anticipated shut- down
22.01.03.04	Anchor coils	Anneal (warmup)	Required anneal after more than 1 FPY; can be accommodated in 30 day anticipated shutdown
22.01.03.05	Plug coils	Anneal (warmup)	Required anneal after more than 1 FPY; can be accommodated in 30 day anticipated shutdown
22.01.03.06.01	C-shape recircular- izing coil	Anneal (warmup)	Number of anneals less than above, but not specified
22.01.03.06.02	Recirculariz- ing solenoid	Anneal (warmup)	Number of anneals less than above, but not specified
22.01.04	Supplemental heating	Gyroytron/filament replacement	Provision made for changeout in <4 hours on a continuous basis as a hedge against short lifetimes. In- spection and mainten- ance on other components as required at annual shutdown or during other reactor shut- downs, access permitting
22.01.07.01	Supplemental heating power supplies	Inspection, calibration	Accomplished at annual shutdown; monitored during operation

Table 24-2. (continued)

Code of accounts designation	System/subsystem	Activity	Comments
22.01.07.02	Magnet power supplies	Calibration/ inspection of quench detector and breaker driver circuitry	No downtime requirement during annual shutdown or reactor operation (spares provided); 6 month interval recommended
22.01.07.03	Startup ICRH power supply	Calibration/ adjustment	Performed while startup system is not on line
22.01.08	Drift pump coils and power supplies	Roughly annual replacement	Designed for reasonably straightforward removal/insertion; performed at annual shutdown
22.01.09	Direct converter	Inspection/ replacement as required	Performed on an annual basis at routine shutdown as required; Inner halo scraper is a minimum lifetime component--rated at 12 FPY
22.02.01	Heat transfer, Li-Pb coolant	Pump inspection/ replacement	Failures during reactor operation are valved off and replaced at annual shutdown. All units inspected and maintained as required at this time
	Heat exchangers	Inspection/ replacement/repair	While no failures are considered in this assessment, it is anticipated that at annual shutdown appropriate maintenance of these devices will be accomplished on an as-needed basis within the annual shutdown period

Table 24-2. (continued)

Code of accounts designation	System/subsystem	Activity	Comments
22.02.02	Heat transfer	Similar to 22.02.01	
22.02.03	Reflector H ₂ O and end tank	Similar to 22.02.01	
22.03.01	Auxiliary cooling-cryogenics	Service compressors expansion engines, removal of "air icicles," replace adsorbers	Performed on line, drawing down on reservoirs or exploiting installed redundancy; all equipment also inspected at annual shutdown
22.05	Fuel handling and storage	Nozzle replacement redundant valves, sensors	Performed at annual shutdown or online as permitted
	Fuel injection	Service pellet fabricators, railguns	Performed at annual shutdown or during forced outages, as allowed
23 and 24	Turbine plant and electric plant equipment	Inspection/replacement refurbishment	Most equipment to be serviced on an annual basis, with opportunity outages during the year providing for additional service times

included, but the thermal hydraulics and power conversion calculations for these reduced power operating states have not been performed).

We have not included acts of God, personnel error, or severely propagating failures (i.e., multiple-barrier failures with synergistic effects; we assume that with this design such failures are improbable). Our assessment here is based on equipment that can fail, resulting in repairs to the failed equipment and the performance of necessary startup operations.

24.2.2 Numerics

We have treated all components as binary states (i.e., working or nonworking). All subsystems are considered either as independent systems or their common mode failures are elevated to a separate level of development in the logic model (e.g., failure of the common power supply for all of the gyrotrons is considered at a higher level of development than the gyrotrons themselves).

The top-level equivalent-availability logic model is shown in Fig. 24-1. It is analogous to a series electrical circuit, where "failure" of any element constitutes system failure. Figure 24-2 shows an expanded development for the block "cryogenics subsystem". For any diagram like Fig. 24-1, the system availability is determined by

$$\Delta_s = \prod_{i=1}^N \Delta_i$$

where $\Delta_s \equiv$ the system availability and $\Delta_i \equiv$ the subsystem availability.

Fig. 24-1. Availability model (top level).

For a subsystem development like that shown in Fig. 24-2, each of the "parallel" blocks is converted to an equivalent series block. For a parallel block where N items are supplied and M are required to be "working," the availability of the equivalent series block is derived from

$$A_b \equiv (A)^N + (A)^{N-1} (1 - A) + \dots + \frac{(N!)}{(N - M)! M!} (A)^N - M(1 - A)^M, \quad ,$$

where $A_b \equiv$ equivalent series block availability.

Cryogenics System

A = Oil injection screw compressors
 B = Oil bearing turbo-expander engines
 F = LHe recovery compressors
 G = LN₂ booster compressors
 H = LN₂ recycle compressors

Notes: (1) Piping, heat exchangers, dewars not included
 (2) Items A, F, G, H shown in parallel for availability (capacity implications)
 (3) Items B shown in series

Fig. 24-2. Availability model (lower level of development).

In the case of the electron-cyclotron resonant heating (ECRH) systems, different reactor power levels can be associated with different numbers of operative gyrotrons. The equivalent availability is given by a generalization of the above

$$A_c \equiv (A)^N F(N) + (A)^{N-1} (1-A) F(N-1) + \dots + \frac{N!}{(N-M)! M!} (A)^{N-M} (1-A)^M F(N-M),$$

where $A_c \equiv$ block equivalent availability and $F(N) \equiv$ system power factor for N functional units.

For certain components (e.g., Li-Pb pumps), repair is considered unlikely during reactor operation. For these systems, availability and reliability are identical. In the absence of detailed failure information, the failure probability per-unit-time is assumed to be constant and the probability that an individual component survives to time t is given by

$$P(t) = e^{-t/T},$$

where $T =$ mean time to failure for this component. The availability of this system is calculated from

$$A_p \equiv 1 - \frac{PPD}{(365-K)}$$

$$PDD \equiv (MTTR) (FD)$$

$$FD = 1 - [A^N + A^{N-1} (1-A) + \dots + \frac{N!}{(N-M)! M!} A^{N-M} (1-A)^M],$$

where

FD ≡ annual probability of failure in system,
PDD ≡ annual expected forced outage days,
MTTR ≡ time to repair from failed state,
K ≡ annual scheduled outage days,
A_p ≡ pump system availability.

Finally, in the case of supplemental heating (specifically, the gyrotrons), on-line redundancy has been provided to enable changeout without interruption of reactor operation. Because the beam filaments may have short lifetimes and a large number of filaments need to be changed, a semi-continuous changeout is envisioned. For this system, one unit is always considered down for replacement and system failure is predicted from an equivalent availability model, as above, with reduced N. Note that from an operations standpoint, for less than full power operation, the model assumes symmetric operation (i.e., shuts off gyrotrons to maintain balanced injection powers at each end).

24.3 ASSUMPTIONS

In addition to those assumptions inherent in the nature of a steady-state availability assessment, we have made a number of assumptions regarding the conduct of reactor operations and the handling of certain failure modes. These assumptions include:

1. Failure modes that do not directly cause an outage (reactor operation can continue until routine shutdown), are not considered in the assessment.
2. In general, connective piping, electrical cabling and grounding, and external power sources (e.g., electrical grid from which startup power is drawn) are not assumed to cause unavailability.
3. For numerical assessment, superconducting magnets are assumed to have no unavailability during steady-state operation of MARS.
4. Long term deterioration of heat transfer equipment (e.g., heat exchangers) is detectable and correctible at annual shutdown. This results in no unavailability during steady-state operation.
5. We have not accounted for reductions in downtime associated with early warning of impending failure (e.g., bearing acoustical signature) by the instrumentation and control system.

24.4 AVAILABILITY SUMMARY

Table 24-3 summarizes the results of the assessment of the availability for the time that is not devoted to scheduled maintenance. We have not included all of the failure modes of Table 24-1 in the determination of each subsystem's availability. In particular, failure modes that can be tolerated until such time as the annual shutdown have been deleted from the assessment process.

The availability for the system is calculated parametrically from the relationship

$$A = 1 - [U_{SCH} + (1 - U_{SCH}) A_u] \quad ,$$

Table 24-3. Subsystem availability summary.

Code of accounts designation	System/subsystem	Assessed availability	Comments
22.01.01.01	Blanket	0.98916	Steady-state availability computation
22.01.01.02	Reflector	0.98734	Steady-state assessment for the two specified failure modes
22.01.01.03	Service station	0.99809	Considered only water leak failure mode
22.01.01.04	Piping and joints	0.99863	Based only on large He leak
22.01.02.01	Central cell shield	0.99564	Steady-state computation for whole reactor
22.01.02.02	Choke coil shield	0.99636	Steady-state computation for 2 shields
22.01.02.03	Transition coil shield	0.99636	Steady-state computation for 2 shields
22.01.02.04	Anchor coil shield	0.99636	Steady-state computation for 2 shields
22.01.02.05	Plug coil shield	0.99636	Steady-state computation for 2 shields
22.01.02.06	Recircularizer coil shield	0.99636	Steady-state computation for 2 shields
22.01.03	Magnets	All superconducting magnets assessed as having no steady-state unavailability penalty	
22.01.03.02.02	Normal choke coils	0.99454	Steady-state computation for 2 coils
22.01.04.01	Sloshing ion beam	0.98030	Steady-state computation for 2 ends
22.01.04.02	Thermal barrier ECRH	0.9971	Equivalent availability; each end 18 supplied, 16 required for on-line replacement

Table 24-3. (continued)

Code of accounts designation	System/subsystem	Assessed availability	Comments
22.01.04.03	Potential peak ECRH	0.9984	Equivalent availability; each end 3 supplied, 2 required for on-line replacement
22.01.04.04	Anchor ICRH	0.98030	Steady-state computation for 2 ends
22.01.04.05	Startup ICRH	1.0	No steady-state impact
22.01.07.01	Supplemental heating power supplies	0.9985	No calculation; used raw data
22.01.07.02	Magnet power supplies	0.99672	Used only thyristor failure mode; steady-state computation for 6 such supplies
22.01.07.03	Startup ECRH power supply	1.0	No impact on steady-state operation
22.01.08	Drift pump coils and power supplies	0.98912	Steady-state computation for 4 such coils
22.01.09	Direct converter	0.98911	Steady-state computation for 2 ends, each having 4 collector/scrapper elements
22.02.01	Heat transfer (Li-Pb coolant)	0.9946	Reliability model (i.e., no repair during reactor operation) for pumps
22.02.02	Heat transfer (reflector H ₂ O)	0.9986	Reliability model (i.e., no repair during reactor operation) for pumps
22.02.03	Heat transfer (end tank)	0.9986	Reliability model (i.e., no repair during reactor operation) for pumps

Table 24-3. (continued)

Code of accounts designation	System/subsystem	Assessed availability	Comments
22.03.01	Auxiliary cooling (cryogenics)	0.9663	Equivalent availability model for 2 similar systems (He I and He II)
22.05.01	Tritium extraction	0.99986	Based solely on cover seal leak; steady-state computation
22.05.02	Fuel preparation	1.0	Negligible impact due to redundancy
22.05.03	Fuel purification	1.0	Negligible impact due to redundancy
22.05.04	Storage	1.0	Negligible impact due to redundancy
22.05.05	Fuel injection	0.99999	Steady-state computation for fully redundant hardware
2.05.06	Air detritiation	1.0	No direct availability impact
23 & 24	Turbine plant and electric plant equipment	0.946	Based on turbine and generator outage hours (forced and maintenance) for nuclear units, National Electric Reliability Council generating availability data system

where

A ≡ system annual availability,
U_{SCH} ≡ annual maintenance shutdown fraction,
A_u ≡ forced outage fraction.

The data from Table 24-3 are used to obtain the forced outage fraction (1 - availability), A_u, of 0.2008. The U_{SCH} may be as small as the maximum replacement time at scheduled outage--currently 11 days for replacement of the blanket modules in a given service station--if operations can be performed concurrently. We estimate conservatively that an annual 30-day shutdown would be typical, although many of the repairs and maintenance activities in Table 24-2 are accomplished simultaneously, therefore, providing downside protection against infrequent, but possibly lengthy, unanticipated failures in some subsystems (for example, turbine generator equipment, heat exchangers, superheaters, etc.). Table 24-4 shows the effect of variations in scheduled annual shutdown time on plant availability, as described in the previous equation.

Table 24-4. Plant availability vs scheduled maintenance time.

Annual scheduled outage (days)	Plant availability
20	0.73
30	0.73
40	0.71
50	0.69

24.5 CONCLUSIONS

Based solely on the data developed for MARS, the most availability-sensitive systems/components would include:

1. Turbine and generator electrical equipment (~5% unavailability),
2. Normal magnets (~1.5% unavailability),
3. Blanket, reflector, and service station (~2.5% unavailability),
4. Supplemental heating systems (~5% unavailability),
5. Cryogenics (~3.5% unavailability)

For the turbine and generator, the data are hardware-derived, but are not based exactly on the MARS system, and represent a legitimate estimate of generator technology for the next 40 to 50 years in the absence of a significant development effort. We presume that the normal magnets (i.e., drift pump and normal choke coils) will fail from coolant leakage at joints; The failure intensity and repair times are estimates only, and could vary in either direction.

Central-cell reactor-hard core failure is caused by leakage (water or Li-Pb). The failure intensity is an estimate only; the repair estimates are somewhat firmer, based on the the timeliness of "typical" actions (cut, transport, etc.). The failure parameter for the central cell is given in terms of "whole reactor" specification and the numbers may reflect optimism regarding the quality control processes associated with fabrication and annual maintenance.

The cryogenics subsystem availability is determined by making conservative estimates for the time to failure and time for repair; however, significant improvements in availability can be realized by the addition of excess capacity. The MARS system is presumed to be unavailable when the liquid helium II cryogenics system is down for repair. Cryogen reserves are maintained for the helium I and nitrogen systems. Additional storage and reserve capacity could be provided at modest increased cost. It does not appear that this system will effect unavailability.

In the case of supplemental heating, we feel that the lack of substantial component data has led to unfavorable numerics. The existence of reliable gyrotrons and/or ion sources would greatly improve the supplemental heating systems availabilities. Even in the absence of such components, we have made use of the ECRH redundancy provided in MARS by creating an on-line replacement scheme with short replacement time. The ability to accomodate off-nominal supplemental heating levels (as would be expected when failure of a single gyrotron occurs), the instrumentation and control requirements, and operating philosophy for the reactor (at off-nominal power levels) needs to be further developed.

Finally, although not considered as an availability factor in this analysis, we assume that the superconducting magnets represent a potential downside risk because we have insufficient lifetime data to definitively support the assumption of "no steady-state failure incidence".

In summary, the availability goal for MARS is 80% and all systems are designed to accomodate this high availability. The results of our availability analysis indicate a system availability of 73% based on a 30-day annual maintenance period. Our analysis is based on estimates by system/subsystem designers. Because MARS is an advanced system, we could not rely on test data, therefore, the derived values are quite uncertain. However, they are our best estimates. We made no attempt to normalize the results to meet the goal of 80% availability.

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25 *Plant Construction*

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25. Plant Construction

In this section, we address the siting considerations and the planned construction sequence for the MARS facility including the reactor. The overall construction time is 69 months, which includes 24 months of subsystem and startup testing before commercial operation. The schedule's critical path is through the construction and assembly of the reactor building and the reactor. Thirty-three months of initial construction work in the reactor building is required before starting the assembly of the reactor. The reactor is scheduled to take 24 months for complete assembly. The basis for the overall schedule is that the plant is tenth-of-a-kind, and, therefore, includes standardized building arrangements and subsystem components so that only site-specific modifications (i.e., grading, access roads for construction, cooling water, and transmission-line routing etc.) are changed. The construction schedule includes a time credit for use of improved construction techniques.

25.1 SITE

We have made the following assumptions in developing the site considerations for MARS. The site chosen for MARS is Middletown, USA, and, for the purpose of labor statistics, it is located outside of Kansas City, Missouri. The site is close to a major inland waterway, major access roads, and existing rail lines. The site ranges from level to gently rolling topography and has water available for the makeup system. Finally, it is within Seismic Zone 2 as defined by the Uniform Building Code.

The initial site work begins with the installation of the construction access road, clearing and grubbing of the power block area, and the beginning of on-site railroad construction.

Excavation of the power block area (reactor and steam-generator building, turbine-generator buildings, and support buildings) will begin several months into the initial site work.

25.2 CONSTRUCTION CONSIDERATIONS

To keep capital investments as low as possible during the power block construction, the other site facilities will be constructed as they are needed after power block construction. To meet required construction codes and standards, the schedule is predicated on the availability of certified craftsmen.

The outlying buildings and cooling system structure (i.e., pumphouse and towers) are scheduled to allow for optimum usage of construction equipment and manpower, in addition to adequate access.

Subsystem testing will be initiated in the fourth year, after completion of early systems in the support facilities. Reactor testing and startup will be initiated in the fifth year after the reactor and turbine assembly are completed. Startup data will be available to finish the operation procedures.

Underground major construction, such as circulating water piping in heavily traveled areas, is completed during early portion of the schedule to avoid any schedule delays. Significant potential impacts on the construction schedule are the large quantities of bulk material and the number of large

modules and major equipment items that are needed. For these reasons, the early area approach is applied to turnover areas as well as allowing early installation of bulk material and setting of large modules and major equipment.

25.2.1 Handling and Shipping

Ideally, the plant would be located next to an inland waterway. If this is impossible, the most feasible method for transporting heavy items and/or modules would be off-loading from barges and moving these items via highway or rail to the facility. If an inland waterway is near the site, dredging may be required along the waterway and at the barging slip or dock. Transporting by highway will require review of the highway route to ensure unimpeded movement of large items, possibly involving:

- Constructing highway on- and off-ramps;
- Adding or strengthening bridges and culverts;
- Providing temporary roads;
- Temporary raising or lowering of obstructing overhead wires.

Hauling and lifting requirements for MARS are well within current state-of-the-art capabilities.

25.3 CONSTRUCTION SCHEDULE BASIS AND ASSUMPTIONS

We made the following assumptions in establishing the schedule durations and sequences:

1. Schedule durations are based on optimistic times that are derived from current and past experience in construction and procurement lead times that are representative of large nuclear power plant projects. The schedule also assumes multishift operations, maximum level of effort, and total commitment to the project. No schedule contingencies are included.
2. The schedule starting point indicates that facility construction starts when: a construction permit is approved; the reactor engineering and development interfaces are completed in sufficient detail to allow the plant construction to start; the site has been preselected and acquired; and the basic site data are available. Environmental studies and licensing activities are not included, are not shown as part of the schedule, and, therefore, do not constrain construction.
3. We have used the critical-path-method milestone-schedule format, giving major construction durations for each major building. The system and major equipment are identified for each building in the milestone construction schedule Fig. 25-1. The time scale is in years by months, with major blocks of work sequenced and identified. Startup and testing activities are also identified.
4. A magnet-fabrication building is included both in the cost-baseline and fabrication-shop schedule. This allows the critical-path items (the magnets) to be fabricated on-site and reduces any possibility of future schedule and cost impacts.

Fig. 25-1. The MARS facility milestone construction schedule.

5. The site railroad spur is complete and the reactor building crane operational for assisting in the assembly of the reactor before start-of-construction.
6. The construction durations for bulk material installation and for setting major equipment are determined by using the early area concept (i.e., when the slab framework is in place above, the area is available for installation of material and equipment) as the basis for the overall schedule.

25.3.1 Reactor

The installation of the MARS reactor is fully integrated with the overall construction schedule for the balance-of-plant and other facilities. The reactor is the critical-path item in the overall schedule for the construction and operation of MARS. Any delays associated with the assembly and installation of the reactor may have a significant impact on plant costs because reactor assembly occurs late in the overall plant installation schedule. To minimize field construction and installation time, major components of MARS are preassembled either on site or in vendor shops for installation in the reactor building. This approach allows the use of reactor-maintenance equipment, which is already designed and installed, as part of the installation and assembly sequence, thereby speeding up construction time and providing for the testing of systems that are envisioned as needing scheduled and nonscheduled maintenance.

The installation approach and schedule were developed, based on the following assumptions:

1. The MARS reactor is the tenth of a standardized plant design to be constructed, and the majority of the reactor-construction crew have gained experience on previous plant construction. Schedule durations assume multishift operation without labor strikes.
2. All major components have been preassembled, insofar as possible, either on site or at vendor shops.
3. Reactor installation begins once the construction of the reactor building progresses to the point where the roof is installed.

The detail reactor schedule, shown as Fig. 25-2, indicates separate and parallel assembly sequences for the central cell and the end cells. This approach is consistent with the linear nature of MARS and the layout of the reactor building. The schedule portrays the manufacturing and production schedule for each of the major components, as well as the installation and assembly sequence. The manufacture and production durations are based on manufacturing studies of the components and on recent experience in fabricating components of similar dimensions and types for such devices as the Mirror Fusion Test Facility (MFTF-B).

We assume that for the central cell, an on-site magnet-fabrication building is provided, and, that the 42 central cell magnets, plus two spares, are fabricated on site. The production schedule for these magnets runs concurrent with that for the reflector and shield, and for the basic sector support structures. The reflector and shield, as well as the sector support structures, are shipped to the site as they come off the vendor's production line. At the site, they are assembled and the magnets installed, thus

Fig. 25-2. The MARS facility detail reactor schedule.

completing the central cell segment. These segments are then stored in a staging area. Once the reactor building roof is set in place, about 33 months after the start of construction, and sector modules are rolled into place, using the embedded floor rails and transhaulers envisioned for use in maintenance in the unlikely event of a reflector or central cell magnet failure. As the reactor segments are installed, the main power piping from the steam-generator building, both for Li-Pb and water is brought to each segment. The reflector and shield cooling water, can be completely installed at this time, whereas the Li-Pb piping completion must await the installation of the blanket modules.

Blanket module production is time phased to coincide with the availability of the central cell segments for receipt of the blanket modules. The central cell segments are completely installed approximately 43 months after the start-of-construction. At that time, cryogenic piping to the central cell coils, and connection of the coil power supply and protective circuits begins. The installation of the blanket modules begins approximately 48 months into the construction period. As the blanket modules are installed, the Li-Pb piping and the remote joint may be completed. Installation of the central cell modules will use the equipment and procedures envisioned for blanket module replacement, thus providing a good test of the equipment and training for personnel.

For the end cells, the long-lead items are the manufacture of the magnet support structures (strongbacks) and fabrication of the large vacuum tanks containing the end cell magnets. Like the central coil, the largest, practical subassembly will be made on site. This includes installing the large end cell magnets within their supports. Once the first large vacuum tank has been installed, the magnet subassemblies can be rolled into place using the embedded floor rails and transhaulers provided. After completion of the first end cell, the direct converter for that end of MARS may be installed, followed by the supplemental heating.

The schedule for the second end cell overlaps with, while still following in sequence with the first end cell; thus, the crew installing one end cell tank will move to the other end of the machine and install the second tank. The magnet installation crews, the direct-converter installation crews, and the supplemental-heating system installation crew follow the same logic. Once the equipment at one end of the machine has been installed, system hookup and testing may begin.

Reactor installation will be completed in the 56th month after start-of-construction at the site. Subsystem testing of balance-of-plant systems and major reactor-support systems, including the tritium processing, proceeds and is completed by the time the reactor is installed. Thus, once the reactor has been installed, reactor and main-heat-transport system calibration, plus integrated reactor testing and startup, begins. The overall milestone construction schedule provides for one year of integrated testing and startup, with commercial operation commencing in the 70th month after the start-of-construction. Engineering startup is discussed in detail in Sec. 23 of this volume.

25.3.2 Balance-of-Plant

The milestone construction schedule for MARS, Fig. 25-1, has been developed based on the balance-of-plant drawings and descriptions given in Sec. 22 of this volume; the capital cost estimate, including labor and

material, presented in Sec. 26 of this volume; and the detailed reactor installation sequence, discussed above in Sec. 25.3.1. As noted above, the critical path is through the construction of the reactor building and the installation of the reactor.

An area approach is used to develop the milestone construction schedule. Excavation for the reactor steam generator buildings and the turbine-generator building takes place in parallel. Once the reactor steam generator buildings are brought to grade elevation, excavation for the hot-cell building foundations begins. Excavation and foundations for the tritium building follow those for the hot-cell building. The erection, via slip forming, of the reactor steam-generator building walls are followed by the same construction for the hot-cell building and the tritium building. This approach ensures the earliest availability of the reactor building and minimizes interference with crews working in other areas of the balance-of-plant.

Construction and installation of all balance-of-plant facilities will be completed approximately 50 months after the start-of-construction. The installation of major electrical equipment is completed some 45 months after the start-of-construction, allowing for testing of electric power and essential control systems before the completion of the balance-of-plant facilities. Once the balance-of-plant facilities have been completed, integrated subsystem testing may begin. This approach provides for maximum readiness of the balance-of-plant and some of the reactor support systems before the completion of reactor construction at the end of the 57th month. Thus, once reactor installation is completed, integrated system testing may begin. As noted above, 12 months are provided for integrated system testing and startup before commercial operation at the end of the 69th month.

26 *Economic Analysis*

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26. Economic Analysis

26.1 INTRODUCTION

As an advanced reactor study, MARS represents a benchmark in the development of system goals for tandem mirror fusion. The cost estimate and economic analysis contribute to the system goals by identifying the largest cost items in the system, by allowing an assessment of the economies of scale, and by providing guidance for research and development to achieve the most economic and reliable design. This cost estimate, therefore, is specific to the system designs defined in this report.

26.2 GUIDELINES

To be consistent with other fusion reactor design studies, we based our code-of-account and cost-estimating techniques on the DOE guidelines in Ref. 1 for "fusion reactor design studies--standard accounts for cost estimates" and in Ref. 2 for "fusion reactor design studies--standard unit cost and cost scaling rules." These guidelines, particularly the code of accounts, were modified to meet the design parameters of MARS. The code of accounts was also expanded, especially in the reactor area, to provide the finest level of detail consistent with the level of technology and state of design in the MARS program. This level of detail ensured that all key cost components were identified and independently costed, and avoided, when possible, lump-sum costing on a dollars-per-watt basis.

The balance-of-plant facilities (including the buildings, turbine plant, electric plant, and, to a great extent, the heat transport and energy conversion system--part of account 22, reactor systems) are within the current state-of-the-art technology. Many other components of the reactor system, although an extension of current technology, are also well within the state of the art. This is particularly true for the magnet systems, account 22.01.03, which are based on recent manufacturing experience with magnets for Mirror Fusion Test Facility Upgrade (MFTF-B), LLNL's large coil program; and Elmo Bumpy Torus (EBTT-P) at Oak Ridge National Laboratory.

Table 26-1 presents a summary of the MARS capital costs by major code of account. All costs are presented in 1983 dollars and we assume, for material estimates, that a mature market exists and that sufficient quantities of the raw material are available to support that market.

26.2.1 First- and Tenth-of-a-Kind Costing

In our preparation of the MARS cost estimate, each team member was responsible for developing bottoms-up cost data for a particular area of technology and/or plant design. Unit costs were usually based on recent experience or relied on published figures. Estimates were initially prepared for a first-of-a-kind mirror-fusion power plant and included an allowance for engineering uncertainty (to be discussed in Sec. 26.2.3). In some cases, we used published estimates for the material involved as the basis for our costs.

Because MARS represents a tenth-of-a-kind mature-design mirror-fusion power plant, we assumed that the design technology would be thoroughly proven. Since the initial estimate was prepared on a first-of-a-kind basis,

Table 26-1. MARS capital costs.

Account number	Account title	Cost (1983 K\$)	
20	Land and land rights	5,000	
20.01	Acquisition		5,000
20.02	Relocation of existing facilities		--
21	Structures and site facilities	249,850	
21.01	Site improvements and facilities		10,650
21.02	Reactor building		86,900
21.02.01	Basic building structure		26,900
21.02.02	Building services		28,000
21.02.03	Containment structure (tritium barrier)		32,000
21.03	Turbine building	35,980	
21.04	Cooling system structures	5,500	
21.05	Power supply and energy storage building	5,150	
21.06	Miscellaneous buildings	68,410	
21.06.01	Tritium process building		32,510
21.06.02	Hot cell		27,200
21.06.03	Control room		2,290
21.06.04	Magnet fabrication		480
21.06.05	Water treatment		1,050
21.06.06	Administration		510
21.06.07	Warehouse		630
21.06.08	Maintenance		550
21.06.09	Auxiliary boiler		280
21.06.10	Cryogenic		1,630
21.06.11	Other		1,280
21.07	Ventilation stack	2,900	
21.98	Spare parts	2,010	
21.99	Contingency	32,350	
22	Reactor plant equipment	1,517,300	
22.01	Subtotal, 22.01 level		892,920
22.01.01	Blanket and first wall		71,190
22.01.01.01	Blanket structure		11,200
22.01.01.02	Reflector		54,280
22.01.01.03	Service station		2,890
22.1.01.04	Piping and remote joint		2,820
22.01.02	Shield		74,970
22.01.02.01	Central cell		51,940
22.01.02.02	Choke coil		1,900
22.01.02.03	Transition coil		5,810
22.01.02.04	Anchor coil No. 1		6,200
22.01.02.05	Plug coil No. 1		7,580
22.01.02.06	Recirc coil No. 1		1,540
22.01.03	Magnets		493,020

Table 26-1. (Continued).

Account number	Account title	Cost (1983 K\$)
22.01.03.01	Central cell	196,850
22.01.03.02	S/C choke	60,410
22.01.03.03	Normal choke	2,480
22.01.03.04	Transition	43,060
22.01.03.05	Anchor	74,410
22.01.03.06	Plug	83,510
22.01.03.07	Recircularizer No. 1	30,460
22.01.03.08	Recircularizer No. 2	1,840
22.01.04	Supplemental heating	101,300
22.01.04.01	Sloshing ion beam	27,780
22.01.04.02	Thermal barrier ECRH	39,660
22.01.04.03	Potential peak ECRH	6,340
22.01.04.04	Anchor ICRH	13,660
22.01.04.05	Startup ICRH	13,860
22.01.05	Primary structure and support	54,550
22.01.05.01	CC inter-magnet support	120
22.01.05.02	Module support structures	17,350
22.01.05.03	End cell magnet supports	17,250
22.01.05.04	End cell structure/vacuum tank	12,840
22.01.05.05	Supplemental heating support(s)	890
22.01.05.06	Vacuum vessel	6,100
22.01.06	Reactor vacuum systems	6,970
22.01.06.01.01	Plasma chamber and direct converter	1,020
22.01.06.01.02	CC/end cell isolation valves	150
22.01.06.02	Magnet dewar	2,090
22.01.06.03	Supplemental heating	3,710
22.01.07	Power supply switching and energy storage	63,420
22.01.07.01	Supplemental heating	47,900
22.01.07.02	Magnets	15,520
22.01.08	Drift pump coils	4,710
22.01.09	Direct converter	22,790
22.01.09.01	Collector plates	3,380
22.01.09.02	Halo scraper	4,060
22.01.09.03	dc to ac inverter	15,350
22.02	Main heat transfer	236,580
22.02.01	Li-Pb coolant	156,130
22.02.01.01	Pumps	17,260
22.02.01.02	Piping	80,960
22.02.01.03	Heat exchangers	51,200
22.02.01.04	Tanks	2,300
22.02.01.05	Insulation	4,410
22.02.02	Reflector and direct converter H ₂ O	80,450
22.02.02.01	Pumps	11,550
22.02.02.02	Piping	39,970
22.02.02.03	Heat exchangers	21,920

Table 26-1. (Continued).

Account number	Account title	Cost (1983 K\$)
22.02.02.04	Other equipment	4,250
22.02.02.05	Insulation	2,760
22.03	Auxiliary cooling system	50,160
22.03.01	Cryogenic cooling	33,050
22.03.01.01	LHe I refrigerator	5,720
22.03.01.02	LHe II refrigerator	10,830
22.03.01.03	LN refrigerator	8,070
22.03.01.04	Cryogenic piping	8,430
22.03.02	Water cooling	13,720
22.03.02.01	Pumps	1,150
22.03.02.02	Piping	4,610
22.03.02.03	Heat rejection	3,030
22.03.02.04	Heat exchangers	4,930
22.03.03	Helium cooling	3,390
	heating for seal valves	
22.04	Radioactive waste treatment	11,480
22.04.01	Li-Pb (cold trap)	1,890
22.04.02	Primary H ₂ O coolant	840
22.04.03	Gaseous tritium	3,380
22.04.04	Solid	4,050
22.04.05	Blanket bakeout oven	560
22.04.06	Blanket compacting equipment	760
22.05	Fuel handling and storage	45,880
22.05.01	Tritium extraction	4,820
22.05.02	Fuel preparation	1,940
22.05.03	Fuel purification	6,510
22.05.04	Fuel storage	3,300
22.05.05	Fuel injection	2,360
22.05.06	Air detritiation	21,310
22.05.07	End cell water tritium clean-up	5,640
22.06	Other reactor plant equipment	30,480
22.06.01	Maintenance equipment	25,810
22.06.01.01	Blanket and coil maintenance equipment	2,710
22.06.01.02	Other maintenance equipment	14,570
22.06.01.03	Transporting lifting and unloading equipment	8,530
22.06.02	Special heating	1,200
22.06.02.01	Trace heating	760
22.06.02.02	Li-Pb tank heating	500
22.06.03	Coolant	3,370
22.06.03.01	Li-Pb dump storage tank	2,760
22.06.03.02	Li-Pb makeup	610

Table 26-1. (Continued).

Account number	Account title	Cost (1983 K\$)	
22.06.04	Gas system		100
22.07	Instrumentation & control	24,800	
22.07.01	Reactor I&C		6,480
22.07.02	Radiation monitoring		2,460
22.07.03	Isolated inst & transducer		11,980
22.07.04	Central control computer		3,180
22.07.05	Tritium analytical lab instruments		700
22.08	Spare parts allowance	32,000	
22.09	Contingency	193,000	
23	Turbine generator plant	274,980	
23.01	Turbine generator	97,900	
23.02	Main steam	11,410	
23.03	Heat rejection	32,060	
23.04	Condenser system	11,950	
23.05	Feedwater heating	13,100	
23.06	Other turbine plant	56,430	
23.07	I&C	12,870	
23.98	Spare parts	3,900	
23.99	Contingency	35,360	
24	Electric plant equipment	160,000	
24.01	Switchgear	16,250	
24.02	Station service equipment	24,200	
24.03	Switchboards	6,300	
24.04	Protective equipment	3,200	
24.05	Electrical structure & control	24,600	
24.06	Power & control wiring	58,500	
24.07	Electrical lighting	4,590	
24.98	Spare parts	1,400	
24.99	Contingency	20,960	
25	Miscellaneous plant equipment	33,160	
25.01	Transport & lifting equipment	12,790	
25.02	Air & service water	8,570	
25.03	Communications system	5,060	
25.04	Furnishing and fixtures	2,020	
25.98	Spare parts	460	
25.99	Contingency	4,260	

Table 26-1. (Continued).

Account number	Account title	Cost (1983 K\$)
26	Special materials	124,900
26.01	Reactor coolant Li-Pb	124,200
26.04	Other materials	700
	<u>Total direct cost</u>	<u>2,365,190</u>
91	Construction facilities equipment & services	240,000
91.01	Temporary facilities	19,000
91.02	Construction equipment	119,000
91.03	Construction services	102,000
92	Engineering & construction services	189,000
93	Other	118,000
	<u>Total capital cost</u>	<u>2,912,190</u>

we applied learning curves to the original estimate to arrive at the estimated cost in 1983 dollars for the tenth-of-a-kind mature reactor design. The learning curves were applied individually to each subaccount, based on the MARS team's assessment of the level of technology and/or technological risk involved, plus the cost savings to be made in a mature market. Therefore, these learning curves include projected gains in production efficiency, labor reductions, and technological improvements to the design presented here.

Table 26-2 presents the study's predicted learning-curve cost reductions by major code of account. As we expected, after applying these learning curves, the largest cost reductions occur in account 22 (reactor systems) because this is the area with the greatest design uncertainty and the least current technology or experience with production. The fairly large percentage of reduction in account 26 (special materials) is from our use of highly enriched Li-Pb as the primary reactor coolant. We project that significant improvements can be made in lithium enrichment technology to provide the required high volume production and attendant reductions in cost. Cost reductions in the other accounts reflect design improvements that will be developed as experience is gained during the fabrication, installation, and operation of the initial MARS-type reactors.

26.2.2 Site

We chose the MARS site to match the recommendation contained in Ref. 1 and made the following assumptions: The site is near "Middletown," USA, and is 1,000 acres costing \$5,000 per acre. It is also level to gently rolling topography and is near existing rail lines and major highways. In addition, sufficient cooling water is available at the site for makeup. For the purpose of building design, the site is in UBC Seismic Zone 2. "Middletown," USA is placed near Kansas City, Missouri for labor and material costs purposes. A composite labor rate of \$25 per hour, including fringe benefits and other allowances, is used to determine installation costs for MARS.

26.2.3 Design Allowance, Contingency, and Spares

In arriving at the first-of-a-kind cost for each MARS system, a design allowance was applied to the material costs to account for the immaturity of the design and design uncertainties. These design allowances were applied by subaccount number to each item within the reactor system's account 22. Rather than use a blanket fixed allowance, these allowances were assigned by individual account numbers. In developing the learning curves applied above, this design allowance was, in effect, depreciated over the first 10 reactors.

During the normal course of preparing cost estimates for large capital projects, a contingency is generally applied. This contingency specifically does not cover an allowance for design uncertainty, but rather provides an allowance for unforeseen events, such as changes in labor productivity, uncertainties in estimated quantities, and the effects of strikes, wars, or "acts of God." We followed the guidelines from Ref. 2 in that we allowed a 15% contingency for all capital cost accounts. Contingencies were applied to both direct material and the installation labor costs.

Table 26-2. Learning curve cost reduction.

Account	Title	First (M\$)	Tenth (M\$)	Reduction (%)
20	Land	5.0	5.0	0
21	Structures	264.8	249.8	5.7
22	Reactor	1,879.4	1,517.3	19.3
23	Turbine	299.7	275.0	8.2
24	Electrical	180.0	160.0	11.1
25	Other	36.1	33.2	8.0
26	Special materials	177.3	124.9	29.6
	Total	2,842.3	2,365.2	16.8

To allow for unscheduled breakdowns of parts and indoor components, a spare parts inventory is maintained as part of the MARS capital costs. These parts would be stored on-site. For all major codes of account, with the exception of account 22 (reactor plant), we applied a spare parts allowance of 2% of direct material cost. In the case of the reactor plant, we made a system-by-system review and estimated the number of spares required. Chief among the capital cost items, included as spares for the reactor plant, are two central cell magnets, which would allow the replacement of one complete sector in the central cell in the unlikely event of magnet failure. In effect, one complete central cell sector is kept in reserve as a spare, including its seven blanket modules, reflector, and shield.

For some of the supplemental heating systems, on-line installed spares are included as part of the equipment installed cost. This is true for the gyrotrons and launchers in the ECRH systems and sources in the neutral beamlines.

26.2.4 Indirect Costs

Indirect costs are those costs associated with support functions for the design manufacturer, construction facilities and equipment, and other costs that are principally owner costs connected with the development of a major capital project. Indirect costs do not include costs associated with the actual production of equipment and materials for MARS and with the manual construction labor required to install those pieces of equipment. Specifically, indirect costs do include construction facilities, equipment, and services (account 91); engineering and construction management services (account 92), and other costs that are principally owner costs (account 93).

Construction facilities, equipment, and services (account 91) include nonmanual construction personnel, temporary construction facilities (e.g., offices, warehouses, temporary fabrication shops, sanitary facilities and the like, temporary power in communication systems, as well as heavy and light construction equipment, their maintenance costs, fuel expenses, etc.). For

MARS, we made a detailed and comprehensive estimate of indirect construction facilities, equipment, and services. The cost analysis was based on the construction schedule envisioned for MARS, as well as the overall capital costs. The result of this analysis was an estimate of \$240 million for account 91. All cost figures were based on mid-1983 price levels and on our recent experience with light-water reactor construction. The \$240 million is only slightly more than 10% of the direct construction cost for the facility and is, therefore, consistent with those numbers used for other reactor studies.

Engineering and construction management services consist of those expenses for the design engineering of the reactor and balance-of-plant facilities. Because MARS is the tenth of a standardized design, much of the cost associated with engineering and construction management services stems from site-specific or owner-specific details. No allowance is made in the engineering cost estimate for new regulatory guides nor design improvements, except where those design improvements are accounted for via the learning curves in various plant systems. The estimate for the construction management portion of those services is based on our recent experience with projects of a similar type and the total number of man-hours estimated for the construction of MARS. The total estimate for engineering and construction management services is approximately \$189 million--8% of the total direct cost.

Other indirect costs include insurance, taxes, owner's general and administrative costs, plus staff training. Following Ref. 1 recommendations, these costs are assumed to be equal to 5% of the total direct capital cost for MARS.

26.2.5 Time-Related Costs

The total specific construction cost for MARS is based on constant 1983 dollars. This total specific construction cost includes the direct capital cost plus the indirect costs of construction services, facilities and equipment, engineering construction management services, and other costs. To reflect an actual construction period, any cost estimate must include the effects of escalation and interest costs during construction, sometimes known as the allowance for funds used during construction. To arrive at the time-related cost for MARS, a cash-flow cash-requirement summary was developed based on the reactor and balance-of-plant construction schedules presented in Sec. 25 of this volume, as well as on the direct and indirect costs presented here. In developing the cash requirement estimate, material and installation labor are spread separately and then sum-totalled by quarters for each year of the construction. Table 26-3 presents the percentage expenditure in each of the six years of construction by material and installation as separate items.

Financing costs were based on an investor-owned utility, with 55% debt financing and 45% equity financing. The nominal cost of capital was determined to be 10% per year and, for the purposes of MARS, escalation was assumed to be either 5 or 8% per year.

We used two modes of analysis in accordance with Ref. 1. The first is a "constant-dollar mode," which assumes the purchasing power of the dollar remains constant over time. This mode expresses all costs in 1983 dollars. We used two sets of assumptions for the constant-dollar mode because of the guidelines given in Ref. 1. The 10% cost of capital, used to determine interest during construction (IDC), could effectively be cut in half--5% per year in case 1. For case 2, we used 10% in IDC. The second analysis is the

Table 26-3. Percentage expenditure during construction.

Year	Material	Installation
1984	2.7	6.2
1985	14.7	10.8
1986	32.8	23.4
1987	29.7	29.6
1988	16.8	23.6
1989	3.3	6.4
Total	100%	100%

"then-current-dollar mode," which assumes that inflation takes place during the construction period and that the utility can still borrow money for its capital outlays during the construction period. The inflation or escalation rate is 5 and 8% in cases 3 and 4, respectively. For case 3, IDC is 1%, which is 5% above general inflation in accordance with the Ref. 1 guideline. Case 4 shows the effect of a higher escalation rate. Table 26-4 gives the total capital investment in the plant for these four cases.

The utility accounting system, used to arrive at the total installed costs of plant in the "then-current-year analysis," with escalation at an assumed 5 or 8% per year, must be applied to the cash requirement summary on a year-by-year basis. The sums of the cash requirement from the baseline estimate plus the escalation are subtotaled before applying the cost of money to determine IDC because the operating utility must satisfy the interest on the escalated costs of the equipment and services.

Figures 26-1 and 26-2 graphically represent the estimated cash requirements for MARS based on the above statements. Figure 26-1 expresses the result in constant 1983 dollars, showing the total specific construction cost and the IDC. Figure 26-2 shows the results of this analysis in the "then-current-dollar mode" of evaluation.

Many of the assumptions made in this study were taken from Ref. 1 in an effort to remain consistent with previously published fusion reactor studies, therefore, great care should be taken in comparing these results with those of other studies, in particular with studies of existing energy sources and/or other alternative future energy sources.

26.3 CAPITAL COSTS BY ACCOUNT

In Table 26-5 we present the MARS capital cost estimate by code of account. All costs are in 1983 dollars and include both materials and installation. The table presents the account and/or subaccount number, the account title, and the capital costs for each item.

Table 26-4. Time related costs.

Cost element	Case 1	Case 2	Case 3	Case 4
	(IDC = 5%) (EDC = 0%) (M\$)	(IDC = 10%) (EDC = 0%) (M\$)	(IDC = 10%) (EDC = 5%) (M\$)	(IDC = 10%) (EDC = 8%) (M\$)
Total capital cost	2912.1	2912.1	2912.2	2912.2
IDC ^a	354.1	708.3	863.2	897.8
EDC ^b	0	0	521.7	938.9
Total capital investment	3266.3	3620.5	4297.1	4748.9
\$/w _e	2722	3017	3581	3957
	Constant 1983 dollars		Then-current 1989 dollars	

^aIDC = Interest during construction.

^bEDC = Escalation during construction.

Fig. 26-1. Constant-dollar mode.

Fig. 26-2. Then-current-dollar mode.

Table 26-5. (Continued.)

Account number	Account title
21.05	Power supply and energy storage building: Basic building structures including earthwork, foundation, structural, and gallery steel; Building services including plumbing, HVAC, and fire protection
21.06	Miscellaneous buildings: Basic building structures including earthwork, foundations, footings, structural steel, rebar, concrete, hot cell pool, architectural, paint, embeds, and forms as applicable to each building; Building services including plumbing, HVAC, lighting, heat tracing, and fire protection as applicable
21.07	Ventilation stack: Basic building structure including foundation and concrete
21.98	Spare parts--2% of material
21.99	Contingency
22	Reactor plant equipment
22.01.01	Blanket and first wall including blanket structure, reflector, service station, piping, and remote joint
22.01.02	Shield including central cell, choke coil, transition coil, anchor coil No. 1, plug coil No. 1, and recirc. coil No. 1
22.01.03	Magnets including central cell, S/C choke, normal choke, transition, anchor, plug, recircs Nos. 1 and 2
22.01.04	Supplemental heating including sloshing ion beam, thermal barrier, ECRH, potential peak ECRH, anchor ICRH, startup ICRH
22.01.05	Primary structure and support including CC intermagnet support, module support structures, end cell magnet supports, end cell structure/vacuum tank, supplemental heating supports and vacuum vessel--foundations, footings, concrete, rebar, and steel

Table 26-5. MARS code-of-accounts and abstract of equipment, structures, and services provided for each account.

Account number	Account title
20	Land and land rights
20.01	Land acquisition cost
20.02	--
21	Structures and site facilities
21.01	Site improvements and facilities: General yard improvements including clearing, yard surfacing, landscaping, drainage, fencing, and gates; Transportation and parking including roads, parking area, and railroad; Fire protection, Raney well system, and sewage facilities;
21.02	Reactor building and steam generator building
21.02.01	Basic building structures including foundations, excavation, concrete including RB pipe channel), footings, structural steel, instrumentation, rebar, forms, steel liner (RB), and architectural;
21.02.02	Building services including HVAC, plumbing, lighting, and painting;
21.02.03	Containment structure (tritium barrier) wall, roof, and slab concrete
21.03	Turbine building: Basic building structure including earthwork, excavation, backfill, foundations, concrete (including pedestals and pedestal mats), structural steel, heat exchanger enclosure liner and concrete, painting and building steel (including HVAC, plumbing and drainage, doors)
21.04	Cooling system structures: Basic building structures including concrete anchors and blocks, cooling tower M/U and blowdown, yard piping, sewers and drains, backfill, earthwork, foundations, concrete CW conduit and pipe slabs, steel, rebar, embeds, forms, architectural

Table 26-5. (Continued.)

Account number	Account title
22.01.06	Reactor vacuum systems including plasma chambers, direct converter, CC/end cell isolation valves, magnet dewar and supplemental heating
22.01.07	Power supply switching and energy storage including supplemental heating and magnets
22.01.08	Drift pump coils
22.02	Main heat transfer
22.02.01	Li-Pb coolant--primary coolant system: pumps and motor drives, Li-Pb piping including shop fabricated pipe fittings (90 deg elbows, tees, reducers, caps) and flanges shop fabrication of spool pieces (shop welds, cuts, bevels, preheating liquid penetrant and testing, reinforced nozzle and preheat nozzle and welds); Li-Pb valves--motor-operated gate and 36-in. check valves; Li-Pb 2-in. and smaller piping; Li-Pb hangers and supports; Testing and inspection; Field welding including field preheat stress relief, X-rays, magnetic particle and bolt-ups; Heat exchangers (steam generators, economizers, reheaters, superheaters); Head tank; Insulation - piping and equipment calcium silicate with aluminum jacket and fittings
22.02.02	Reflector and direct H ₂ O converter (Unit 2): Feed pumps and motor drives (8)--CC reflector and direct converter; Piping (LB + SB) and valves--hangers, supports, QA, testing, inspection and field welds; Heat exchangers (steam generators and preheaters); Ion exchangers, EMF pumps (2), pressurizer, chemical feed and makeup tanks, and pumps (2); Insulation--calcium silicate for piping

Table 26-5. (Continued.)

Account number	Account title
22.03	Auxiliary cooling system
22.03.01	Cryogenic cooling including liquid He-1 and -2 refrigerators, liquid N refrigerator and piping
22.03.02	Auxiliary cooling water pumps [reactor cooling water (4)], reactor auxiliary cooling tower pumps, booster pumps, and motor drives; Piping--main header (cold), gate valves, runouts, field welds, and bolt-ups. Main header (hot), branch connections, gate valves, drains, field welds, and bolt-ups; Heat rejection including mechanical draft cooling tower, basin concrete, 48-in. pipe runs and trenches, intake structure, 24-in. lines, globe and gate valves, and flanges; Heat exchangers including (hot) reactor auxiliary cooling, and (cold) reactor auxiliary cooling
22.03.03	Helium cooling/heating for seal valves
22.04	Radioactive waste treatment
22.04.01	Li-Pb (cold trap),
22.04.02	Primary H ₂ O coolant including Li-Pb radwaste processing, piping and valves, spent resin storage tank, mixing and transfer pumps (4), excess water collector tank, cartridge filter, excess water ion exchanger, water re-use tank, filter backwash water collector tanks
22.04.03	Gaseous tritium
22.04.04	Solid, dewatering solid system
22.05	Fuel handling and storage
22.05.01	Tritium extraction
22.05.02	Fuel preparation
22.05.03	Fuel purification
22.05.04	Fuel storage
22.05.05	Fuel injection
22.05.06	Air detritiation
22.05.07	End cell water tritium clean-up

Table 26-5. (Continued.)

Account number	Account title
22.06	Other reactor plant equipment
22.06.01	Maintenance equipment
22.06.01.01	Blanket and coil maintenance equipment
22.06.01.02	Other maintenance equipment
22.06.01.03	Transporting, lifting and unloading equipment
22.06.02	Special heating
22.06.02.01	Trace heating
22.06.02.02	Li-Pb trace heating
22.06.03	Coolant
22.06.03.01	Li-Pb dump storage tank
22.06.03.02	Li-Pb makeup
22.06.04	Gas system
22.07	I&C
22.07.01	Reactor I&C equipment
22.07.02	Radiation monitoring
22.07.03	Isolated instrument & transducer
22.07.04	Central control computer
22.07.05	Tritium analytical lab instrumentation
22.08	Spare parts
22.09	Contingency
23	Turbine generator plant
23.01	Turbine generators (900 MW superheated, and 350 MW saturated)
23.02	Main steam including piping and insulation for superheated and saturated systems
23.03	Heat rejection, superheated and saturated including circulating water system natural draft cooling tower, CW pumps piping and insulation, intake equipment, intake transmission block, intake and pump structure, mechanical draft tower, tower basin excavation--as applicable
23.04	Condensate system (superheat and saturated) Auxiliary pumps, main condensers, condensate pumps, vacuum (air ejector) system, and deaeration
23.05	Feedwater heating--superheated and saturated Feedwater heaters, Feedwater pumps,
23.06	Boiler feed pumps, and startup pumps
	Other turbine plant including miscellaneous heat exchangers, pumps, tanks, piping, and insulation

Table 26-5. (Continued.)

Account number	Account title
23.07	I&C
23.98	Spare parts
23.99	Contingency
24	Electric plant equipment
24.01	Switchgear
24.02	Station service equipment
24.03	Switchboards
24.04	Protective equipment
24.05	Electrical structures and conduit
24.06	Power and control wiring
24.07	Electrical lighting
24.98	Spare parts
24.99	Contingency
25	Miscellaneous plant equipment
25.01	Transporting and lifting equipment
25.02	Air and service water
25.03	Communications system
25.04	Furnishing and fixtures
25.98	Spare parts
25.99	Contingency
26	Special material
26.01	Reactor coolant Li-Pb
26.04	Other materials
91	Construction facilities equipment and services
91.01	Temporary facilities
91.02	Construction equipment
91.03	Construction services
92	Engineering and construction services
93	Other

26.4 BUSBAR ENERGY COSTS

The busbar energy cost is the unit cost of generating a kilowatt-hour of electricity at the generator busbars. The major element determining this energy cost is the fixed carrying charge on total plant investment plus other fixed annual charges. These fixed charges are expressed as a levelized-fixed-charged rate (FCR) based on financing assumptions, tax structure, insurance, etc. The variable components of the energy cost include operating and maintenance costs, scheduled component replacement cost, and fuel costs. Each of these items will be discussed below.

26.4.1 Annual Levelized Capital Costs

The annual levelized capital cost of the facility includes the following major components:

- Annual carrying charges on investment,
- Depreciation,
- Federal income taxes,
- Ad valorem taxes (state and local),
- Insurance and miscellaneous including interim replacement.

We made several financial assumptions in preparing this analysis:

(1) The facility is an investor owned utility with 55% debt financing at 10% interest and 45% equity financing with a 12% return. (2) Straight-line depreciation is assumed for book purposes, while sum-of-years digits depreciation is used for income tax purposes on a flow-through basis. (3) Depreciation reserve is invested in the new plant to reduce the return requirements without abnormal time delay. (4) The life of the plant is 30 years with zero net salvage value. This assumption implies that salvage value is approximately equal to the cost of decontamination and decommissioning.

Based on the above assumptions we arrive at the following a fixed-charge rate:

	%
Depreciation	3.333
Return	8.08
Income tax	2.0
Ad valorem tax	1.25
Insurance and miscellaneous	0.25
Total	14.913*

26.4.2 Annual Operating and Maintenance Cost

The annual operating and maintenance cost includes eight major categories with the annual cost attributable to each category in 1983 dollars (see Table 26-6).

*For the purpose of this evaluation the levelized-fixed-charge rate was rounded to 15%.

Table 26-6. Major operation and maintenance categories in 1983 dollars.

Operation and maintenance	Cost (K\$)
Annual salaries of facility personnel	9,000
Annual miscellaneous supplies and equipment	4,500
Annual outside support services	1,000
Annual general and administrative costs	2,200
Annual coolant makeup	0
Annual process materials	1,500
Annual fuel handling costs	0
Annual miscellaneous	<u>2,000</u>
Total annual cost	20,200

26.4.2.1 Annual Salaries of Facility Personnel (Account 40). We assume that a staffing level of 150 persons will be sufficient to provide the following: coverage for five shifts during the operation of the MARS plant, capability for the maintenance of scheduled items including blanket-module replacement, and necessary plant security. An average salary of \$60,000 including fringe benefits, overhead, and premiums is calculated based on the high level of technical expertise required for this staff. The total annual salary cost is \$9 million.

26.4.2.2 Annual Miscellaneous Supplies and Equipment (Account 41). This account includes the consumable supplies and equipment used daily during routine operations. An allowance of \$4.5 million is estimated on the assumption that Account 41 costs will be equal to 50% of the total annual staff costs.

26.4.2.3 Annual Outside Support Services (Account 42). Outside support services include all services that are obtained from other than the usual plant staff during normal working hours. These outside services include utility personnel from other locations, in addition to the cost of station personnel working over-time on special tasks such as maintenance and repair. Other requirements for outside support services are film-badge processing, laundering of contaminated clothing, offsite radiation-waste disposal, and consulting services. An allowance of \$1 million is made for these costs.

26.4.2.4 Annual General and Administrative Costs (Account 43). Activities such as payroll administration, purchasing, and division management are included in this account. The cost is estimated at 15% of the total cost accounts 40, 41, and 42 (approximately \$2.2 million).

26.4.2.5 Annual Coolant Makeup (Account 44). We assume for the purpose of this evaluation that there are no costs associated with this account.

26.4.2.6 Annual Process Materials (Account 45). An allowance of \$1.5 million is assumed for this account, which includes large quantities of materials consumed during reactor operations.

26.4.2.7 Annual Fuel Handling Costs (Account 46). Consistent with Ref. 1, we assume that no cost is attributable for this item.

26.4.2.8 Annual Miscellaneous Costs (Account 47). An allowance of \$2 million is made to cover the miscellaneous costs that follow:

- Training and replacement of staff personnel;
- Requalification of licensed reactor operations;
- Rent of property, equipment, or facilities;
- Travel;
- Public relations;
- Office supplies;
- Fuel and upkeep on station vehicles.

26.4.3 Annual Scheduled Component Replacement Cost

A major portion of the annual cost for scheduled maintenance of major reactor components is expected to be in the replacement of blanket modules. For MARS there are a total of 72 blanket modules with a design life of 5 reactor years. Planned replacements, therefore, would be 20%, or 15 modules per year. The design of the central cell, however, provides for access to and replacement of 7 blanket modules from each service station. We have adopted a maintenance policy whereby once a service station is open for the replacement of one module all seven will be replaced. On this basis the number of modules to be replaced annually is 21 for a total annual material cost of \$3,268,000. The equipment and tools required for module replacement are included as part of the capital cost estimate, and it is assumed that the facility personnel will perform the replacement operations. An additional allowance of \$2 million has been included to account for the annual replacement of reactor components other than the blanket modules.

26.4.4 Annual Fuel Costs

The breeding ratio for MARS is sufficient for the reactor to provide all tritium requirements, therefore, the annual cost of tritium is assumed to be zero. An allowance of \$500,000 per year has been included to cover the cost of deuterium, and \$2,300,000 per year for makeup lithium. There are no requirements for off-site processing or disposal facilities.

26.4.5 Plant Capacity and Availability

The net electric generating capacity for MARS is 1200 MW. This capacity is derived from the output of both turbine generators and the direct converters minus the plant auxiliary electric power requirement that includes plasma heating. The plant is assumed to operate at 100% of the design power level of 1200 MWe whenever the plant is available.

As discussed in Sec. 24 of this volume, the plant is expected to have equivalent full power capability 73% of the time based on a 30-day scheduled annual maintenance period. On this basis we have annual generation of

$$1200 \text{ MW} \times 8760 \frac{\text{hrs}}{\text{yr}} \times 0.73 \text{ availability}$$

$$\text{or } 7,673,760 \times 10^3 \text{ kWh}.$$

26.4.6 Cost of Electricity

The cost of electricity (COE) from the MARS power plant is a direct function of the annual fixed charges plus the variable annual component, including operation and maintenance, scheduled replacements, and fuel. The COE (in mills/kWh) is then the total annual cost divided by total annual generation in kWh and may be expressed as

$$\frac{C_{AC} + (C_{O\&M} + C_{SCR} + C_F)(1+E)^P}{PC \times 8760 \times PAF \times 10^3}$$

where

- C_{AC} = annual capital cost charge = total capital cost x fixed charge rate;
- $C_{O\&M}$ = annual operations and maintenance cost;
- C_{SCR} = annual scheduled component replacement cost;
- C_F = annual fuel costs;
- E = escalation rate = 0 for constant dollars and 5 or 8% for then-current dollars (EDC);
- P = construction period;
- PC = plant capacity (MW);
- PAF = plant availability factor.

The basic assumptions are a levelized fixed charge rate of 10 or 15%, a 5 or 8% general inflation rate, and a 5 or 10% cost of money for interest during construction (IDC).

The cost of electricity for the constant-dollar mode and the then-current-dollar mode are summarized in Table 26-7 for the four sets of escalation during construction (EDC) and the IDC assumptions given in Sec. 26.2.5. The term FCR denotes fixed charge rate. The driving component of the COE for either mode is the annual fixed charges, which imply that a reduction in the total investment or an increase in plant availability will yield the greatest improvement in COE.

The general inflation rate also has a significant effect on the COE. Lowering this rate to 5% (case 3), while holding the other assumptions constant, yields a 10% reduction in COE when compared with case 4. The constant-dollar mode (cases 1 and 2) provides a perspective that can be compared with present prices and other fusion studies. Case 3 gives a cost of 7 1/2 cents per kWh. This is within the range of present generation costs although it is near the upper end of this range. Case 1, using the Ref. 1 guidelines, can be used for comparison with other fusion studies. We find that under the same set of economic assumptions and availability goals, and when normalized to the same year, the COE for MARS is about 4% less than that of Starfire.³

Table 26-7. Cost of electricity.

Cost element	Case 1	Case 2	Case 3	Case 4
	(IDC=5%)	(IDC=10%)	(IDC=10%)	(IDC=10%)
	(EDC=0%)	(EDC=0%)	(EDC=5%)	(EDC=8%)
	(FCR=10%)	(FCR=15%)	(FCR=15%)	(FCR=15%)
	(mills/kWh)	(mills/kWh)	(mills/kWh)	(mills/kWh)
Annual fixed charge	42.56	70.77	84.00	92.83
Annual O&M, SCR, fuel	3.68	3.68	4.88	5.73
Total	46.2	74.5	88.9	98.6
	Constant 1983 dollars		Then-current 1989 dollars	

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