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SYNFUELS FROM FUSION -
PRODUCING HYDROGEN WITH THE
TANDEM MIRROR REACTOR AND THERMOCHEMICAL CYCLES

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SCOPING REPORT FY'80

SYNFUELS FROM FUSION— PRODUCING HYDROGEN WITH THE TANDEM MIRROR REACTOR AND THERMOCHEMICAL CYCLES

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SYNFUELS FROM FUSION

A SCOPING REPORT ON PRODUCING HYDROGEN WITH THE TANDEM MIRROR REACTOR AND THERMOCHEMICAL CYCLES

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4.0 THE TANDEM MIRROR FUSION REACTOR

4.1 GENERAL DESCRIPTION OF THE REACTOR

The Tandem Mirror Reactor with Thermal Barriers produces the energy to run the entire synfuel plant and produce hydrogen. The design discussed in this section optimizes the Q of the reactor (defined as the ratio of fusion power produced to input power) for a given power output and physical parameters, therefore minimizing the recirculated power fraction. The reactor design is constrained by the maximum allowable incident neutron power on the first wall and blanket region, and by plasma physics considerations in the three main sections of the reactor. These regions are the central solenoid, the barrier cells and the plugs at each end.

The concept of a simple minimum-B mirror reactor (Fig. 4.1.1a) is not applicable to multimegawatt energy production because of its inherently low reactor Q value. The original tandem mirror concept (Fig. 4.1.1b) combined two minimum-B mirrors as electrostatic end plugs with a straight central cell that produces the reactor power. The fusion power from the central cell relative to the power necessary to sustain the plugs determines the reactor Q value. As originally conceived^(4.1), this configuration had two technical drawbacks. The neutral beams in the plugs required very high injection energy (of the order of 1200 KeV) to maintain high electron temperatures in the plugs, and the magnetic field in the plugs was in the range of 17 T, which is technologically difficult for the complicated minimum-B plug magnets. The addition of thermal barriers^(4.2) (Fig. 4.1.1c) considerably alleviates these problems. The barriers thermally insulate the plug electrons from central cell electrons by separating them with an

electrostatic potential barrier. The technological requirements on plug magnets and neutral beam injectors then become compatible with presently conceived technology. The presence of thermal barriers also introduces new physics constraints on plasma stability and increases the significance of electron-cyclotron heating (ECRH).

As shown in Fig. 4.1.1c, the reactor consists of a long central cell in which a power-producing high-beta (0.4) D-T plasma is confined by straight magnetic field lines using simple, circular NbTi superconducting coil modules. This plasma is electrostatically confined at its ends by the plasma in the Yin-Yang "plugs," each of which is a minimum-B stabilized mirror. The thermal barriers at each end between the central cell and the end plugs are formed primarily by the 12-T Nb₃Sn barrier coils of Fig. 4.1.1c which apply large magnetic mirrors at the ends of the central cell, but inside the end plugs. The central-cell plasma spreads out along the radially expanding magnetic lines as it leaves the magnetic barrier field and its density is thereby reduced in the barrier regions. Since the electrons are in thermal equilibrium the electrostatic potential ϕ and the number density n are connected by the relation $n = n_0 \exp [e(\phi - \phi_0)/k T_e]$. Therefore a dip ϕ_b in electrostatic potential is induced in the barrier region as shown in Fig. 4.1.2 (lower). This dip is a rise in potential energy for electrons and separates the plug and central-cell populations, thereby allowing them to have different temperatures. The ion density of the barrier is kept low by neutral beams directed into the barrier region inside the central-cell loss cone. Trapped barrier ions are neutralized and escape, while the injected neutrals fuel the central-cell as charge-exchange fast ions. Electron-cyclotron heating is applied to the inside of each plug, thereby raising T_e and allowing the plug potential to rise as shown in Fig. 4.1.2 (lower). This provides the potential barrier

(a) Original minimum B reactor concept
(yin-yang coils)

(a) Tandem mirror reactor concept

(b) Tandem mirror reactor with thermal barriers

Fig. 4.1.1 Illustrating the evolution
of the Tandem Mirror Reactor

4.4

for central-cell ions without excessive plug density and without requiring high values of central-cell T_e .

A set of self-consistent reactor parameters is computed in Section 4.4.3 consistent with the plasma physics relationships necessary for tandem-mirror confinement, using the Lawrence Livermore Laboratory TMRTB computer program. Table 4.1.1 gives a set of major parameters which maximize the reactor power gain or Q value for the operating point required for the synfuel plant discussed in Section 7.9 below. The length and reactor power depend critically on the assumed central-cell beta of 0.4 (cf. the discussion in Section 4.2.2.2). As the reactor theory evolves (to external thermal barriers, "A" cells and possibly axisymmetric end plugs) the reactor power and length can be expected to decrease in future designs. The right-hand column of Table 4.1.1 shows reactor parameters in a recent LLL design for an electricity producing reactor.

4.2. Basic Confinement Relations

Figure 4.1.2 illustrates the axial variation of magnetic field and electrostatic plasma potential in a tandem mirror with thermal barriers, corresponding to the magnet arrangement of Fig. 4.1.1c. The vacuum magnetic field B_{op} of the plug is larger than the magnetic field B_{oc} of the central-cell in order to provide magnetic-mirror confinement of the central-cell plasma. The plugs themselves are magnetic mirrors, as indicated by the dips in their magnetic field profiles. Plasma electrons, because of their higher thermal velocity, escape axially faster than the ions, and this tendency is overcome in the central-cell by charging it positively by an amount ϕ_e in order to hold the electrons back. Similarly the plugs charge positively to a potential $\phi_p = \phi_e + \phi_c$. The basic operation of the tandem-mirror makes use of the positive potential ϕ_c of the plugs with respect to the central-cell to

Without thermal barrier

With thermal barrier

Fig. 4.1.2 Axial magnetic field and electrostatic potential profiles in the Tandem Mirror with thermal barriers

4.6

Table 4.1.1 Design parameters for the Tandem Mirror Reactor (cf. Section 4.4.3) as a Driver for the Hydrogen Synfuel Plant (with topping-cycle electric generation) and as an Electricity Producer

	<u>Synfuel</u>	<u>Electricity</u> ^(4.9)
Fusion Power (MMf)	3850	3500
Thermal Power (MWt)	3485	3360
First-Wall Loading (MWn/m ²)	1.5	2.6
ECRH Power (MW)	248	260
Neutral-Beam Power		
Central-Cell (MW)	0	0
Barrier Cell (MW)	73	58
Plugs (M)	7.4	48
Central-Cell Length (M)	213	125
Central-Cell First Wall Radius (m)	1.6	1.7
Central-Cell Beta	0.4	0.5
Reactor Q Value	11.6	9.6

repel escaping central-cell ions and to prevent their escape. The combination of magnetic mirroring and electrostatic effects was first analyzed by Pastukhov^(4.3) to give the $n\tau$ confinement formula [Eq. (4.3.33) below] which applies either to plug confinement of central-cell plasma (potential ϕ_c) or plug-mirror confinement of plug plasma (potential ϕ_p).

4.2.1 Plasma Heating and Power Loss

In the tandem-mirror the central cell is heated primarily by fusion alpha particles. Energy loss occurs primarily by Pastukhov particle end loss, each particle carrying away an energy $e(\phi_c + T_c)$, where T_c is the central-cell temperature applicable to the ions or the electrons. The separate energy balances of central-cell ions and electrons are given in Eqs. (4.3.37) and (4.3.44) below. The central-cell electrons interact with plug electrons exchanging energy, according to Eq. (4.3.46). The plug ions and electrons are energized primarily by neutral beam injection and ECRH. The injected ions build up the plug ion energy E_p [Eq.(4.3.24)] and give energy to the electrons by coulomb collisions (drag). The trapped plug electrons primarily receive the drag energy plus ECRH energy and lose energy by Pastukhov end loss; each electron carries out an energy $(\phi_c + \phi_e + T_{ep})$, where T_{ep} is the plug electron temperature [Eq. (4.3.53)].

4.2.2. Plasma Stability

There are two major classes of instabilities that must be dealt with in tandem-mirrors: microinstabilities which cause excessive end loss over that given by the collisional Pastukhov relation, and macroinstabilities (MHD instabilities) which cause gross fluid movement of the plasma. The latter do not occur in the single-cell classical mirror reactor of Fig. 4.1.1a. However, because of the bad magnetic curvature which necessarily occurs in

connecting the Yin-Yang plugs to the central-cell both MHD interchange and ballooning instabilities can occur, provided the central-cell beta (ratio of plasma pressure to confining magnetic pressure) exceeds a critical value β_c . The value of β_c is lower for ballooning modes (whose amplitude is large in region of bad curvature) than for interchange modes. As indicated below, β_c values of approximately 0.2 are indicated by present theory for the TMR under consideration here. However, the manipulation of TMR magnetic fields to provide confinement has not reached its ultimate degree of development, and in anticipation of this we arbitrarily take $\beta_c = 0.4$ in our scaling studies.

4.2.2.1 Flute interchange instability^(4.4):

This magnetohydrodynamic mode requires for stabilization:

$$\int_{-L}^L dl B^{-1} (p_{\perp} + p_{\parallel}) K_{\psi} > 0 \quad (4.2.1)$$

L is the half length of the entire plasma column, B the magnetic field strength, $p_{\perp}$ and $p_{\parallel}$ the perpendicular and parallel plasma pressures, and K_{ψ} the normal curvature of the field line. The coordinate system used is (ψ, α, l) , where ψ is the axial magnetic flux, α the azimuthal angle measured around the magnetic axis, and l the distance measured along a field line. For calculational purposes:

$$\begin{aligned} \underline{B} &\sim B \underline{\hat{b}} &= \nabla \psi \times \nabla \alpha \\ \underline{K} &\sim \underline{\hat{b}} \cdot \nabla \underline{\hat{b}} &= K_{\psi} \nabla \psi + K_{\alpha} \nabla \alpha, \quad (\text{field line curvature}) \\ K_{\psi} &\sim B^{-1} \underline{K} \cdot (\nabla \alpha \times \underline{\hat{b}}) &= B^{-1} \underline{K} \cdot (\nabla \alpha \times \nabla l), \quad (\text{normal curvature}) \\ K_{\alpha} &\sim B^{-1} \underline{K} \cdot (\underline{\hat{b}} \times \nabla \psi) &= B^{-1} \underline{K} \cdot (\nabla l \times \nabla \psi), \quad (\text{geodesic curvature}) \\ x &\sim [2 \psi / B(l)]^{1/2} \exp [c(l)] \cos \alpha \\ y &\sim [2 \psi / B(l)]^{1/2} \exp [-c(l)] \sin \alpha \\ z &\sim \end{aligned}$$

where the last three equations represent the transformation of coordinates for vacuum flux surfaces with elliptical cross-sections with principal axes equal to $[2\psi/B(\ell)]^{1/2} \exp[\pm c(\ell)]$ (paraxial approximation).

Using the approximations:

omnigenity of the system; i.e., $p_{\perp, \parallel} = p_{\perp, \parallel}(\psi, B)$ only

$p_{\perp, \parallel}$ separable in ψ and B : $p_{\perp, \parallel}(\psi, B) = v(\psi) \bar{p}_{\perp, \parallel}(B)$

$dv/d\chi \leq 0$

large aspect ratio; i.e., longitudinal length scale/radial length scale $\gg 1$,

the curvature term in Eq. (4.2.1) can be written in the form

$$K_{\psi} = x (2\psi)^{-1} \partial_{\ell} (B^{-1} B_x) + y (2\psi)^{-1} \partial_{\ell} (B^{-1} B_y). \quad (4.2.2)$$

To satisfy the condition expressed by Eq. (4.2.1), the regions of good curvature (positive K) must be heavily weighed against the regions of bad curvature (negative K) by having more plasma [higher $(p_{\perp} + p_{\parallel})/B$] in the good curvature regions. This requirement only restricts the ratio of plasma pressures in different regions along the field lines, not their absolute values. Detailed analysis^(4.5) yields the maximum central-cell beta to plug beta ratio, for a given barrier beta to plug beta ratio. A reactor design having $\beta_c/\beta_p = 0.6$ for $\beta_b/\beta_p \approx 0$, is compatible with this requirement.

4.2.2.2 Ballooning instability

Ballooning modes set the limits for high central-cell-beta operations of tandem mirror reactors with thermal barriers. In the large aspect ratio expansion of the Newcomb guiding center energy principle^(4.6) and in

the short perpendicular wavelength limit (which localizes the mode to a single field line), an eigenvalue equation for these modes can be obtained:

$$0 = \partial_{\ell} [B^{-1} |\nabla S|^2 B^{-2} P \partial_{\ell}] \psi + [B^{-2} \rho \omega^2 B^{-1} |\nabla S|^2 + \partial_{\psi} (p_{\perp} + p_{\parallel}) B^{-1} S_{\alpha} (S_{\alpha} K_{\psi} - S_{\psi} K_{\alpha})], \quad (4.2.3)$$

where

$$\nabla S = S_{\psi} \nabla \psi + S_{\alpha} \nabla_{\alpha} = \text{wave vector}$$

$$P = \mu_0^{-1} B^2 + p_{\perp} - p_{\parallel}$$

$$\rho = \text{mass density}$$

$$\omega = \text{eigenfrequency}$$

Here v is the eigenfunction, $v(\ell; \psi, \alpha, \gamma)$ and v is related to the perturbed electrostatic potential ϕ by $\nabla_{\perp} \phi = B \nabla_{\perp} (v/B)$; $\gamma = \arctan (2\psi S_{\psi}/S_{\alpha})$ and the boundary condition on ϕ is $dv/d\ell|_{\ell = \pm L} = 0$.

Normalizing the eigenfunction:

$$\int_{-L}^L (d\ell/B) (\rho/l^2) |\nabla S|^2 \psi^2 = 1, \quad (4.2.4)$$

and solving for the mode growth frequency ω , it is found that

$$\omega^2 = \int_{-L}^L (d\ell/B) [(|\nabla S|^2/B) (P/B^2) (d\psi/d\ell)^2 - \partial_{\psi} (p_{\perp} + p_{\parallel}) S_{\alpha} (S_{\alpha} K_{\psi} - S_{\psi} K_{\alpha}) \psi^2] \quad (4.2.5)$$

with the requirement that $\omega^2 \geq 0$ for stability.

Solutions to Eq. (4.2.3) are necessarily complicated. The results obtained by Kaiser^(4.5) at Lawrence Livermore Laboratory are reproduced in Fig. 4.2.1 for the case of an A-cell barrier configuration. Further theoretical and experimental work is required to handle this type of instability. Future developments may permit stable reactor operation at central-cell betas up to or beyond 0.4, and the present reactor design adopts this value.

Fig. 4.2.1

Marginal ballooning stability boundaries for an A-cell barrier configuration with $\beta_b/\beta_p = 10^{-3}$. The curves plotted through the open-circle points correspond to the vacuum field-line geometry; the curve through the solid circle points correspond to finite-beta field-line geometry, where α is the azimuthal angle. The dashed line gives the flute-interchange stability limit.

4.2.2.3 Drift-cyclotron-loss-cone instability (DCLC)

The minimum field region in the yin-yang plugs exhibits microinstabilities driven by anisotropies in the ion velocity distribution function (the electron velocity distribution function is nearly Maxwellian). The loss-cone portion in velocity space must constantly be replenished by a source of hot ions. This requires that the characteristic radial decay length of the plasma density be longer than a fixed number N of ion gyroradii for stabilization^(4.7):

$$R_p \equiv (n^{-1} dn/dr)^{-1} \geq N a_i \quad (4.2.6)$$

$$N \approx 2.6 (\omega_{ci} \omega_{pi}^{-2} + m_e m_i^{-1})^{-2/3},$$

where n is the ion number density, r the radial distance, a_i the ion gyroradius, ω_{ci} the ion cyclotron frequency, ω_{pi} the ion plasma frequency, and m_e and m_i the masses of the electrons and of the ions respectively. Even when (4.2.6) is not satisfied the stabilization of this mode is observed when a stream of warm plasma flows into the yin-yang plasma^(4.8). Such "stream stabilization" as is necessary is assumed here to be provided by warm ions escaping from the central-cell. In fact, other modes of instabilities are also stabilized by warm streaming ions (axial loss-cone mode, etc.).

Proper use of thermal barriers also contributes a strong element of stabilization for this class of instabilities and considerably alleviates the dependence on stream stabilization. This is achieved by transferring the site of worst instability to the thermal barrier regions themselves. There the plasma density is lower owing to the stronger magnetic fields, and stabilization for this class of instabilities is easier to achieve. Thus, the thermal barriers, introduced to help create a large electron temperature difference between the plugs and the central-cell, also help provide plasma microstabilization.

4.3 THE PLASMA MODEL

Figures 4.3.1a, b, and c show the approximate axial profiles of the vacuum magnetic field, ambipolar potential, and ion density at one end of the reactor. Radial dependence is neglected for simplicity. The barrier cell is the region between Z_{mb} (barrier mirror), and Z_{mp} (plug mirror).

4.3.1 Reactor Dimensions and First-Wall Loading

The plug midplane radius is given by flux conservation:

$$r_p = r_c \left[\frac{B_{oc,vac}}{B_{op,vac}} \left(\frac{1 - \beta_c}{1 - \beta_p} \right)^{1/2} \right]^{1/2}, \quad (4.3.1)$$

where r_c is the central-cell plasma radius, β_c and β_p (ratios of the perpendicular plasma pressure to the magnetic field pressure) are the central-cell and plug betas, and where $B_{oc,vac}$ and $B_{op,vac}$ represent the respective vacuum magnetic fields. The radius of the barrier cell at any point is also related to r_c by flux conservation:

$$r_b(z) = r_c \left[\frac{B_{oc,vac}}{B_{b,vac}(z)} \left(\frac{1 - \beta_c}{1 - \beta_b(z)} \right)^{1/2} \right]^{1/2} \quad (4.3.2)$$

where $\beta_b(z)$ and $B_{b,vac}(z)$ are the barrier beta profile and the barrier vacuum magnetic field profile, which assumes the form:

$$\begin{aligned} B_{b,vac}(z) &= B_{b,vac}(z_{ob}) + \left(B_{vac}(z_{mb}) - B_{vac}(z_{ob}) \right) \times \\ &\times (1 - 3\delta^2 + 2\delta^3) \quad \text{for } z_{mb} \leq z \leq z_{ob} \\ B_{b,vac}(z) &= B_{vac}(z_{mp}) + \left(B_{vac}(z_{ob}) - B_{vac}(z_{mp}) \right) \times \\ &\times (1 - 3\delta^2 + 2\delta^3) \quad \text{for } z_{ob} \leq z \leq z_{mp} \end{aligned} \quad (4.3.3)$$

Fig. 4.3.1a Vacuum magnetic field profile

Fig. 4.3.1b Plasma potential profile

Fig. 4.3.1c Ion density profile

Here δ is a dimensionless quantity which is linearly proportional to the distance from the barrier mirror. Other reactor dimensions are:

The central-cell length derived from the total fusion power

$$P_{Fus} = \frac{1}{4} n_c^2 \langle \sigma v \rangle_{DT} E_{fus} (\pi r_c^2 L_c) \quad (4.3.4)$$

and the first-wall radius

$$r_{FW} = r_c + 3\rho_\alpha, \quad (4.3.5)$$

which is used to calculate the first wall loading

$$\Gamma_{FW} = \frac{E_n}{E_{Fus}} \left(\frac{P_{Fus}}{2\pi r_{FW} L_c} \right). \quad (4.3.6)$$

In the above equations, n_c is the central cell ion density, E_n and E_{Fus} represent the neutron and the fusion energy release, ρ_α is the alpha-particle gyroradius, and $\langle \sigma v \rangle_{DT}$ is the fusion reaction rate averaged over a Maxwellian plasma and modeled by^(4.9):

$$\langle \sigma v \rangle_{DT} \approx \frac{3.05 \times 10^{-13} \exp(-17.7/E^{0.348})}{(1.0 + 0.1554 T_c^{1/3} - 0.05 T_c + 0.0364 T_c^{4/3})} \quad (4.3.7)$$

4.3.2 Plasma Densities

The densities in the central cell and the plugs are derived from the following beta equations:

$$n_c \approx \frac{(B_{oc,vac})^2 \beta_c}{2\mu_o (T_c + T_{ec} + 8.3 \times 10^{10} \langle \sigma v \rangle_{DT} T_{ec}^{3/2} E_\alpha f_{ea})} \quad (4.3.9)$$

$$n_p \approx \frac{(B_{op,vac})^2 \beta_p}{2\mu_o (0.9 E_p + T_{ep})},$$

where E_α is the alpha creation energy, f_{ea} is the fraction of alpha energy deposited into the electrons, derived from^(4.10):

$$f_{ae} \approx 0.8816 \exp(-T_{ec}/67.50) ; \quad (4.3.10)$$

T_c is the central cell ion temperature; T_{ep} is the plug electron temperature; E_p is the plug ion energy; and the factor 0.9 appears because neutral beam injection causes most of the plug energy to be perpendicular^(4.11).

There are two types of ions in the barrier cell: 1) trapped ions and 2) passing ions between the central cell and the barrier cell. The function $g_b(z)$ is defined as the ratio of total ion density to passing ion density:

$$g_b(z) = n(z)/n_{pass}(z) \quad (4.3.11)$$

where the following model is used for the $g_b(z)$ profile:

$$g_b(z) = 1 + (g_{bo} - 1) \frac{\left[1 - \frac{B_{vac}(z)}{B_{mb,vac}} \right]}{\left[1 - \frac{B_{bo,vac}}{B_{mb,vac}} \right]} ; \quad (4.3.12)$$

$$\text{with } g_{bo} \equiv g_b(z_{bo}) , \quad (4.3.13)$$

and where $n_{pass}(z)$ is derived from conservation of momentum of passing ions between the central cell and the barrier cell^(4.12):

$$n_{pass}(z) \approx n_c \frac{B(z) [1 - \beta(z)]^{1/2}}{B_{bm} [1 - \beta_c]^{1/2}} \left[\frac{T_c}{\pi (\phi_e - \phi(z)) + T_c} \right]^{1/2} \quad (4.3.14)$$

for $\phi(z) \leq \phi_e$.

For $\phi(z) > \phi_e$, we apply the Maxwell-Boltzmann relation of passing ions between the central cell and the barrier cell to get:

$$n_{pass}(z) = n_c \frac{B(z) [1 - \beta(z)]^{1/2}}{B_{bm} [1 - \beta_c]^{1/2}} \left[\frac{\exp\left(-\frac{\phi(z) - \phi_e}{T_c}\right) - \exp\left(-\frac{\phi_c}{T_c}\right)}{1 - \exp\left(-\frac{\phi_c}{T_c}\right)} \right] \quad (4.3.15)$$

for $\phi(z) > \phi_e$.

The Maxwell-Boltzmann relation between central cell electrons and barrier-cell electrons at $z \leq z_{b0}$ gives

$$n(z) = n_c \exp \left(- \frac{\phi_e - \phi(z)}{T_{ec}} \right) \quad \text{for } z \leq z_{b0}. \quad (4.3.16)$$

The particle balance between electrons becoming passing electrons by escaping from the plug and the collisional trapping of electrons into the plug^(4.13) yields the following approximate formula:

$$n(z) = n_p \left(\frac{T_{ec}}{T_{ep}} \right)^v \exp \left(- \frac{\phi_e + \phi_c - \phi(z)}{T_{ep}} \right) \quad \text{for } z \geq z_{b0}. \quad (4.3.17)$$

For the particular case $z = z_{t0}$, the last two equations give the potential ϕ_c which confines most of the central cell ions:

$$\phi_c = \left(\frac{T_{ep}}{T_{ec}} - 1 \right) \phi_b + T_{ep} \ln \left[\frac{n_p}{n_c} \left(\frac{T_{ec}}{T_{ep}} \right)^v \right]. \quad (4.3.18)$$

The case $v = 0$, derived by assuming a Boltzmann distribution on either side of the barrier is assumed here. Recent work^(4.14) indicates that v is close to 0.5, which would significantly degrade the reactor performance calculated from these equations. However, at the cost of extra ECRH heating to mirror-trap electrons in the barrier, ϕ_b could be raised significantly to overcome the deleterious effects of non-zero v ^(4.15).

4.3.3 The Plug Equations

The plug equations form a set of simultaneous nonlinear equations that may be solved self-consistently. To compute the confinement product $(n\tau)_p$ for plug particles, we use an expression developed by Logan and Rensink^(4.16) which incorporates both electron drag and ion scattering:

$$(n\tau)_p = \left[\left\{ \left(\frac{3.9 \times 10^{12}}{\ln \Lambda_{ii}} \right) E_{inj}^{3/2} \log_{10} (R_{eff}) \right\}^{-1} + \left\{ \left(\frac{2 \times 10^{13}}{\ln \Lambda_{ei}} \right) \tau_{ep}^{3/2} \ln \left(\frac{E_{inj}}{E_L} \right) \right\}^{-1} \right]^{-1} \quad (4.3.19)$$

where all energies are in KeV, n in cm^{-3} , and τ in S. The different parameters used in the above equation are:

a. the effective mirror ratio:

$$R_{eff} = \frac{R_D}{1 + \frac{\phi_p}{E_{inj}}} ; \quad (4.3.20)$$

b. the average ion loss energy

$$\bar{E}_L = \frac{E_{inj}}{1 + (\tau_{ii}/\tau_{drag})} + \frac{E_c (\tau_{ii}/\tau_{drag})}{1 + (\tau_{ii}/\tau_{drag})} , \quad (4.3.21)$$

$$\text{where } E_c = \frac{\phi_p}{R_D - 1} ,$$

and

$$\frac{\tau_{ii}}{\tau_{drag}} = 0.11 \left(\frac{E_{inj}}{\tau_{ep}} \right)^{3/2} \frac{\log_{10} (R_{eff})}{\ln (E_{inj}/E_L)} \frac{\ln \Lambda_{ei}}{\ln \Lambda_{ii}} ; \quad (4.3.22)$$

c. the electron and ion logarithms

$$\ln \Lambda_{ei} = \ln (5.7 \times 10^{13} \tau_{ep} \sqrt{n_p}) \quad (4.3.22)$$

and

$$\ln \Lambda_{ii} = \ln (1.4 \times 10^{15} \sqrt{T_e E_p / n_p}) . \quad (4.3.23)$$

d. the plug ion energy E_p is derived from the plug ion energy balance

$$E_p = \frac{E_{inj} - \bar{E}_L}{(n\tau)_p} (n\tau)_p^{drag} + \frac{3}{2} \tau_{ep} ; \quad (4.3.24)$$

e. the other parameters used in this model are:

the plug potential

$$\phi_p = \phi_e + \phi_c \quad , \quad (4.3.25)$$

the plug mirror ratio

$$R_p = \frac{B_{mp}}{B_{op} (1 - \beta_p)^{1/2}} \quad , \quad (4.3.26)$$

and the plug density n_p given by the plug beta equation (see Eq. 4.3.9).

4.3.4 The Barrier Scheme

The rate at which ions are trapped in the barrier cells is given by the following equation which has been fitted to Fokker-Planck results:

$$I_{\text{trap}} = 2e \int_{mb}^{mp} \frac{[n_{\text{pass}}(z)]^2}{5.5 \times 10^9 T_c^{3/2}} \left(1 + 0.55 \frac{B_{mb} [1 - \beta_c]^4}{B(z)[1 - \beta(z)]} \right) dv \quad (4.3.27)$$

where the integration is carried out over both barrier cell volumes. To untrap these ions, a specific pumping scheme is used: first, trapped ions that are far enough from the axis ("halo" of trapped particles) in the barrier cell are removed by a gas feed either by charge exchange or ionization. Thus we define the gas feed current:

$$I_{Gp} = f_D I_{\text{trap}} \quad . \quad (4.3.28)$$

The remaining trapped ions are pumped by three neutral beams (see Fig. 4.4.b).

The currents of these neutral beams are:

$$\begin{aligned}
 I_{LEPB} &= (1-f_D) f_{LEPB} \left[\frac{g_b(z_{LEPB})}{g_b(z_{LEPB}) - 1} \right] \left(1 + \frac{\langle \sigma v \rangle_{LEPB}^{ion}}{\langle \sigma v \rangle_{LEPB}^{cx}} \right) I_{TRAP} , \\
 I_{MEPB} &= (1-f_D) f_{MEPB} \left[\frac{g_b(z_{MEPB})}{g_b(z_{MEPB}) - 1} \right] \left(1 + \frac{\langle \sigma v \rangle_{MEPB}^{ion}}{\langle \sigma v \rangle_{MEPB}^{cx}} \right) I_{TRAP} , \\
 \text{and} \\
 I_{HEPB} &= (1-f_D) f_{HEPB} \left[\frac{g_b(z_{HEPB})}{g_b(z_{HEPB}) - 1} \right] \left(1 + \frac{\langle \sigma v \rangle_{HEPB}^{ion}}{\langle \sigma v \rangle_{HEPB}^{cx}} \right) I_{TRAP} , \quad (4.3.29)
 \end{aligned}$$

where the subscripts "LEPB," "MEPB," and "HEPB" mean low, medium, and high energy pump beams, and where the superscripts "ion" and "cx" are used to denote the ionization and charge exchange fractions of these currents, respectively.

4.3.5 Particle and Energy Balances

The central cell ion particle balance is written:

$$\frac{n_{co}^2}{(n_T)_c} \frac{V_c}{c} + \frac{1}{2} n_{co}^2 \langle \sigma v \rangle_{DT} V_c + I_{GP} = I_{LEPB}^{ion} + I_{MEPB}^{ion} + I_{HEPB}^{ion} + I_{AUX}^{ion} , \quad (4.3.30)$$

where the particle currents

$$I_{AUX}^{ion} \equiv \frac{\langle \sigma v \rangle_{AUX}^{ion}}{\langle \sigma v \rangle_{AUX}^{ion} + \langle \sigma v \rangle_{AUX}^{cx}} I_{AUX} , \quad (4.3.31)$$

and

$$I_{AUX}^{cx} \equiv \frac{\langle \sigma v \rangle_{AUX}^{cx}}{\langle \sigma v \rangle_{AUX}^{ion} + \langle \sigma v \rangle_{AUX}^{cx}} I_{AUX} \quad (4.3.32)$$

comprise an auxiliary fuel source located at the central-cell potential.

The confinement time-density product

$$(n\tau)_c \approx 5.547 \times 10^{11} \frac{g(R_{ci}/2)}{\ln \Lambda_{ii}} T_c^{3/2} \left(\frac{\phi_c}{T_c} \right) \exp \left(- \frac{\phi_c}{T_c} \right) \quad (4.3.33)$$

is the Pastukhov $(n\tau)^{(4.3)}$ appropriate to a magnetic mirror with ambipolar electrostatic effects with

$$\ln \Lambda_{ii} = \ln \left[3.08 \times 10^{14} \left(\frac{T_c^{3/2}}{n_c^{1/2}} \right) \right] \quad (4.3.34)$$

$$R_{ci} = \frac{B_{op} [1 - \beta_p]^{1/2}}{B_{oc} [1 - \beta_c]^{1/2}} \quad (4.3.35)$$

and

$$g(x) \equiv \frac{\sqrt{\pi}}{2} \left(1 + \frac{1}{x} \right)^{1/2} \ln \left\{ \left[\left(1 + \frac{1}{x} \right)^{1/2} + 1 \right] / \left[\left(1 + \frac{1}{x} \right)^{1/2} - 1 \right] \right\}. \quad (4.3.36)$$

The different terms of equation (4.3.30) are from left to right respectively:

- the Pastukhov end loss of central cell ions,
- the ion burnup rate in fusion reactions,
- the current of trapped ions that is pumped by the gas feed,
- the fueling due to the pump beams,
- the ionization current I_{AUX}^{ion} of an auxiliary fueling source.

The central cell ion energy balance is given by:

$$\begin{aligned} & \frac{1}{4} n_{co}^2 \langle \sigma v \rangle_{DT} \left[E_\alpha \left(1 + \frac{E_R}{E_{FUS}} \right) f_{\alpha i} - E_R \right] V_c - \frac{n_{co}^2 V_c}{(n)_c} (\phi_c + T_c) \\ & + \frac{n_{co}^2 V_c}{(n\tau)_{ei}} + \frac{3}{2} (T_{ec} - T_c) + \left[I_{AUX}^{ion} E_{c, inj} + I_{AUX}^{cx} (E_{c, inj} - \frac{3}{2} T_c) \right] \end{aligned}$$

$$\begin{aligned}
& + I_{\text{AUX}}^{\text{ion}} (V_{\text{LEPB}} + \phi_{\text{LEPB}} - \phi_e) + I_{\text{LEPB}}^{\text{CX}} (V_{\text{LEPB}} + \phi_{\text{LEPB}} - \phi_e - \frac{3}{2} T_c) \\
& + I_{\text{MEPB}}^{\text{ion}} (V_{\text{MEPB}} + \phi_{\text{MEPB}} - \phi_e) + I_{\text{MEPB}}^{\text{CX}} (V_{\text{MEPB}} + \phi_{\text{MEPB}} - \phi_e - \frac{3}{2} T_c) \\
& + I_{\text{HEPB}}^{\text{ion}} (V_{\text{HEPB}} + \phi_c) + I_{\text{HEPB}}^{\text{CX}} (V_{\text{HEPB}} + \phi_{\text{HEPB}} - \phi_e - \frac{3}{2} T_c) \\
& - I_{\text{GP}} (\frac{3}{2} T_c) \Big] / e = 0,
\end{aligned} \tag{4.3.37}$$

where $E_R = 45.0 + \frac{3}{2} T_c$ is a fusion reactant ion energy (4.3.38)

$$(n\tau)_{ei} \approx 2.5 \times 10^{13} T_{ec} / \ln \Lambda_{ei} \tag{4.3.39}$$

$$\text{and } \ln \Lambda_{ei} \approx \ln [2.65 \times 10^{13} T_{ec} / \sqrt{n_c}] \tag{4.3.40}$$

The different terms in equation (4.3.37) are:

- a. the alpha particle heating of ions,
- b. the Pastukhov energy loss out the ends,
- c. the energy transfer between ions and electrons,
- d. the energy gain due to ionization of an auxiliary fuel beam,
- e. the charge exchange energy transfer with an auxiliary fuel beam,
- f. the energy gain due to ionization of the LEPB,
- g. the charge exchange energy transfer with the LEPB,
- h. the energy gain due to ionization of the MEPB,
- i. the charge exchange energy transfer with the MEPB,
- j. the energy gain due to ionization of the HEPB,
- k. the charge exchange energy transfer with the HEPB, and
- l. the energy loss due either to ionization or to charge exchange with a gas feed.

The passing-electron particle balance may be written:

$$(I_{LEPB}^{ion} + I_{MEPB}^{ion} + I_{HEPB}^{ion} + I_{AUX}^{ion} + I_s)/e + \frac{n_{po}^2 (2V_p)}{(n\tau)_p} = \frac{n_{co}^2 V_c}{(n\tau)_{ec}} \quad (4.3.41)$$

where

$$(n\tau)_{ec} \approx 4.10 \times 10^9 \frac{g(R_{ce})}{\ln \Lambda_{ee}} T_{ec}^{3/2} \left(\frac{\phi_e}{T_{ec}} \right) \exp \left(\frac{\phi_e}{T_{ec}} \right) \quad (4.3.42)$$

with

$$\ln \Lambda_{ee} \approx \ln (2.83 \times 10^{13} T_{ec} / \sqrt{n_{co}}) \quad (4.3.43)$$

The following physical processes are considered in equation (4.3.41):

- electrons produced by ionization of the LEPB, MEPB, HEPB and auxiliary fuel beam, respectively,
- auxiliary current I_s of cold electrons that one may wish to inject into the central cell
- plug electrons becoming passing electrons every time plug ions are lost out the end of the machine
- the Pastukhov end loss of electrons.

The passing-electron energy balance is given by:

$$\begin{aligned} & \frac{1}{4} n_{co}^2 \langle \sigma v \rangle_{DT} \left[E_\alpha \left(1 + \frac{E_R}{E_{FUS}} \right) f_{ae} \right] v_c + \frac{1}{e} I_s \phi_e \\ & - \frac{n_{co}^2 V_c}{(n\tau)_{ec}} (\phi_e + T_{ec}) + \frac{n_{po}^2 (2V_p)}{(n\tau)_p} (\phi_b + T_{ep}) - \frac{n_{co}^2 V_c}{(n\tau)_{ei}} \cdot \frac{3}{2} (T_{ec} - T_c) \\ & + P_{cp}^* + P_{cp} = \frac{-1}{e} \left[I_{LEPB}^{ion} (\phi_b + \bar{E}_{e \text{ LEPB}}) \right. \\ & \quad + I_{MEPB}^{ion} (\phi_e - \phi_{MEPB}) \\ & \quad \left. + I_{HEPB}^{ion} (\phi_b) \right] \quad (4.3.44) \end{aligned}$$

where

$$E_e \text{ LEPB} = \phi_b + \phi_{\text{LEPB}} - \phi_e, \quad (4.3.45)$$

$$P_{cp} \approx G_{ep} \frac{n_p^2}{(n\tau)_p^{ee}} (T_{ep} - T_{ec}) \exp \left(- \frac{\phi_c + \phi_b}{T_{ep}} \right) (2 V_p) \quad (4.3.46)$$

$$P_{cp}^* = \int_{z_{bo}}^{z_{mp}} 2 G_{et} \frac{n_p^2}{(n\tau)_t^{ee}} (T_{ep} - T_{ec}) \exp \left(- \frac{\phi_e + \phi_c - \phi(z)}{T_{ep}} \right) dv, \quad (4.3.47)$$

$$G_{ep} \approx G_{et} \approx 1, \quad (4.3.48)$$

$$(n\tau)_p^{ee} \approx 8.19 \times 10^9 \frac{T_{ep}^{3/2}}{\ln \Lambda_p^{ee}}, \quad (4.3.49)$$

$$(n\tau)_t^{ee} \approx 8.19 \times 10^9 \frac{T_{ep}^{3/2}}{\ln \Lambda_t^{ee}}, \quad (4.3.50)$$

$$\ln \Lambda_p^{ee} = \ln (2.83 \times 10^{13} T_{ep} / \sqrt{n_p}), \quad (4.3.51)$$

$$\ln \Lambda_t^{ee} = \ln \left\{ 2.83 \times 10^{13} T_{ep} / [n(z)]^{1/2} \right\}. \quad (4.3.52)$$

The different terms in equation (4.3.44) represent:

- the alpha heating of the passing electrons,
- the gain due to the influx of cold auxiliary electrons (see Eq. 4.3.41)
- the Pastukhov energy loss
- the energy gain each time a trapped electron becomes a passing electron

- e. the energy transfer between central-cell ions and electrons
- f. the energy transfer between the passing electrons and the trapped electrons in the transition regions
- g. the energy transfer between the passing electrons and the trapped electrons in the plugs
- h. the contributions to passing-electron energy due to the arrival of electrons in the LEPB, MEPB, and HEPB, respectively.

The trapped-electron energy balance, which completes the system of equations, is

$$P_{\text{ECRH}} + \frac{n_{\text{po}}^2 (2V_p)}{(n\tau)_{\text{drag}}} (E_p - \frac{3}{2} T_{\text{ep}}) - \frac{n_{\text{po}}^2 (2V_p)}{(n\tau)_p} (\phi_c + \phi_b + T_{\text{ep}}) \quad (4.3.53)$$

$$= P_{\text{cn}}^* + P_{\text{cp}} + P_{\text{synchrotron}} + \frac{1}{e} I_{\text{LEPB}}^{\text{ion}} \left[(\phi_{\text{LEPB}} + \phi_b - \phi_c) + E_{\text{e LEPB}} \right].$$

The different terms in the above equation are:

- a. the ECRH, power input
- b. the heating due to plug ion drag on the plug electrons
- c. the energy loss due to cooling by incoming neutral-beam electrons
- d. the energy loss to the passing electrons in the transition regions,
- e. the energy loss to the passing electrons in the plug,
- f. the loss of energy due to synchrotron radiation
- g. the cooling effect of electrons that arrive with the LEPB.

4.4 SOLUTION OF THE PLASMA MODEL EQUATIONS

4.4.1 Description of the TMRTB Code

The tool used to study different operating scenarios is a code developed at the Lawrence Livermore Laboratories^(4.17). This code is capable of simulating radial density profiles of the form:

$$n_c = n_{co} \left[1 - \left(\frac{r}{r_c} \right)^\alpha \right] \quad (4.4.1)$$

and

$$n_p = n_{po} \left[1 - \left(\frac{r}{r_p} \right)^\alpha \right],$$

where α is an input. Also, given the axial vacuum magnetic field profile of the barrier region (input), the code calculates the axial profiles of the potential, the density, and the perpendicular beta. In all other respects, the code is zero-dimensional.

As a first step, the program uses the following input parameters:

$B_{oc,vac}$ = central cell vacuum field

$B_{op,vac}$ = plug midplane vacuum field

$B_{mp,vac}$ = plug mirror vacuum field

$B_{ob,vac}$ = barrier mirror vacuum field

β_{co} = perpendicular beta on axis in central cell

β_{po} = perpendicular beta on axis in plugs

$\phi_p = \phi_e + \phi_c$ = ambipolar potential at plug midplane.

Finally, there are two input options:

either:

r_c = central cell plasma radius

and:

L_c = central cell length

or:

Γ_{FH} = first wall loading of 14 MeV neutrons

and:

P_{Fus} = fusion power.

4.4.2 Program Structure

The program contains three iterative loops (see Fig. 4.4.1):

- a. the first loop uses a previous guess of the mirror ratio in the barrier cell, R_b (the vacuum mirror ratio provides a convenient starting guess), and then proceeds as follows:

1. find the barrier potential ϕ_b by solving numerically the following equation:

$$\left(\frac{\phi_b}{T_c}\right) = \left(\frac{T_{ec}}{T_c}\right) \ln \left\{ \left(\frac{R_b}{g_{bo}}\right) \left[1 + \pi \left(\frac{\phi_b}{T_c}\right) \right]^{1/2} \right\}, \quad (4.4.2)$$

which combines Eqs. (4.3.14) and (4.3.16) to eliminate n_b .

2. Calculate n_{bo} from

$$n_{bo} = n_{co} \exp(-\phi_b/T_{ec}) \quad (4.4.3)$$

where n_{co} is the central cell density on axis (see Eq. (4.4.1)).

3. Compute the profile-averaged perpendicular beta at both the barrier midplane and mirror:

$$\bar{\beta}_{ob} = \beta_{obo} \left(\frac{\alpha}{\alpha + 2}\right) = \frac{n_b \left[(T_c/R_b) + T_{ec} \right]}{\left[(B_{ob,vac})^2 / 2\mu_0 \right]} \left(\frac{\alpha}{\alpha + 2}\right)$$

and

$$\bar{\beta}_{mb} = \beta_{mbo} \left(\frac{\alpha}{\alpha + 2}\right) = \frac{n_c \left[(T_c/R_b) + T_{ec} \right]}{\left[(B_{mb,vac})^2 / 2\mu_0 \right]} \left(\frac{\alpha}{\alpha + 2}\right) \quad (4.4.4)$$

INPUT PARAMETERS: T_c T_{ec} T_{ep} $B_{oc,vac}$ $B_{np,vac}$ $B_{op,vac}$ $B_{ml,vac}$ B_{co} B_{po} E_{injp} ϕ_p or $F_c L_c$ P_{fus} Γ_{FW}

PROGRAM FLOWCHART

Fig. 4.4.1 Tandem Mirror Reactor Code TMRTB Flow Chart

where β_{ob} and β_{mbo} are on-axis perpendicular betas. We have used Eq. (4.4.1) and the fact that the betas are proportional to density to calculate the profile averaged perpendicular betas:

$$\begin{aligned}\bar{\beta}_{ob} &= \frac{1}{\pi r_b^2} \int_0^{r_b} \beta_{ob} \cdot 2 \pi r \, dr = \left(\frac{\alpha}{\alpha + 2} \right) \beta_{obo} \\ \bar{\beta}_{mb} &= \frac{1}{\pi r_b^2} \int_0^{r_b} \beta_{mb} \cdot 2 \pi r \, dr = \left(\frac{\alpha}{\alpha + 2} \right) \beta_{mbo}\end{aligned}\quad (4.4.5)$$

4. Use:

$$R_b = \frac{\beta_{mb,vac}}{\beta_{ob,vac}} \sqrt{\frac{1 - \bar{\beta}_{mb}}{1 - \bar{\beta}_{ob}}} \quad (4.4.6)$$

to obtain a better guess for R_b .

The entire process is then repeated until convergence to self-consistent values of ϕ_b , n_{bo} , $\bar{\beta}_{ob}$, $\bar{\beta}_{mb}$, and R_b is obtained.

- b. The second loop uses the equations describing the Logan and Rensink plug model and the plug beta equation to calculate the plug confinement parameter $(n\tau)_p$, the plug density n_p , and the average energy of the plug ions E_p :
- c. The third loop encompasses a system of five linear equations which are described in section (4.3.5) (see also Fig.4.4.1).

After reading the input parameters, the program uses Eq. 4.3.8 to solve for n_c ; then, depending on the input option, the code calculates either P_{Fus} and r_{FW} (Eqs. (4.3.4), (4.3.5)) or r_c and L_c (Eqs. (4.3.4), (4.3.5),

and (4.3.E)). The total fusion power is defined in the following way:

$$P_{Fus} = \int_0^{r_c} \frac{1}{4} n_c^2 \langle \sigma v \rangle_{DT} E_{fus} \cdot 2 \pi r dr \cdot L_c$$

$$P_{Fus} = \frac{1}{4} n_{co}^2 \langle \sigma v \rangle_{DT} E_{fus} \pi r_o^2 L_c \frac{\alpha^2}{(\alpha + 1)(\alpha + 2)} \quad (4.4.7)$$

which defines a "zero dimensional radius"

$$r_{co} = r_c \frac{\alpha}{\sqrt{(\alpha + 1)(\alpha + 2)}} \quad (4.4.8)$$

Other parameters calculated before entering the first loop are:

- the profile-averaged perpendicular betas in both the central cell and the plug (in the spirit of Eq. (4.4.5))
- the plug radius from conservation of magnetic flux (Eq. (4.3.1)) and the zero dimensional plug radius
- the central cell and the plug "zero-dimensional" volumes

$$V_c = \pi r_{co}^2 L_c \quad (4.4.9)$$

$$V_p = \frac{4}{3} \pi r_{po}^3$$

Before entering the third loop (see Fig. (4.4.1)), the program calculates the ion confining potential from

$$\phi_c = T_{ep} \ln \left(\frac{n_{po}}{n_{bo}} \right) - \phi_b \quad (4.4.10)$$

and the central cell potential from

$$\phi_e = \phi_p - \phi_c \quad (4.4.11)$$

The code then proceeds to calculate

- a. the density profile, the ambipolar potential profile, and the perpendicular beta profile in the barrier region^(2.10).
- b. a differential area and a differential volume

$$da(z) = 2\pi r(z) dr(z)$$

$$dv(z) = \pi r^2(z) dz \quad (4.4.12)$$

- c. the rate at which ions are trapped into the barrier region
- d. the power transfer from trapped to passing electrons in the transition regions, and in the plugs (Eqs. (4.3.46) and (4.3.47))
- e. the synchrotron radiation power lost in the transition regions and in the plugs^(2.11).

After exiting the third loop, the program calculates the ECRH power needed in the barrier region from Eq. (2.4.53), and the Q value from

$$Q = \frac{P_{Fus}}{P_{pump} + P_{NB}^{plug} + P_{ECRH}} \quad (4.4.13)$$

Here P_{pump} is the pumping power needed in the barrier region, and P_{NB}^{plug} is the neutral beam power injected in the plugs.

4.4.3 Computed Tandem Mirror Reactor Parameters

Table 4.4.1 shows the optimized reactor parameters for $P_{Fus} = 4600$ MWf and $P_{Fus} = 3580$ MWf, appropriate for coupling to the synfuel plant at a hydrogen chemical equivalent power of 1671 MW_{H₂}. As discussed in Section 7.9 and Figs. 7.17 and 7.18, these two cases correspond to power distribution systems with bottoming, and topping and bottoming electric generation respectively. In Figs. 7.17 and 7.18 the reactor Q value (= 11.6) is adjusted so that no external recirculating power to the TMR is required. The key output parameters are central-cell length $L_c = 213$ m and 207 m, respectively and neutron wall loadings $\Gamma_w = 1.48$ and 1.68 MW/m². The quantities P_{Fus} and Γ_w are input and L_c is derived under optimized plasma conditions.

Table 4.4.1 TMR parameters for coupling to the synfuel plant with bottoming, and topping and bottoming electric power generation

		<u>Topping</u>	<u>Bottoming</u>
Central Cell			
Ion Density (n_c)	(cm^{-3})	$1.7 \text{ e} + 14$	$1.7 \text{ e} + 14$
Ion Temperature (T_c)	(Kev)	19.0	18.4
Electron Temperature (T_{ec})	(Kev)	23	23
Vacuum Magnetic Field ($B_{c,vac}$)	(T)	2.9	2.9
Plasma Magnetic Field ($B_{c,plas}$)	(T)	2.5	2.5
Beta (long-thin approximation) (β_{oc})		0.4	0.4
C.C. Ambipolar Potential (ϕ_e)	(Kev)	170	170
Central Cell Length	(m)	213	207
Profile-averaged Plasma Radius (r_c)	(m)	1.27	1.4
First-Wall Radius (r_{FW})	(m)	1.56	1.69
Barrier Cell			
Midplane Ion Density (n_b)	(cm^{-3})	$9.1 \text{ e} + 12$	$9.2 \text{ e} + 12$
Ratio of Peak and Passing Particles (g_{ob})		2	2
Vacuum Midplane Field ($B_{ob,vac}$)	(T)	1.2	1.2
Vacuum Mirror Ratio ($R_{b,vac}$)		10.3	10.3
Beta at Midplane (β_{ob})		$6.7 \text{ e} - 2$	$6.8 \text{ e} - 2$
Pump-Beam energies:			
Low-Energy (V_{LEPB})	(Kev)	1.3	1.3
Medium (V_{MEPB})	(Kev)	34	34
High (V_{HEPB})	(Kev)	69	69
Midplane Ambipolar Potential (ϕ_{HEPB})	(Kev)	102	102
Barrier Length (L_b)	(m)	13.5	13.5
Barrier Potential Dip (ϕ_b)	(Kev)	67	67

Table 4.4.I Continued

		<u>Topping</u>	<u>Bottoming</u>
Plug			
Ion Density (n_p)	(cm^{-3})	2.3 e + 13	2.3 e + 13
Ion Energy (E_p)	(Kev)	916	911
Electron Temperature (T_{ep})	(Kev)	171	168
Vacuum Midplane Field ($B_{op,vac}$)	(T)	4.0	4.0
Vacuum Mirror Ratio ($R_{p,vac}$)		1.5	1.5
Beta (long-thin approximation) (β_{op})		0.6	0.6
Plug N.B. Injection Energy ($E_{p,inj}$)	(Kev)	400	400
Plug Ambipolar Potential (ϕ_p)	(Kev)	265	260
Plug Radius	(m)	1.13	1.25
Power Bookkeeping			
ECRH Power	(MW)	248	335
Barrier-Cell Neutral-Beam Power	(MW)	73	57
CC Neutral-Beam Power	(MW)	0	0
Plug Neutral-Beam Power	(MW)	7.4	9.5
Fusion Power	(MW)	3850	4600
Neutron Wall Loading	(MW/m)	1.48	1.68
Q		11.6	11.6

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SECTION 5
THE CAULDRON BLANKET MODULE

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5.0 THE CAULDRON BLANKET MODULE

This is one of two basic, high temperature blanket module concepts considered in this study. The concepts are intended to be complementary. The flowing microsphere design, the so-called "solid" blanket approach, is discussed in Section 6.0. The Cauldron design, a Li-Na selective distillation blanket module, is our "liquid" blanket approach. This is the intended meaning of the two competing concepts being complementary; solid moderator vs liquid moderator. The Cauldron design is the subject of this section.

5.1 General Description

The Tandem Mirror Reactor has a fundamentally simple, cylindrical geometry in the energy producing central cell. We are able to surround the plasma in this region with a moderating, lithium-sodium liquid envelope which is contained in an appropriate structure. This envelope lies between and under the successive field coils that are spaced at intervals of four meters. The coils magnetically contain the plasma. We have chosen to divide this energy absorbing envelope at the vertical mid-plane of the central solenoid and at the half distance between magnetic coils to form what we designate as a unit cell. Each unit cell contains one blanket module and its mirror image. Figure 5.1.1 illustrates the unit cell and its modules. The discussion that follows focuses on the module.

Fig. 5.1.1 Cross-section through two unit cells of the TMR.

THE CAULDRON CONCEPT-HOUSING A HOT FLUID IN A COOL CONTAINER

Fig. 5.1.2 Cross-section through a Cauldron Blanket Module. In the binary LiNa fluid of the pool the sodium vaporizes and transfers the energy deposited in the blanket to the heat exchanger in the dome. This energy is transported to remote processors for the thermochemical production of hydrogen. The hot fluid of the pool is enveloped by the "cool" structural container. The steep temperature gradient needed to produce the cool container is achieved using a metallic "feltmetal" insulation across which a controlled quantity of energy is allowed to pass.

Fig. 5.1.3 Relative flow velocities across 1 meter² of flow area with energy transfer of 18.8 MW corresponding to 2 MW/M² wall load and a first wall area of 3m².

5.1.1 The Thermal Cycle of the Module

A cross-sectional view of the Cauldron module is illustrated in Figure 5.1.2 Notice the module's resemblance to a pool boiler. It is, however, substantially more complicated than a pool boiler due to geometric effects, due to the exponential energy generation in the fluid contained within the module and, last but not least, due to MHD effects on the convective mixing of the two liquid metals, lithium and sodium, we have chosen to use in the pool. The two liquids in this cauldron module act as both the neutron moderator and heat transfer fluid, absorbing energy in direct proportion to the energy input and transferring it by latent heat of vaporization of the sodium to a heat exchanger in the dome of the vessel. The lithium performs the function of tritium breeding. In the dome the condensing vapor heat exchanger (CVHX) transfers the thermal energy out of the module to various chemical processors located some distance from the reactor. The cauldron vapor dome is processed for the recovery of tritium and helium and is discussed in Section 5.7.

Historically, the basic idea of a pool boiling blanket module using lithium as the single fluid was first suggested twelve years ago by J.D. Lee.^(5.1.1, 5.1.2) His idea was set aside and not investigated in any detail because it was not utilizable with the complicated mirror reactor geometries we at the Lawrence Livermore National Laboratory had at that time. Neither did it seem particularly adaptable to tokamak reactor topology although in the tokamak case the constraints may have been a little

less severe than they were with the old classical mirror. More recently, Pendergrass et al. suggested the lithium boiler for a 1500 to 2000K reactor application for either magnetic or inertial confinement.^(5.1.3) The lithium pool boiler would fault on two counts if it were to be proposed for 1200K use. The first difficulty with the single fluid, lithium boiler at the temperatures in which we are interested (~1200K) is one of a requirement for extremely high vapor velocities as the vapor travels from the pool surface to the CVHX. The energy density of the lithium vapor is simply too low and for a given quantity of energy transport the vapor velocity must be very high. The product of vapor density and latent heat of vaporization is inadequate. The second difficulty in using only lithium as the pool boiler fluid is related to the first. The quantity of vapor bubbles existing within the pool volume would be excessively high due to the low energy density. These bubbles, nucleated at sites within the pool and subsequently collapsing as convective mixing takes place, would more than totally displace the volume of liquid. Even at temperatures as high as 1500K this second difficulty would be a problem.

The major modification to the basic concepts of Lee or Pendergrass was to substitute a binary mixture of either LiNa or LiK for the single lithium fluid. The use of the binary mixture dramatically changes the operating character of the boiler.

With this change the high vapor density sodium or potassium preferentially vaporizes leaving behind the lithium in the liquid state to do its neutron moderating, tritium producing function. The sodium or potassium vapor, traveling at vapor velocities roughly two orders of magnitude less than that of lithium, condenses on the heat exchanger tubes in the dome, yields energy and returns as liquid droplets to the pool thus completing the cycle. We have selected the lithium/sodium mixture for our studies. The two fluids are miscible and tritium production with a 50-50 mix is greater than 1. The neutronics of the potassium is a little less favorable although the LiK can run cooler for the same vapor velocity.

We have calculated the range of vapor velocities that would be required as a function of the four parameters; (a) a wall load energy input of 2 MW/m^2 , (b) an estimated 1 m^2 of available vapor flow area, (c) a first wall area of $3 \pi \text{ m}^2$, and (d) temperature of the vapor. These data are plotted in Figure 5.1.3 for lithium, sodium and potassium as are the sonic velocities of the vapors. One can see immediately that the high vapor velocity required for the lithium at 1200K (nearly sonic) precludes its use. The two other fluids are much more likely to be acceptable for velocity considerations and also aid in convective mixing within the pool.

5.1.2 Energy Transfer Within the Cauldron

There are two macro-fluid mixing forces acting within the

pool which, we believe, will aid in the overall heat transfer process. These two forces are created by (1) the exponentially declining energy deposition in the blanket with increasing radius and (2) the return path of the higher density sodium condensate to the outer radius of the blanket module because of the physical location of the heat exchanger in the dome. As a result, a strong convective mixing loop tends to be set up. Within that loop there are also likely to be established micro-convective mixing loops. These micro loops are considered to be the normal mode of energy transfer in conventional pool boiling where vapor bubbles are created at a surface nucleation site, migrate to a cooler zone and collapse. In this conventional pool boiling energy is bootstrapped to the surface by little eddies of local convective mixing. Very little energy may be carried by vapor bubbles from the interior directly to the surface. Tong estimates this latter contribution to be of the order of 2%^(5.1.4) in one particular case. We must determine if this is applicable. The magnetic field complicates this energy transfer, as does the fact that nucleation sites must occur in the bulk fluid rather than on surfaces because of internal heat generation.

5.1.3 Magnetic Field Effects on Pool Boiling

In this particular pool boiling heat transfer process we must also assess the effect the magnetic field will have on the overall process. The magnetic field, exerting a force as it does to contain the plasma, also effects the electrically conducting fluid in the module. It is an established fact that the B field will act as a brake to

Fig. 5.1.4 Blanket geometric constraints based upon fusion driver dimensions.

slow the turbulent mixing and the natural convection flow. To what overall extent this will occur is not yet clear. We have completed a preliminary analysis which is discussed in detail later in Subsection 5.5. We conclude that due to the B field some additional superheat will occur and that bubble velocities will decrease. There is also a possibility that the size or the life history of any one micro-convective mixing loop will decrease with increasing B field. Wong, et al. and their analytical model of a spherical bubble being forced into an ellipsoid shape under the influence of a unidirectional B field support this speculation.^(5.1.5) It could be envisioned that the elongated ellipsoid bubble must collapse more readily than the spheroid due to its larger surface to volume ratio and therefore would have a shorter life. Fraas, et al have performed tests with potassium boiling on a cylindrical rod in transverse fields up to 6 Tesla and noted relatively little effect from the magnetic field.^(5.1.6) Our fields in the central cell of the TMR are relatively weak, ~2.T. Before we can be totally confident of the heat transfer performance of the Cauldron, modelling tests will have to be performed. Mathematical analysis is not sufficient in itself nor are the experimental data of experimenters such as Lykoudis totally adequate because there is no internal heat generation and nucleation is off of surfaces and not in the bulk fluid.^(5.1.7) A scale model test is tentatively planned for FY '81.

5.1.4 Topological Constraints on Module and Unit Cell Spacing

The maximum allowable longitudinal spacing of the field coils in the central cell is determined by the allowable

ripple in the magnetic field. This ripple has been set at 5% at the plasmas outer radius. It is defined as the amount the magnetic field can change (decrease) between successive coils without upsetting plasma stability. For our study this ripple tolerance allowed a longitudinal coil spacing of four meters when the mean coil diameter was 7.6 meters and the plasma radius was 1.4 meters. Figure 5.1.4 shows these dimensions plus shield thickness requirements and the necessary coil cross-section that establish the geometric operating region in which the blanket modules must be placed.

5.1.5 The Cool Container

Success of this Cauldron concept depends in part upon our ability to contain the very hot fluid (1200K) in a cool structural container. This is accomplished by establishing a steep temperature gradient across a thickness of material intervening between the hot fluid and the structural container itself. The material chosen is a commercial product called "feltmetal" which can be fabricated to have an effective thermal conductivity k_e tailored to the needs of the problem. For our particular case the k_e was 0.675 w/mK. In our application an important feature and advantage of the feltmetal is its substantial compressive strength and its low shear strength.

We introduce a continuous membrane to separate the feltmetal from the LiNa fluid. This loads the feltmetal in compression due to the hydrostatic head of the fluid (~8m)

and due to the sodium vapor pressure (~ 1.5 atm). The 500K temperature gradient across the material, with the differential expansion it creates, is accommodated by allowing the feltmetal fibers to slide on one another (the low shear stress feature). The energy flow into this structural container is controlled so as to obtain the 500K temperature gradient. The heat transfer in the overall module is set up in such a way that all of the energy generated in four volumes; in the first wall tubes, in the first wall coolant, in the feltmetal, and in the membrane is removed by the first wall heat exchanger (FWHX). All the thermal energy generated in the LiNa binary mixture pool is removed by the condensing vapor heat exchanger (CVHX) located in the dome of the module. Roughly 5-10% of the energy is handled by the FWHX at ~ 700 K and 90-95% is processed by the CVHX at 1200K.

5.1.6 Achieving a Low Stress Structure

It may be seen from Figure 5.1.2 that this Cauldron module can be characterized as a low pressure, low stress assembly, certainly a design advantage. The main forces acting internal to the Cauldron are the vapor pressure of the sodium at ~ 1.5 atmospheres at 1200K and the hydrostatic head of the fluid of about eight meters. In the vapor dome region the pressures are nearly balanced between the condensing vapor and the coolant flowing in the CVHX tubes since the coolant may also be sodium. It should be noted that by design choice this condensing vapor heat exchanger is physically outside of the bore of the field coil winding

and no MHD effects can be generated within the coolant since there is no magnetic field. The vapor and the condensate droplets are also MHD effect free because of low electrical conductivity and individual droplet flow. For the cool structural container (FWHX) enveloping the liquid metal pool, in its role as a heat exchanger, the coolants considered were sodium, helium and an organic polyphenyl fluid. These three coolants produce a spectrum of pressures and stresses as will be seen.

Because of the relatively low stress inherently designed into the Cauldron we have been able to consider the use of structural materials with low residual radioactivity---an important safety feature. Specifically, we investigated the titanium vanadium alloys. The low tensile or compressive stress in the structure relaxes the problem of creep in both the hot membrane, (the material that separated the molten LiNa bath from the fuel metal) and in the hot fuel metal. An imposed design constraint is not to exceed 1% creep deformation in 100,000 hours (11 years). This materials area warrants substantial further study; for material choices, for stress analysis, for fabrication techniques, for creep stress, etc. The cool structural container itself, operating as it does at temperatures of 700K or less should pose no particularly difficult problems. Section 5.6 discusses materials for this module in detail.

5.1.7 Assembly Disassembly

Figures 5.1.5 through 5.1.8 graphically illustrate the advantages for assembly-disassembly purposes of the TMR with its simple central cell and the particularly strong advantages of the Cauldron module with its integral heat exchanger. There is only one module type and its mirror image. Four modules are required for each magnet field coil. Two modules are slipped into the coil from one side. A similar set is fed into the coil assembly from the other side. The five pieces, four modules and a coil, are designated as a unit cell. Spacing between coils may be four meters or less.

5.1.8 Using Heat Pipes in the Dome

There is an interesting variation on the basic Cauldron module and that is to use heat pipes in the vapor dome instead of the standard heat exchanger coolant tubes. There are several benefits:

- The heat pipes can be used as flux amplifiers or deamplifiers extending the number of coolant choices (on the condenser side of the heat pipe.)
- The heat pipes further isolate the module for additional safety.

- A method of tritium recovery is introduced. Tritium permeates through the heat pipe wall at partial pressures of 10^{-6} torr, is then swept along by the heat pipe vapor (like a diffusion pump) to the extreme end of the condenser part of the heat pipe where it collects as a non-condensable, builds up its pressure and finally permeates through the tube wall at partial pressures as high as the pressure of the vapor. Very little area for permeation is needed because of the pressure buildup.

Fig. 5.1.5 Two Cauldron modules
in a preliminary
state of assembly.

Fig. 5.1.6 Two Cauldron modules
joined and ready for
installation into the
bore of the solenoid.

Fig. 5.1.7 Perspective of a unit cell on its assembly cart. The four modules and the solenoidal coil are pre-assembled and then brought to the reactor area for assembly in the central cell. The axial length of the unit cell is approximately 4 meters.

Fig. 5.1.8 A partially assembled central cell of the TMR showing a number of unit cells in place. The end plugs with their neutral beam injector ports are also shown in a highly simplified way. The length of this reactor for synfuel application could be expected to be approximately 200 meters.

Blank

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5.2 The First Wall Heat Exchanger (FWHX)

5.2.1 Introduction

For the thermochemical process cycle that we use to produce hydrogen, there will be some necessary partitioning of energy to drive the various components and steps of the process. This energy partitioning will be both in the form of quantity: the number of kilojoules required to cause a particular set of chemical reactions to occur; and quality: the temperature range over which any one set of chemical reactions is likely to take place.

For the General Atomic sulfur-iodine cycle, in order to have a manageable system for our preliminary analysis, we assume three principal heat exchangers driving the total thermochemical process. As seen from the fusion reactor side as an energy source these are:

1. The Condensing Vapor Heat Exchanger (CVHX)
2. The Thermal Component of the Direct Convertor Heat Exchanger (DCHX)
3. The First Wall Heat Exchanger (FWHX)

The temperatures over which these three heat exchangers are expected to operate are specified in Figure 5.2.1 as is the approximate percentage of the total energy each of the three heat exchanger will supply. See Section 7 for details.

Fig. 5.2.1 Thermal energy sources and their temperature regimes in the TMR for coupling it to the General Atomic Sulfur Iodine Cycle.

As can be noted, the First Wall Heat Exchanger and the Direct Converter Heat Exchanger, operated in series, will supply about 15% perhaps 20% of the total process heat. The principal amount of the energy needed for the process chemistry, ~85%, must come from the Condensing Vapor Heat Exchanger. It supplies high quality heat at high temperature (1200K).

In the following, we describe a substantial part of the conceptual design details of the FWHX. Although it supplies only 5-10% of the process heat it is very important as a cooled structural container. Data on heat

transfer, thermal hydraulics, and the structural stresses that arise as a consequence of sets of varying conditions and assumptions are provided. Our first choice for the FWHX coolant is sodium since it is also a primary choice coolant for the condensing vapor heat exchanger. The backup coolants are helium and the organic polyphenyls.

5.2.2 The Dual Role of the First Wall Heat Exchanger

The FWHX, in this Cauldron Blanket Module Design, plays two important, interrelated roles:

- A. It provides process heat to the thermochemical plant at fairly modest temperatures ($\sim 500 - 750\text{K}$).
- B. Most importantly, it forms the cooled structural container that houses the very high temperature (1200K) Li-Na moderating/heat transfer fluid. This cool container concept has already been shown in Fig. 5.1.2.

The need to be able to house a very hot fluid in a cool container arises from obvious considerations of preserving structural strength. For our heat exchanger this thermally cool structure design is accomplished by introducing a fibrous metal sheath (a commercial product called "Feltmetal"), sandwiched between the structural container tube array and the Li-Na pool. This feltmetal is tailored so as to have a certain effective thermal conductivity, k_e , to provide the large temperature gradient required.

The feltmetal is isolated and protected from direct contact with the Li-Na pool by a thin metallic membrane. Two structural characteristics of the feltmetal are very valuable in this application (a) its compressive strength is reasonably high so that it can safely take the stresses due to the hydrostatic head and the pressure of the sodium vapor in the pool. And (b) its shear strength is quite low since the micron-sized fibers can slide on one another (like a deck of cards).

The low shear strength allows differential expansion to be accommodated and temperature induced stresses thus kept at minimal levels. Figure 5.2.2 and Table 5.2.1 illustrate some of the characteristics of this commercially available product. Metal fibers were chosen over ceramic from radiation damage considerations.

Fig. 5.2.2 Different Materials and Structures of Some of the Commercial Feltmetals

	DENSITY	THICKNESS INCHES	AREA DENSITY (LBS. FT. ²)	PORE SIZE RANGE (MICRONS)	TENSILE STRENGTH (PSI)	THERMAL CONDUCTIVITY (BTU-FT. HR.-FT. ² . F)	ELECTRICAL RESISTIVITY (MICROHM.-CM.)
347 SS	20	062	0.52	18-72	1,300	0.10	1,900
	30	062	0.77	10-42	3,000	0.16	1,100
	40	062	1.04	7-27	6,000	0.24	700
	60	062	1.55	3-12	16,000	0.48	300
	20	125	1.04	18-72	1,300	0.10	1,900
	30	125	1.55	10-42	3,000	0.16	1,100
	40	125	2.08	7-27	6,000	0.24	700
	60	125	3.10	3-12	16,000	0.48	300
	20	250	2.08	18-72	1,300	0.10	1,900
	40	015	0.28	12-48	3,400	0.95	72
	20	040	0.37	32-128	760	0.39	190
	30	040	0.56	18-74	1,800	0.64	110
NICKEL	40	040	0.74	12-48	3,400	0.95	72
	15	080	0.56	45-182	450	0.31	260
	20	080	0.74	32-128	760	0.39	190
	30	080	1.12	18-74	1,800	0.64	110
	40	080	1.46	12-48	3,400	0.95	72
430 SS	10	062	0.25	250-1000	500	0.11	3,300
	20	062	0.50	110-440	1,100	0.16	1,500
	40	062	1.00	41-160	5,100	0.39	600
	10	125	0.50	250-1000	500	0.11	3,300
	20	125	1.00	110-440	1,100	0.16	1,500
	40	125	2.00	41-150	5,100	0.39	600
	10	250	1.00	250-1000	500	0.11	3,300
	20	250	2.00	110-440	1,100	0.16	1,500
	40	250	4.00	41-160	5,100	0.39	600

Table 5.2.1 Feltmetal Property Data

5.2.3 The Heat Transfer Model

In this first wall heat exchanger of the blanket module, there are three principal zones of energy transfer as illustrated in Fig. 5.2.3.

Fig. 5.2.3

Zone 1, where, as the enlarged view of this area shows, the energy is distributed as:

- (a) Internal heat generation in the coolant fluid and in the coolant tube $\dot{q}_c$ and $\dot{q}_t$.
- (b) Internal heat generation in the metallic feltmetal and in the membrane $\dot{q}_f$ and $\dot{q}_m$.

Zones 2 and 3 are substantially less complicated and energy sensitive than Zone 1. Energy transfer in Zone 3 is solely by condensing sodium vapor on the membrane surface. Zone 2 has energy transfer by direct contact with the high temperature Li-Na bath and exponentially decreasing internal heat generation from r_1 to r_2 , the inner and outer radius of the blanket.

We will characterize the thermal hydraulics, the heat transfer and the structural stresses of Zone 1 since it is the more important area.

5.2.4 Wall Loading and Initial Assumptions

The energy input into any one blanket module per unit of area is determined by the neutron wall loading, W_L in MW/m^2 . For many reasons the wall loading is a very important parameter in reactor design. It strongly influences economics, radiation damage, the time between assembly and disassembly, the heat transfer, the temperature distribution, the temperature gradients and the structural stresses that are produced in the module. Therefore, we will use W_L as a control parameter and write equations in terms of it.

For tandem mirror reactor applications we will assume that all of the alpha energy, q_α , as charged particle energy, exits the reactor's central cell and ends up at the direct convertor and thus deposits no energy on the half tube

surface of the cooled container tube structure facing the plasma. Also, any radiation energy component (synchrotron or bremsstrahlung radiation) is neglected.

The functional relationship between the alpha flux, q_α , and the wall loading term, W_L , comes from the basic D-T fusion reaction:

Since wall loading is defined in terms of neutron flux, q_n , the alpha flux, q_α , in terms of units of wall load is:

$$q_\alpha = \frac{3.5}{14.1} W_L = 0.25 W_L \quad (2)$$

The total flux produced within the plasma is the sum of the neutron and alpha fluxes and therefore in units of wall load must be:

$$\begin{aligned} q_n + q_\alpha &= (1.0 W_{LN} + 0.25 W_{LCP}) \\ &= (1.25 W_L) \end{aligned} \quad (3)$$

There is an energy multiplication, M , associated with fusion reactors and this produces an additional quantity of energy in the blanket volume from exothermic reactions of the neutrons with the lithium. When this is taken into

account the total flux of the reactor per unit of wall area is:

$$q_{\text{total}} = (1.0 W_L M + 0.25 W_L) \quad (4)$$

The value of M is about 1.1 - 1.2 for lithium compound blankets without neutron multipliers. Our initial neutronic runs indicate a value of 1.05 with a thin blanket. Using the intermediate of the two values:

$$q_{\text{total}} = (1.1 W_L + 0.25 W_L) \quad (5)$$

This quantity of flux, q_{total} , is unfortunately not all available for use in our hydrogen production process and must be reduced by the amount of power required to drive the reactor and operate its ancillary equipment. (See Section 7 and 8 on reactor energy balance for details on circulating power, etc.) For our scoping design we have (arbitrarily) dedicated all of the high voltage dc output of the direct convertor to provide the power to drive the reactor and its support equipment. This requirement is satisfied at a reactor Q of about 10-15. This Q is the energy amplification of our driven tandem mirror reactor machine and is equal to the fusion power output divided by the injected power. Details of Q are discussed in the subsection on reactor energy balance in the physics Section 4 and in Subsection 8.5.2 and Section 7.9. The efficiency of the reactor direct convertor, η_{dc} , at producing this dc power component to drive the reactor can be estimated to be 50%. Thus the energy output available from all energy

sources of the reactor which we may use to drive the chemical plant is:

$$Q_{ava1} = \text{Blanket Thermal Power} + \text{Dir Conv. Thermal Power}$$

$$\begin{aligned} Q_{ava1} &= (1.2 W_L + 0.25(1-\eta_{dc})W_L) A \\ &= 1.325 W_L A \end{aligned} \quad (6)$$

where A is the total surface area of the first wall facing the plasma.

The direct convertor heat exchanger, DCHX, whose design is deferred to a subsequent report, can supply only a fraction, g, of q_{ava1} to the chemical process:

$$g = \frac{(1 - \eta_{dc})}{1.325} = 0.0943 (q_{ava1}) \quad (7)$$

or about 10%.

The corresponding percentage removed by the FWHX as energy from the blanket structure is held to about 8%. This small percentage is chosen because there is no point in degrading the quality of the energy in the pool from 1200 K down to lesser values when there is no thermodynamic need to do so. The minimum is fixed by the neutronics. The maximum is at our control.

5.2.5 The Physical Model of the Heat Exchanger

We have taken as a model for Zone 1 heat transfer a set of adjoining, round tubes of outside dimension d , that face the plasma and are included together to form the container assembly. On the other side of this tube array there is the feltmetal of thickness t and then a membrane of thickness δ . Figure 5.2.4 illustrates the arrangement. We want to conserve the quantity and the quality of the high temperature, Cauldron pool boiling energy source (The Cauldron fluid is at 1200K) but also must retain the steep temperature gradient between the hot pool and its container so as to retain the cool container concept. To do this we will not divert any of the energy from the LiNa pool but instead rely on only the energy deposition in the membrane and in the feltmetal to establish the gradient from the pool side of the membrane to the cooler tube wall (700K) of the FWHX.

Fig. 5.2.4 Section through the first wall showing the arrangement of coolant tubes, feltmetal insulation for establishing the temperature gradient and the membrane that separates the LiNa bath from the first wall. Typical dimensions: $d=2-4$ cm, $t=0.5$ cm, $\delta=0.2$ cm.

It can be observed from Figure 5.2.4 that a single tube width, d , is the smallest dimension that can be used for heat transfer analysis. The tube length will be taken as L . For this particular point design the first wall radius is 1.4-1.5m. The length of L that lies in the Zone 1 area of heat transfer is route dependent. For calculation of the important Hartmann pressure gradients created in liquid metal coolant flow (caused by the influence of the magnetic field perpendicular to the flow path) there is also a tube length L' in the magnetic field zone that is some multiple of L . The routing chosen for our coolant tube heat transfer analysis was shown in Figure 5.2.3. The length of L , in the heat transfer area, is assumed to be two meters. The value of L' for MHD pressure drop calculations is taken to be 3.2 meters. This 3.2 meters is the distance the coolant flows perpendicular to the B field.

5.2.6 The Neutronics of the Cauldron - Energy Deposition and Generation

From our preliminary attenuation data and neutronic runs for a representative Li-Na blanket module having equal volumes of the two fluids, we have initially established that the internal heat generation, the volumetric heating, as a function of radial position, y , in the Li-Na pool volume is:

$$P(y) \approx 2.24 \exp\left(\frac{-y}{0.3796}\right) \text{ MW/m}^3 \quad (8)$$

when the wall loading W_L is 1.0 MW/m^2 .

Furthermore, the internal heat generation in the structural materials (close to the first wall) is shown in Table 5.2.2 for the tube wall, the feltmetal and the membrane. For the first wall heat transfer analysis all we need are the data given in Table 5.2.2 A detailed discussion of the overall neutronics will be found in Subsection 5.5.

First Wall Coolant	Tube dia m	Volumetric Heating $w/m^3 \times 10^6$				
		Front Half of Tube	Coolant	Back Half of Tube	Felt Metal*	Membrane
Na	0.04	12.4	1.49	10.6	2.86	7.0
He	0.04	12.2	0	11.1	2.99	7.33
Na	0.02	12.5	1.59	11.6	3.2	7.82
He	0.02	12.5	0	11.9	3.29	8.04

Table 5.2.2 Volumetric heating in materials close to the first wall when wall loading is 1.0 MW/m^2 .

*Based on feltmetal porosity of 60%.

5.2.7 Magnet Field Effects on Heat Transfer in the Coolant Tubes

In addition to the Reynolds number Re , the Prandtl number Pr , and the Nusselt number Nu that are commonly used in the conventional heat transfer calculations there is another dimensionless number called the Hartmann number, H , used when magnetic fields are involved. This number is of high

importance in fusion reactors with their confining magnetic fields. It will be seen that this number dominates the character of the flow in the first wall heat exchanger when electrically conducting fluids such as sodium are chosen as the coolant.

The Hartmann number is a measure of the ratio of magnetic forces to viscous forces. It is defined as:

$$H = \left| a B_{\perp} \sqrt{\frac{\sigma_f}{\mu}} \right| \quad (9)$$

where

a = tube inner radius

$B_{\perp}$ = the transverse component of the magnetic field

μ = fluid viscosity

σ_f = fluid electrical conductivity

In magneto-hydrodynamic (MHD) flow in tubes there is also a conductance ratio, C , defined as:

$$C = \frac{\sigma_w t_w}{\sigma_f a} \quad (10)$$

where the new terms are:

σ_w = tube wall electrical conductivity

t_w = effective tube wall thickness

The parameter C , along with the Hartmann number, plays a dominant role in determining magnetically induced pressure drops in liquid metal flow in tubes. The Hartmann number, in addition to its effect on pressure drop, also influences the character of the fluid flow by suppressing turbulence and thus modifying heat transfer. The familiar transition Reynolds number, where flow changes from laminar to turbulent, is raised from its conventional value of ~ 2300 to much higher values when B fields and conducting fluids are involved. For circular pipes, the required Hartmann number to cause the flow to be fully laminarized is given by:

$$H \geq \frac{Re}{500} \quad (11)$$

Our study cases for sodium as a coolant flowing in the first wall tubes at high Reynolds numbers produce Hartmann numbers ranging from 1000 to 4000 and will be such that turbulence will be completely suppressed and flow will be in the laminar regime. For high Hartmann values, the Nusselt number (hd/k), for constant wall heat flux in round tubes, can be taken to be ~ 7.0 as suggested by Figure 5.2.5. The very important Nusselt number determines the temperature drop across the thin film existing between tube wall and coolant. Higher values of film drop will be produced in Hartmann flow than would be predicted for conventional turbulent flow.

Fig. 5.2.5 Average Nusselt numbers as a function of the Hartmann Number. Dashed line indicates suggested interpolation between the high and low Hartmann number solutions

5.2.8 The Coolants

As will be seen in the illustrative examples that will follow, magnetic field effects can dominate in determining heat transfer effectiveness, pressure gradients, and stress levels in this first wall heat exchanger when sodium or a similar conducting fluid is the coolant. For purposes of comparison we have included helium and an organic fluid as possible coolants in addition to our preliminary choice of sodium. These two alternatives avoid the MHD problems of the liquid metal but introduce other considerations.

The organic coolant may be one of the several polyphenyls available. The one ultimately used must be selected with consideration given to appropriate stability in a radiation field, in addition to good heat transfer characteristics. No specific choice has been made.

In terms of stability and compatability, helium is the coolant with the most potential, but characteristically it must be used as a high pressure gas (50 atm. is typical). Film temperature drops are substantially higher with gases than with fluids and it is inherently difficult to show that the high pressure, high velocity helium can remove the heat from the first wall, unequally heated coolant tubes without imposing either high combined total stresses or high pumping power.

Helium and the organics as coolants are particularly interesting comparisons because they are at almost opposite poles - one inert but operating at high pressure, the organic is reactive, more fragile, and subject to dissociation but able to operate at low pressure. The sodium falls somewhere in between. In a sense, as we shall see, sodium and helium are also opposites when cooling tubes are heated from one side only and when B fields are considered.

5.2.9 Keeping the Structural Container Cool

As we have already noted, a key element for the success of the cauldron blanket module idea is the ability to keep the

structural container cool compared to the hot fluid it is designed to contain. We require this structural container material to be cool enough so that strength, creep stress, etc. need not be a dominant design consideration. At the same time the structural container should be hot enough to thermodynamically satisfy a small portion of the process chemistry requirements.

The nominal temperature that we have chosen for the structure is $\sim 700\text{K}$ or 425C . This is not uncommonly high and is well within current technology for materials. The gradient we will establish between the structure and the LiNa bath is therefore 500K . This gradient is obtained by the use of a commercial product called FELTMETAL. This is the fibrous structure previously mentioned that can be tailored by fiber size and density to provide very low values of effective thermal conductivity.

For the "cool container" we have created an adiabatic surface at the interface between the membrane and the feltmetal as shown in Figure 5.2.6. Thus all the internal energy generation in the feltmetal flows to the FWHX and all energy in the membrane and in the pool goes to the CVHX. The internal heat generation in the feltmetal $\dot{q}$ establishes the desired temperature gradient which we have set at 500°K . The gradient can be established by the equation

$$\frac{d^2t}{dx^2} + \frac{\dot{q}}{k_e} = 0 \quad (12)$$

Fig. 5.2.6 Establishing the Temperature Gradient Across the Feltmetal

Upon integration the solution of this equation is found to be:

$$t = -\frac{\dot{q}x^2}{k_e} + C_1x + C_2 \quad (13)$$

and

$$t_2 - t_1 = \frac{\dot{q}L^2}{2k_e} \quad (14)$$

For example when

$$\dot{q} = 7 \times 10^6 \text{ W/m}^3$$

$$L = 0.005 \text{ m}$$

$$\text{and } t_2 - t_1 = 500 \text{ K}$$

$$k_e = 0.175 \frac{\text{W}}{\text{mK}} \quad (15)$$

5.2.10 Equations for Heat Transfer Calculations

The equations that are used for heat transfer calculations may be found in the appendix. The equations are applicable to all three coolant possibilities: sodium, helium or the polyphenyl. Where there are differences, they are so indicated.

5.2.11 Heat Transfer Results and Conclusions

In our heat transfer analysis we have used the input parameters, the input data and output data listed in the following Table 5.2.3 in order to survey a large number of possible operating combinations for the first wall. We have selected those combinations that illustrate three main situations: (1) acceptable combinations, (2) trends and (3) what output data are most important. These data are plotted in the series of Figures 5.2.7 through 5.2.10.

<u>INPUT PARAMETER</u>	<u>INPUT DATA</u>	<u>OUTPUT DATA</u>
Wall Load, W_L Tube Dia, d Temp Rise T_L Helium Press.	Tube Wall Thickness Spec. Heat Membrane Thick Density Viscosity B Field Elec. Cond. of Fluid	Q in Coolant Hartmann No. Q in Tube Press Drop Q Feltmetal Pumping Power Q Membrane % Pump Power Q total Al Film Coeff. Mass Flow rate ΔT Film Flow Vel. ΔT Wall Reynolds No.

Table 5.2.3 Heat Transfer Input and Output Parameters

5.2.11.1 Sodium as a Coolant

In the case of sodium as a coolant, where a substantial part of the flow must be across the 2 Tesla B field of the central cell, the most important output information was found to be the pressure drop created by the MHD effects of the magnetic field as shown in Figure 5.2.7. To keep this "cool container" concept in the desired regime of being a "low pressure" vessel it will be necessary to allow the sodium coolant temperature rise, $\Delta T_L = (T_{out} - T_{in})$, to be fairly high or, in other words, to allow the flow velocity to be very low. Figure 5.2.7 compares two temperature rises of 50 and 200K. Clearly a low ΔT_L causes high velocity flow which in turn exacerbates the MHD induced pressure drop. From the figure an acceptable operating case would be in using tube diameters of 3 or 4 centimeters with the 200K value for ΔT_L . In Fig. 5.2.8 we have plotted the pressure drop vs tube diameter for our design point wall load of 2.5 MW/m^2 . At 2 centimeters tube diameter or greater the pressure effect is substantially flattened out. Pressure drops as low as 7 atmospheres are achievable. At small tube diameters of 1 centimeter or less the slope of the curve is very steep and this is an area to be avoided. Smaller diameter tubes are preferable so as to maximize the production of high quality 1200K process heat. That is, we wish to minimize sodium volume in the first wall tube. Reviewing Figures 5.2.7 and 5.2.8 in that light, a tentative design choice would be 2 cm diameter tubes.

5.2.11.2 Helium as the Coolant

Helium, as a coolant, behaves dramatically differently than does sodium in a B field. The two most important differences in using helium in this application instead of sodium are: (1) the need to maintain high flow velocity instead of low (2) keeping the helium pressure as high as possible. The helium flow velocity must be high so as to minimize film temperature drops which will be seen to be critically important in the next section on stress analysis. We will show that helium flow in a tube heated unequally from one side only creates a stress limited design. The helium pressure must be as high as possible to minimize pumping power. Figure 5.2.9 illustrates the large range of film temperature drops calculated as a function of ΔT_L and tube diameter. For helium, the temperature rise, ΔT_L , should be low (just the opposite from the sodium) and the tube diameters should be small as possible (again just the opposite of the sodium). One or two centimeter diameter tubing and a 50 K temperature rise is an appropriate selection.

Pumping power as a function of wall loading and helium pressure for flow in a 2 centimeter diameter coolant tube is shown in Figure 5.2.10. At our design point of 2.5 MW/m² the pumping power ranges from a low of three fourths of one percent at a helium pressure of 60 atmospheres to a high in excess of 100% of the power handled if the pressure were as low as 5 atmospheres. On the basis of the need to keep the helium at pressures

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Fig. 5.2.9

Fig. 5.2.10

greater than 30 or 40 atmospheres due to pumping power we would elect to make sodium our first choice. This choice will be reinforced in the section on stress analysis. However, helium should not be summarily dismissed because of these problems. Its very important features of inertness and safety must be considered.

5.2.12 General Comments on Stress Analysis Applicable to the First Wall Heat Exchanger

The stresses analysis in assemblies such as the first wall heat exchanger are complicated by final overall geometry, by fabrication methods, by assembly/disassembly and other practical considerations. The FWHX is a relatively simple structure, at least in concept. A one-dimensional stress analysis is valuable for our preliminary assessment to see where acceptable operating regimes exist and to see how high stresses in a single tube are likely to be. The analysis provided here treats only the flow in a single tube and calculates the single tube stresses assuming that the contiguous tubes have no influencing effect. Obviously much more elaborate calculations eventually will be necessary as will be the building and testing of sub-scale and full scale models.

The three stresses to be calculated are:

- 1) the diametral stress, σ_B , dealing with tube bending due to unequal heating of the tube from front to back; that is, across the tube diameter.

- 2) the local thermal stresses in the tube wall, σ_T
- 3) pressure stresses from the coolant flow, σ_H and σ_L ; the hoop and longitudinal stresses.

5.2.13 Deformation and Bending Stresses in Nonuniformly Heated Tubes

The equations used for calculation of these stresses may be found in the appendices.

5.2.14 The Stress Levels for the FWX

The combined stresses; bending, thermal and longitudinal stress for a single tube heated from one side only are plotted in Figure 5.2.11 as a function of wall loading and temperature rise in the coolant from inlet to outlet for a 2 meter unequally heated tube length. Only the data for the helium are plotted. The major component of the stress is that due to bending caused by the characteristically high film temperature drops associated with gas flow. Had the tube been heated uniformly around its circumference rather than from one side only the bending stress due to diametral temperature difference would have vanished. From the Figure 5.24 it is clear that small diameter tubes and a small coolant temperature rise are necessary to keep the stresses at acceptable levels. A reasonable first choice might be 2 centimeter diameter tubes and a 50K temperature rise. So for different reasons helium and sodium both require about the same tube diameter.

For sodium and the organic liquid coolants the film temperature drop is very small, the MHD induced pressure head requirement for sodium is also small (~ 10 atm from Figure 5.2.7) compared to 40 or 50 atmospheres for helium. With these two liquid coolants bending stress is not a problem. It remains to be decided from external considerations; safety, dissociation of the organic coolant, fraction of total energy handled by the FWHX ($\sim 5\%$), shutdown, preheating to liquify, dumptanks for sodium disassembly etc., which coolant is the proper choice.

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Fig. 5.2.11 Stresses in a coolant tube as a function of tube diameter and wall load.

5.3 The Condensing Vapor Heat Exchanger

5.3.1 Introduction

As was mentioned in section 5.1.1 the high vapor velocities of lithium force attention to a variation of the concept of pure lithium boilers of Lee or Pendergrass. Our new concept uses binary mixtures of lithium and other liquid metals that have more favorable (higher) vapor densities at our design temperatures ($\sim 1200^{\circ}\text{K}$). We have considered both sodium and potassium to blend with the lithium and selected sodium as our first choice because the solubility of potassium in lithium may be limited whereas sodium and lithium are miscible. However, we certainly do not preclude potassium. Thus for the condensing vapor heat exchanger a binary mixture of LiNa is provided in the pool of the Cauldron blanket module. The energy input from the plasma heats this fluid and causes the sodium to preferentially vaporize leaving behind the lithium in the liquid state. The sodium condenses on the heat transfer tubes in the dome of the cauldron, energy transfer is effected and the sodium returns as liquid droplets to the pool completing that cycle. It should be noted that this internal loop of vapor transport and liquid droplet return is free from MHD effects from the magnetic field.

This section 5.3 discusses the preliminary design considerations of the heat exchanger located in the dome of the cauldron module. This heat exchanger, physically outside of the confining magnetic field, is, as the vapor and condensate flow, free of MHD effects. We have designated this unit as the condensing vapor heat exchanger (CVHX).

5.3.2

The simplified schematic of the coolant flow circuit is as indicated in Fig. 5.3.1.

Fig. 5.3.1 Coolant Flow Schematic

The boxed area within the dashed lines will form the basis of our discussion. Two coolants will be evaluated:

- (a) Helium
(b) Sodium

For analysis of flow and heat transfer in a single tube of the heat exchanger we provide the input parameters shown in the appendix.

The output data we wish to obtain and the appropriate equations for determining these data are also provided in the appendix to this section.

5.3.3 Single Tube Heat Transfer Analysis - Helium Coolant

We have used surface flux, Φ , the energy deposition per unit of coolant tube surface area, as a principal criterion of performance of a single tube. For helium the practical range of this flux must be: (a) at least $10 \times 10^4 \text{ w/m}^2$ to assure that there is adequate room for the total heat exchanger to fit in the dome of the cauldron and (b) less than $50 \times 10^4 \text{ w/m}^2$ because of heat transfer limits with gas coolants. Implicit in the lower range of flux is that it be compatible with wall loading up to 5 MW/m^2 ; that is, sufficient space be available. The design point wall load is 2.5 MW/m^2 , including some safety margin. The physics model requires $\sim 2.0 \text{ MW/m}^2$.

There are four main factors that we use to establish an acceptable operating regime for the helium. These are:

1. film temperature drops that we would like to limit to well under 100 K. The smaller the ΔT the more effectively we have delivered process heat to the chemical plant.
2. percent pumping power that should not exceed 2% - 3%.
3. flow velocities that should be roughly less than $0.25 \times$ sonic velocity. At 1200K the sonic velocity is $\sim 2000 \text{ m/s}$.

4. temperature rise of the coolant, inlet to outlet of the exchanger, that should be at least 50 or preferably 100 degrees minimum to maintain mass flow rates to the external chemical processors that are acceptable (see Section 7 on interfacing).

Data for helium as the coolant are plotted in Fig. 5.3.2 through 5.3.5 using tube diameters from 1 to 4 centimeters. Some conclusions may be reached:

- Tube diameters of 3 and 4 centimeters are too large because of the excessive film temperature drop (Fig. 5.3.2).
- Tube diameters of 1 centimeter are too small due to the large pumping power demands (Fig. 5.3.3).
- At low pressure (10 atm) flow velocities are excessive for small diameter tubes. (Fig. 5.3.4)
- There is a window, at tube diameters around 2 centimeters, where operation is acceptable. However, the helium pressure should be high, ~50 atm.
- Surface flux, ϕ , should be limited to $\sim 20 \times 10^4 \text{ W/m}^2$.

It is difficult to make a unqualified case for the use of helium in this particular application where high pressure of the coolant is mandatory. The principal problem is not in the flow and heat transfer but in the extreme sensitivity that creep stress and creep rupture have to

Fig. 5.3.2

Fig. 5.3.3

Fig. 5.3.4

Fig. 5.3.5

incremental temperature change at these high temperatures. It has been demonstrated that at a nominal 1200K an increase of only 50K can decrease the creep strength of super alloys such as Inconel 617 or Hastalloy by an order of magnitude. It is also evident that the allowable creep stress at 1200K for these super alloys cannot be very high in the first place.

We cannot diminish this temperature requirement and operate at say 100 degrees less because the effectiveness of the chemical processors would be seriously jeopardized. Section 8 discusses process heat temperature needs. The effect of temperature as a parameter for process units such as the SO_3 decomposer is discussed in Section 9. It is conceivable to consider operating the cauldron module at a more moderate temperature, perhaps 1000K, and using an afterheater at the chemical plant itself to pick up the lost 200K. Burning some of the hydrogen product would be one way to provide the afterheating. This concept may have some merit and is discussed in Section 7. Another solution when retaining the 1200K nominal temperature is to use the molybdenum alloy, TZM, as the material for the heat exchanger tubes. The creep behavior is highly adequate at this temperature. Inconel 617 is also possible. All appropriate materials and their use in the Cauldron are discussed in detail in Section 5.6.

Our original choice was to use sodium as the coolant in this CVHX in which case film temperature losses become very small and pressures can be substantially balanced between the Cauldron sodium vapor pressure and the sodium flowing in the heat exchanger tubes.

5.3.4 Single Tube Heat Transfer Analysis - Sodium Coolant

For sodium the surface flux, ϕ , the energy deposition per unit of coolant tube surface area, can be substantially higher than that achievable with helium. The sodium also need not be at high pressure since the condensing vapor heat exchanger is physically outside of the influence of the B field and MHD effects are not encountered. There are no Hartmann pressure gradients. As a heat transfer fluid sodium is highly adequate.

The caveat in the successful use of sodium lies not in its heat transfer qualities but in handling this high temperature fluid in a safe way. There are large volumetric quantities involved, large mass flow rates and substantial distances to traverse between the CVHX energy source and the energy receptors at the chemical process plant. For the SO_3 decomposer alone, which requires about 20% of the reactor thermal output, mass flow rates in the thousands of kilograms per second over distances of about 100 meters must be achieved. At the SO_3 decomposer the sodium is on the tube side of what is, in effect, a heat exchanger with SO_3 vapor on the shell side. It is evident that the SO_3 vapor and the sodium cannot be allowed to mix because of the oxygen reacting with the sodium. Fortunately, pressure balancing can be introduced to minimize this possibility but it is a non-trivial problem.

Further study is needed - not limited to the CVHX itself but factoring in the transport of the coolant and factoring in the coolant - chemical process interactions. It is not clear at this point which coolant is the proper choice.

5.4 NEUTRONICS

5.4.1 Introduction

Most of the energy extracted from a D-T fusion plasma is in the form of neutron radiation. Conversion of neutron kinetic energy into useful thermal energy occurs in the blanket - the region immediately surrounding the first wall. The heat thus generated in the blanket must be removed by a heat transfer system for subsequent conversion and utilization. The process requirements of this synthetic fuel plant design include high temperature heat at approximately 1200 K. This elevated temperature severely limits structural material choices due to mechanical property limitations. The blanket materials are also subject to intense neutron and gamma radiation fields which contribute to mechanical degradation and limited component lifetimes.

The insignificance of naturally abundant tritium has required that all fusion plants be tritium self-sufficient. This necessity is quantified by requiring a tritium breeding ratio of greater than unity, where tritium breeding ratio T is defined as the number of tritons produced (in the blanket) per fusion event in the plasma. The breeding of tritium is accomplished by direct neutron interactions with lithium. These reactions are ${}^6\text{Li} (n, \alpha) {}^3\text{H}$ and ${}^7\text{Li} (n, n' \alpha) {}^3\text{H}$, both contributing to tritium production significantly over the neutron energy range in the blanket.

The transformation of neutron kinetic energy into thermal energy in the blanket is characterized by the blanket energy multiplication factor M . It is defined as the total energy produced in the blanket for each incident neutron divided by 14.1 MeV (the initial energy of the neutron).

In all fusion reactor designs the M value must be corrected for geometric effects before it is used in power balance calculations.

Blanket designs do not provide 100% "blanket coverage" for the plasma, i.e., some of the fusion neutrons escape (through neutral beam injector ports, for example) without interaction with the blanket. The effective blanket energy multiplication factor is, therefore, the product of M and the blanket coverage factor, the percentage of source neutrons entering the blanket. In the present work the blanket coverage factor has been assumed to be 0.99.

In some designs there may also be energy losses that result from energy deposition and removal at temperatures that are too low to be useful. In particular it is not possible in any design to recover all of the energy at the peak temperatures required in a synfuel plant. In some cases the overall design of the plant may be constrained by the fact that the fraction of the energy recoverable at the peak temperature is too small. This is not the case in any of the designs studied for this project. As noted below the energy fraction recoverable at the higher temperatures is relatively large, typically about 90%, and all of the remaining fraction can be efficiently utilized.

Another parameter of engineering importance describes the spatial distribution of heat in the blanket. The heating rate, or power density, H , depends on the neutron flux and material (type and density) in a given region. High power densities would correspond to regions where there are many nuclear reactions producing heat per unit volume. Such detailed power distribution data is required to perform the necessary thermal-hydraulic design. The computer code utilized in the study calculates power densities in eV/source neutron. These tallies are converted to MW/m^3 by accounting for the neutron flux incident to the first wall in terms of neutron wall loading (MW/m^2).

Another set of neutronic results which are essential to fusion reactor design are the radiation damage parameters. The effects

of radiation damage will ordinarily be limiting in two regions - the first wall region (roughly the intermost 10-20 cm) and the superconducting magnet.

The predicted limiting failure mechanisms for the first wall depend on the details of the design and generally involve considerable uncertainty. The radiation damage events which can contribute to first wall failures are hydrogen production, helium production, and atom displacements.

The types of radiation damage in the superconductor which have caused concern include resistivity increases in the copper stabilizer, changes in the critical current, and failure of the insulation. Recently most of the attention has been devoted to the last category and a widely used criterion is that the radiation dose to the superconductor insulation should not exceed 5×10^7 GY (5×10^9 rads¹). It is also possible that energy deposition in the superconductor can lead to prohibitive refrigeration costs in order to maintain the superconductor at liquid helium temperatures. Normally this problem will be avoided if the radiation damage criteria for the superconductor are satisfied.

5.4.2 Computational Methods

The computer code ONEDANT has been used to conduct all neutron transport calculations. This is the most recently developed version of the one-dimensional discrete ordinates neutron transport code ONETRAN². ONEDANT incorporates the synthetic diffusion approximation developed by Alcuiffe, and reduces computing time by a factor of two to ten with no compromise in accuracy³ relative to neutron transport calculations not incorporating the approximation, but otherwise identical. All calculations are performed with S_8 quadrature and utilize 30 neutron groups coupled with 12 gamma groups to facilitate neutron and gamma transport for heating calculations. The energy group structures are given in Table 5.5.1.

TABLE 5.4.1

NEUTRON AND GAMMA-RAY GROUP STRUCTURE

Neutron Group	E_c (low bdy)		$\bar{E}$		ΔE	
1	15.0	MeV	15.73	MeV	2.0	MeV
2	13.5	"	14.07	"	1.5	"
3	12.0	"	12.89	"	1.5	"
4	10.0	"	11.01	"	2.0	"
5	7.79	"	8.705	"	2.21	"
6	6.07	"	6.789	"	1.72	"
7	3.68	"	4.508	"	2.39	"
8	2.865	"	3.219	"	0.815	"
9	2.232	"	2.510	"	0.633	"
10	1.738	"	1.963	"	0.494	"
11	1.353	"	1.527	"	0.385	"
12	0.823	"	1.055	"	0.530	"
13	0.500	"	0.6487	"	0.323	"
14	0.303	"	0.3962	"	0.197	"
15	0.184	"	0.2363	"	0.119	"
16	0.0676	"	0.1115	"	0.1164	"
17	0.0248	"	0.0410	"	0.0428	"
18	0.00912	"	0.0150	"	0.91568	"
19	0.00335	"	0.0055	"	0.00577	"
20	0.001235	"	0.002034	"	0.002115	"
21	454	eV	748.9	eV	781	eV
22	167	"	275.3	"	287	"
23	61.4	"	101.3	"	105.6	"
24	22.6	"	37.3	"	38.8	"
25	.32	"	13.7	"	14.28	"
26	3.06	"	5.0	"	5.26	"
27	1.13	"	1.86	"	1.93	"
28	0.414	"	0.6839	"	0.716	"
29	0.152	"	0.2509	"	0.262	"
30	0.000139	"	0.0359	"	0.151861	"

Gamma Group

1	9.0 MeV	9.5 MeV	1.0 MeV
2	8.0 "	8.5 "	1.0 "
3	7.0 "	7.5 "	1.0 "
4	6.0 "	6.5 "	1.0 "
5	5.0 "	5.5 "	1.0 "
6	4.0 "	4.5 "	1.0 "
7	3.0 "	3.4 "	1.0 "
8	2.0 "	2.5 "	1.0 "
9	1.0 "	1.5 "	1.0 "
10	0.5 "	0.75 "	0.5 "
11	0.1 "	0.3 "	0.4 "
12	0.01 "	0.005 "	0.09 "

The group constants are of order P_3 and are obtained from the MATXS library by the TRANX code⁴. A similar translation produces the neutron and gamma kerma factors utilized in the heating edits. MATXS library data originates from ENDF IV/V files. All neutronics calculations have been performed on the CDC 7600 computer at the Magnetic Fusion Energy Computer Center (FMCC) at Lawrence Livermore Laboratory.

Because the ONEDANT code employs a one-dimensional transport process, cylindrical modelling schemes are necessary to represent some two-dimensional features in a format representation. The most common example is that of cooling tubes having an axis running perpendicular to the radial direction. The general rule followed in such cases is to preserve both mass and volume. A zone is homogenized by reducing the density of each material to its average value over the entire zone. This requires that some results, such as power density, be subsequently corrected for any void fraction that may exist.

5.4.3 Blanket Neutronic Model

The motivation for and a detailed description of the Calderon Pool Boiler are provided elsewhere in this section. A representative one-dimensional model for neutronics calculations is shown in Fig. 5.4.1.

The most important variables for purposes of neutronics analysis are given in Table 5.4.2.

Fig. 5.4.1 ONE DIMENSIONAL MODEL OF CAULDRON (MODEL 3)

TABLE 5.4.2

NEUTRONICS DESIGN VARIABLES FOR THE CAULDRON POOL BOILER

<u>Variable</u>	<u>Range of Interest</u>
Li-Na bath mixture	Li conc. 40-70 w/o
1st wall coolant	He or Na
Coolant pipe diam. (1st W)	2-4 cm
Temp. for fluid density	700-1200 K

It is important to note that the Li-Na bath mixture has been assumed to be uniform and homogeneous. In fact, the heat transfer processes that occur, boiling in the bath and subsequent condensation on tube surfaces above the bath, will cause the bath mixture to have properties that are two-dimensional. Temperature variations (and, therefore, atom densities) and voids will be distributed in both the r and θ directions. Some analysis of the MHD effects on energy transport has been performed and is reported elsewhere in this section. It is anticipated that neutronics analysis of two-dimensional representations will be performed during the next year of this project. The MCNP Monte Carlo code⁵ has been obtained and made operational by the project group. A total of 175 different "cells" can be described with MCNP and this should be adequate to model the problem.

5.4.4 Analysis and Conclusions

Some of the more important results for five cauldron blankets operating with a Li/Na temperature of 1200 K are summarized in Table 5.4.3.

The tritium breeding ratio, T , is strongly dependent upon the Li concentration in the Li-Na blanket bath. For the configurations considered here a lithium concentration of 50% is adequate even

TABLE 5.4.3

NEUTRONIC PERFORMANCE SUMMARY OF CAULDRON MODELS

Case	Description	MODULE POWER								
		First Wall Tube O.D.	First Wall Coolant	T	M	High Temp. ^a Fract.	First Wall (MeV/n)	First Wall (MW) ^b	Cauldron (MeV/n)	Cauldron (MW) ^b
1	Li/Na = 60/40 w/o	4 cm	Na	1.34	---	---	---	---	---	---
2	Li/Na = 50/50 w/o	4 cm	Na	1.15	1.04	.84	2.4	6.0	12.7	31.2
3	Li/Na = 50/50 w/o	4 cm	He	1.18	1.03	.88	1.7	4.5	12.8	32.7
4	Li/Na = 50/50 w/o	2 cm	He	1.20	1.03	.92	1.2	3.0	13.4	34.2
5	Li/Na = 50/50 w/o	2 cm	Na	1.18	1.03	.90	1.5	3.7	13.1	33.5

a) The fraction of the power recovered at peak temperatures.

b) For a 400 cm long blanket module, 0.99 coverage factor, and a neutron wall loading of 1.0 MW/m^2

allowing for reductions in the ^7Li contribution which will occur when newer cross section data are used. Increased amount of first wall structure and the use of sodium vice helium as a first wall coolant are both changes which tend to reduce T. Neither of these variables produce large changes, however.

A particularly impressive feature of this design is the incorporation of tritium breeding into the high temperature region. This makes it possible to achieve values for the energy fraction recovered at high temperature which are considerably greater than those typically reported previously.^{6,7}

The power density in Case 3 is plotted as a function of radius in Fig. 5.4.2 for a neutron wall loading of 1 MW/m^2 with helium as the first wall coolant. These values are, of course, linear with wall loading and for 2 MW/m^2 the results in Fig. 5.3 should be doubled.

It is worth noting that the total power deposited in the superconductor represents a fraction of approximately 3×10^{-8} of the total. Thus the commonly accepted criterion of a maximum of 1×10^{-6} for this fraction can be satisfied with an even thinner shield than that used in these calculations. The shield must, however, also be thick enough so that radiation damage limitations for the superconductor can also be satisfied.

The hydrogen and helium gas production rates have been computed for the vanadium-titanium (10%) structure in the first wall complex. At a wall loading of 1 MW/m^2 the hydrogen production rate varies from 251-191 appm/yr and the corresponding values for helium are 58-41 appm/yr. These values agree quite well with those reported by ANL in Ref. (1). The ANL blanket design was different in terms of first wall radius and blanket composition, and the structural material considered was vanadium-chromium (15%)-titanium (5%). Nevertheless, for the same wall loading the hydrogen and helium production rates were 240 and 57 appm/yr, respectively.

Fig. 5.4.2 Energy Density Profile of Cauldron Blanket Module

Atom displacement cross sections were not available for vanadium and titanium. However, as shown by Gabriel, et al,⁸ vanadium, iron, nickel, and chromium have equal values of the effective displacement threshold energy, and the displacement cross sections for stainless steel and vanadium-titanium are similar. Atom displacement rates in a common first wall neutron spectrum for stainless steel and vanadium-titanium (20%) differed by less than 1% in Ref. (8). In the ANL study¹ the displacement rates for stainless steel and vanadium-chromium (15%)-titanium (5%) were equal. We have computed the atom displacement rates for stainless steel in the neutron spectrum corresponding to the actual composition of the cauldron blanket, including the first wall complex. The maximum displacement rate ($\dot{W}=1 \text{ MW/m}^2$) at the inner surface is 12.0 displ./atom yr, which can be compared with a value of 11.3 reported by ANL.

We have not attempted to estimate the lifetime of the first wall. However, it is worth noting that of six materials studied by ANL¹, the vanadium alloy at 600°C was superior to all others in terms of both swelling and ductility changes.

The adequacy of the blanket/shield combination for protection of the superconductor was evaluated in terms of the radiation dose to the superconductor insulation. The insulation was assumed to be G-10CR, an epoxy resin with SiO_2 added.⁹ For the Case 3 configuration and a wall loading of unity, the maximum dose rate to the superconductor insulation was found to be $2.4 \times 10^3 \text{ GY/yr}$ ($2.4 \times 10^5 \text{ rads/yr}$). If it is assumed that a total dose of $5 \times 10^7 \text{ GY}$ can be tolerated, this gives an essentially unlimited lifetime.

In summary, the Li/Na cauldron appears to be an attractive concept from a neutronics standpoint. Tritium breeding is adequate, the fraction of energy recovered at high temperatures is large, and no

significant problems have yet been identified. It appears likely that the shield thickness could be reduced without exceeding constraints on either superconductor heating or radiation damage to the superconductor insulation. In an economics optimization such reductions are likely to be very important. There may be two-dimensional effects in the Li/Na bath which are important and which will be studied in future work using Monte Carlo analyses.

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5.5 ESTIMATES OF MHD EFFECTS ON THE CAULDRON BLANKET CONCEPT

5.5.1 Introduction

The MHD (magnetohydrodynamic) effects on the cauldron blanket concept are very difficult to estimate at the present time. This is due to a combination of factors including:

- a) the complex pool geometry (see Fig. 5.5.1)
- b) the complex internal heat generation profile
- c) the limited nature of the experimental and theoretical studies of pool boiling phenomena in a magnetic field.

In order to get some idea of the probable importance of the MHD effects, we have concentrated on two simplified models:

Model #1: A rectangular cell filled with liquid with natural or free convection heat transport only.

Model #2: Pool boiling from a horizontal surface at the bottom of the liquid pool.

For each model, there are some limited experimental data and theoretical studies for our case with a horizontal magnetic field, B_H , which we can use as a basis for our estimates for this new TMR liquid pool blanket concept. In the following work, we have assumed a pure liquid sodium

Fig. 5.5.1 Isometric of the Cauldron Blanket Module

blanket for simplicity rather than the actual mixture of lithium and sodium. A very preliminary look at the effect of the mixture properties on the numerical results indicates that the principal conclusions remain essentially the same. However, a more detailed evaluation of this aspect is certainly required.

5.5.2 Model #1: Free Convection Cell

In the absence of magnetic field effects, the heat would be transported to the surface by free convection with perhaps some local nucleate boiling followed by bubble collapse not far from their place of birth. The resultant micro-convection would result in a sizable contribution to the heat transport with subsequent surface evaporation. However, there is a tendency for the MHD forces to suppress the free convection. The simple free convection model for which any information on the effects of a horizontal B_H field is available is that of Emery^(5.5.1). He studied the free convection heat transport across the rectangular cell illustrated in Fig. 5.5.2. In place of our near-exponentially varying internal heat generation profile in the liquid pool, Emery considered the classical case of a heat flux through the side wall on the left of Fig. 5.5.2. In place of our top surface evaporation, Emery has heat removal through the side wall on the right.

Emery developed a theoretical model to account for the MHD effects of a horizontal B_H field on the effective Nu of the cell which agreed fairly well with his liquid mercury

Fig. 5.5.2 Sketch of the boundary-layer development and temperature profile for free convection through a vertical fluid layer between a hot and cold wall according to Eckert.

Fig. 5.5.3 A comparison of the measured and predicted values of $Nu_c / (Nu_c)_{B=0}$ for the one-inch cell for $\Delta T_c = 19$ deg F.

data as shown in Fig. 5.5.3 (reproduced from Ref. 5.5.1). If we consider the limiting case of large B_H , Emery's equation for constant B_H field across the cell reduces to the following for the average Nusselt number in the middle of the cell:

$$\frac{Nu_c}{Nu_c(B=0)} \rightarrow \frac{0.965}{\sqrt{Y}} = \left[\frac{0.965}{\sigma_e B_H^2 / \rho_L \sqrt{g} \beta_L \Delta T_c / H} \right]^{1/2} \quad (1)$$

$$\text{where } Nu_c \equiv \frac{q'' W}{(T_H - T_C)k} = \frac{q'' W}{k \Delta T_c} \quad (2)$$

and $Nu_{c(B=0)}$ = Nusselt number for $B_H = 0$

Note that Emery's $Y = Ha^2 / \sqrt{Gr}$

where $Ha \equiv \text{Hartmann Number} = BW \sqrt{\frac{\sigma}{\mu}}$

and $Gr \equiv \text{Grashof Number} = g \beta_L W^3 \rho_L^2 \Delta T_c / \mu^2$

T_H and T_C are the hot (left) wall and cold (right) wall temperatures, respectively, and $\Delta T_c = T_H - T_C$ the cell temperature difference, σ_e is the liquid electrical conductivity, β_L is the liquid volume expansion coefficient, H is the cell height and W is the cell width (see Fig. 5.5.2). When Equation 1 predicts values of Nu_c below 1.0 for large B_H , Emery points out that we must limit the minimum Nu_c to 1.0, since this is the limiting case of pure conduction across the cell.

Fig. 5.5.4

We must first evaluate the Nusselt number for $B=0$. Shown in Fig. 5.5.4 (from Ref. 5.5.1) are typical experimental results for mercury and water in a vertical cell for $B=0$ along with Jacob's correlation. If we extrapolate the mercury data to a value of $Ra = Gr_{\text{w}} Pr \approx 1.5 \times 10^{10}$ which is typical of the sodium conditions at 1275 K in the reactor (see Table 5.5.1), we find a convection cell $Nu_{\text{c}}(B=0) = 100$.

For our pool-boiling blanket, we have chosen as representative values, $B_H = 2T$ and $H = 5$ m. For sodium at about 1275 K, we then estimate $\gamma \approx 1.14 \times 10^5$ for a minimum value of ΔT_c estimated as roughly $2 \times \Delta T_{\text{SAT}} \approx 10^\circ\text{C}$ and, $W = 2\text{m}$. This large value of γ yields values of Nu_{c} using Equation 1 well below unity, since $Nu_{\text{c}}(B=0)$ is typically on the order of 100 as mentioned above. This implies that $Nu_{\text{c}} \rightarrow 1.0$ for sodium for a horizontal magnetic field on the order of $2T$. This in turn implies that the large-scale free convection will be strongly inhibited by this strong B_H field. A recent study done independently (Ref. 5.5.1a) comes to a similar conclusion. When free convection is strongly inhibited, the liquid temperature will rise until nucleate boiling is sufficiently vigorous to transport the heat input to the free surface via vapor transport through the liquid.

We cannot rule out the possibility that some of the heat transport can still be due to micro-convection, since MHD forces are least effective in damping out the smallest scale eddies and convection cells. However, further experiments are necessary to clarify this question. For

the present, we will assume that nucleate boiling with the liquid pool at about the saturation temperature is the dominant heat transfer mechanism, and examine the MHD effects on this mechanism in the next section.

It should be emphasized that we have assumed that the near-exponentially varying internal heat generation profile can be approximated by an equivalent heat flux through the reactor first wall in our use of Emery's free convection cell results. This is clearly a rough first approximation, but the implications regarding the suppression of the large-scale free convection by the horizontal magnetic field are probably correct.

5.5.3 MODEL #2: Nucleate Boiling

We have found four references^(5.5.2, 5.5.3, 5.5.4, and 5.5.5) which bear directly on our problem of nucleate boiling in a horizontal B_H field. Of these references, only Ref. 5.5.5 is based upon experimental data for stable nucleate pool boiling, so we have chosen to use the semi-empirical equations developed in this work by Wagner and Lykoudis^(5.5.5) as the basis for a first estimate of the effect of B_H on the pool-boiling in the fusion blanket.

The nucleate boiling of mercury in the experiments of Ref. 5.5.5 was performed on a very carefully cleaned, polished and then sand-blasted horizontal surface placed at the bottom of a vertical tube; the general arrangement is shown

Fig. 5.5.5 Experimental Apparatus of Wagner and Lykoudis

in Fig. 5.5.5 (taken from Ref. 5.5.5). In contrast to this situation, our boiling will be produced by a non-uniform internal heat generation inside the volume of liquid as mentioned previously. It seems possible that we will have to provide some solid surfaces for nucleation sites in order to avoid explosive boiling or bumping in the pool. (This assumes that the sites produced by the neutron collisions and/or the tritium and helium gases produced by the neutron reactions in the liquid lithium are inadequate to act as the nucleation sites and that the helium gas sparging system proposed by J. D. Lee results in too high a void fraction.)

We are tentatively proposing the use of screens, wires and/or other surfaces appropriately placed throughout the liquid pool. For this situation, the experimental results of Ref. 5.5.5 will probably provide us with a reasonable first approximation. However, in order to apply the results of Ref. 5.5.5, we must imagine that there is an effective heat flux at the solid surfaces in the liquid pool due to the internal heat generation in an appropriate zone on either side of each nucleation surface, including the internal heat generation in the screens or wires themselves. Obviously, experiments will be required to verify that this way of approximating the nucleate pool boiling with internal heat generation is reasonable.

The semi-empirical equation proposed by Wagner and Lykoudis^(5.5.5) which agrees well with their experimental

results on mercury can be written in the ratio form:

$$\frac{Nu}{Nu_0} = \left[\frac{1}{1 + 17.5 P^{1.25} \sqrt{\Lambda}} \right]^{2.44} \quad (3)$$

where Nu_0 is the Nusselt number for $B=0$, P is the saturation pressure in MPa, and Λ is a new non-dimensional "boiling magnetic interaction parameter" discovered by Lykoudis^(5.5.4) which measures the effect of the MHD interaction on nucleate boiling:

$$\Lambda = B^2 \Delta T_{SAT} * (\sigma_e L^2 C_P^2 \rho_L^2 T_{SAT} / \rho_v^3 h_{fg}^3) \quad (4)$$

where

$$\Delta T_{SAT} = T_\infty - T_{SAT}$$

T_∞ is the superheated liquid temperature in the bulk of the liquid, σ_e is the liquid electrical conductivity, $\alpha_L = (k/\rho_L C_P)_L$ is the thermal diffusivity of the liquid, ρ_L and ρ_v are the liquid and vapor densities, respectively, h_{fg} is the latent heat, and C_P is the liquid specific heat.

For the mercury experiments at 1 atm pressure, we have checked the above equations numerically and find that $\Lambda = 0.171$ for their typical experimental values of $B_H = 1.26$ T and $\Delta T_{SAT} = 10^\circ\text{C}$. Using this value in the Nusselt number equation, we find $Nu/Nu_0 \approx 0.434$ for constant ΔT_{SAT} . The experimental results of Ref. 5.5.5 (reproduced

Fig. 5.5.6 Heat Flux vs Superheat for Mercury at a $P_{\text{sat}} = 760 \text{ mm Hg}$

here as Fig. 5.5.6) give $Nu/Nu_0 \approx 0.44$, which verifies that we are interpreting their equations correctly.

Applying these equations to the case of sodium at $T_{SAT} = 1000^\circ\text{C} = 1273\text{ K}$ ($P_{SAT} = 0.273\text{ MPa}$), we calculate:

$$\Lambda \approx 0.00529 B_H^2 \Delta T_{SAT} \quad (5)$$

A typical value of ΔT_{SAT} for sodium with $B_H = 0$ at effective heat fluxes on the order of 10^5 to 10^6 W/m^2 (about 3×10^4 to $3 \times 10^5\text{ Btu/hr-ft}^2$) and a pressure of 0.273 MPa (2044 mm Hg) should be no more than about 5°C (roughly 10°F). This is based on experimental results for pool boiling of sodium shown in Figs. 5.5.7a and 5.5.7b (taken from Ref. 5.5.6) and Figs. 5.5.8a and 5.5.8b (taken from Ref. 5.5.7). These four figures also serve to point up the large variations in the superheat required for different pressures (Fig. 5.5.7a), surface finishes (Fig. 5.5.7b) and experimental facilities and techniques (Fig. 5.5.7a compared to Figs. 5.5.8a and 5.5.8b) even with no magnetic field effects.

For this value of $\Delta T_{SAT} = 5^\circ\text{C}$ and $B_H = 2\text{ T}$, Equation 5 yields $\Lambda \approx 0.106$ and Equation (3) gives $Nu/Nu_0 \approx 0.16$. This is over a factor of six reduction in the overall heat transfer capability of the sodium pool at fixed ΔT_{SAT} . If we wish to maintain the same effective heat flux, then we must raise the liquid superheat, ΔT_{SAT} , by an amount

Fig. 5.5.7a Effect of pressure on nucleate pool boiling of sodium.

Fig. 5.5.7b Comprehensive plot of sodium heat transfer data at an average pressure of 65 mm.

Fig. 5.5.8a Measured heat-transfer rates during saturated pool boiling of sodium.

Fig. 5.5.8b Comparison of nucleate boiling results with a previous experiment and predicted values.

given by the following semi-empirical relation based on the equations in Ref. 5.5.5:

$$\frac{\Delta T_{\text{SAT}}}{(\Delta T_{\text{SAT}})_0} \approx \left[1 + (17.5 \text{ p}^{1.25} \sqrt{\Lambda}) \right]^{0.763} \approx 1.8 \quad (6)$$

If, in fact, we only require this small amount of additional superheat in the fusion reactor pool-boiling blanket, the effect of the 2 tesla magnetic field will probably not be a serious problem.

However, there is one potential problem area we must be aware of. It is conceivable that a large B_H coupled with a small effective heat flux could move the operating point from the Stable Nucleate Boiling Region III into the Unstable Nucleate Boiling Region II shown on Fig. 5.5.9 (from Ref. 5.5.3). If we use screens or some other solid surfaces, this implies that the spacing of these nucleation surfaces in the fusion reactor liquid pool may be critically important in obtaining high enough effective heat fluxes to avoid unstable nucleate boiling, but not so high a heat flux as to risk burnout.

5.5.4. Vapor Void Fraction Estimate

An estimate of the average vapor "void" fraction, α_v , is required for neutronics calculations. This can be estimated only if we know the fraction, f_{LH} of the total internal heat generation, $\dot{Q}_{INT}$, carried by the vapor

Fig. 5.5.9 Typical pool boiling curve.

bubbles as latent heat to the free surface. As a first rough estimate we can write:

$$\alpha_v \approx \frac{f_{LH} \bar{q}''' H}{\rho_v \bar{u}_v h_{fg}} \quad (7)$$

where $\bar{q}'''$ is the overall average internal heat generation rate ($\bar{q}''' = \dot{Q}_{INT}/\text{Volume of the pool}$), H is the liquid pool height and $\bar{u}_v$ is the average vapor bubble rise velocity.

For $B_H = 0$, Ivey^(5.5.8) says that we can estimate the average bubble velocity from:

$$\bar{u}_v \approx 1.18 (g g_c \sigma / \rho_L)^{1/4} \quad (8)$$

here g_c is the gravitational conversion factor (equal to 1.0 for SI units) and σ is the surface tension coefficient. For our sodium conditions, this yields $u_v \approx 0.23$ m/s, in reasonable agreement with the data for both air and steam bubbles in water given in Ref. 5.5.8 and for mercury vapor bubbles given in Ref. 5.5.9.

Ivey also indicates that the bubble diameter at departure from the nucleation site can be estimated from:

$$D_{ds} \approx \left[\frac{g_c \sigma}{g(\rho_L - \rho_v)} \right]^{1/2} \quad (9)$$

For our sodium conditions, this yields a departure diameter of about 3.8 mm.

In Ref. (5.5.9), Mori, et al., present experimental results on the effect of the equivalent bubble radius, R_m , at $B = 0$ (Fig. 5.5.10a) and in a horizontal magnetic field (Fig. 5.5.10b) on the bubble rise velocity for nucleate boiling of mercury. Extrapolation of their data shown in Fig. 5.5.10b indicates that at 2 Tesla (20 kgauss), the bubble rise velocity is reduced by about a factor of two for bubble diameters around 3 to 4 mm. We will assume that the reduction in velocity for sodium bubbles is no more than this at our conditions, since the magnetic interaction parameter, Λ , for sodium is somewhat less than the Λ for mercury at $B_H = 2T$ and the same ΔT_{SAT} .

Tong^(5.5.10) says on that typically the latent heat carried away from the heated surface by the bubbles is only about 2% of the total heat input in subcooled pool boiling with no magnetic field (i.e. $f_{LH} \approx 0.02$). We have found no reference on the effect of a B field on the percent of the heat input carried as latent heat, and Lykoudis^(5.5.11) says that this is a frontier problem in pool boiling of liquid metals.

In order to make a preliminary rough estimate of the average void fraction, we have solved Eq. 7 for $q'' = 1 \times 10^6 \text{ W/m}^2$ and $H = 5 \text{ m}$ for our sodium conditions:

$$\bar{\alpha}_v \approx 8.48 f_{LH} \left[\frac{\bar{u}_v (B \approx 0)}{\bar{u}_v} \right] \quad (10)$$

5.84

Fig. 5.5.10a Relation between U and R_m ($B=0$)

Fig. 5.5.10b Relation between U and R_m

For $B_H=0$, the void fraction for $f_{LH} = 0.02$ would be 0.17. If f_{LH} stays at 2% when B_H is raised to 2T, then the void fraction would roughly double to 0.34 due to the reduction in the average bubble velocity by about a factor of 2. However, this can only be considered a very rough lower bound. We will have to wait for the results of experiments to see how the B_H field affects the distribution of the heat transported by the various mechanisms operative in pool boiling and what f_{LH} actually is at $B_H=2T$ before we can make a more refined estimate of the void fraction.

5.5.5 CONCLUSIONS

Based on this initial look at the MHD effects on the cauldron blanket concept, we can reach the following preliminary conclusions:

- a) Large-scale free convection will be inhibited by a 2 tesla horizontal magnetic field. This will probably result in the heat transport from the liquid metal taking place primarily by nucleate boiling.
- b) Nucleate pool boiling is also inhibited by a horizontal magnetic field, although to a lesser extent than large-scale free convection.
- c) Based on the limited experimental and theoretical results available, it is estimated that the liquid superheat required to transport a given amount of heat

by nucleate boiling may be roughly doubled by the presence of a 2 tesla horizontal magnetic field.

- d) It is estimated that the vapor void fraction will also be roughly doubled by the 2 tesla field if the fraction of the heat transported by the latent heat remains the same as it was for the $B = 0$ case.
- e) The above nucleate boiling estimates for our case with non-uniform internal heat generation throughout the pool are based on analogies with the more common situation of a heat flux across the bottom surface of the liquid pool. We have assumed that nucleation screens or other surfaces may have to be appropriately distributed throughout the liquid in order to produce an effective surface heat flux in each zone of the liquid pool. This may be necessary to avoid bumping or other unstable boiling or burnout phenomena. Experimental verification of these models is clearly essential before we can actually design an advanced pool-boiling fusion reactor blanket with high confidence.

TABLE 5.5.1

Typical Sodium Property Data for the
Liquid Pool Blanket Conditions

Saturation temperature, T_{SAT}	1275 K (≈ 1000 C)
Saturation pressure, p	0.273 MPa
Latent heat, h_{fg}	3.77×10^6 J/kg

Saturated Liquid Properties

Density, ρ_L	720 kg/m ³
Specific heat, C_p	1280 J/kg-K
Thermal conductivity, k_L	48 W/m-k
Thermal diffusivity, α_L	5.21×10^{-5} m ² /s
Dynamic viscosity, μ_L	2.2×10^{-4} kg/m-s
Prandtl number, $Pr \equiv (\mu C_p / k)_L$	0.0059
Surface tension coeff., σ	0.10 N/m
Electrical conductivity, σ_e	1.58×10^6 mho/m
Volume expansion coeff., β_L	3.0×10^{-4} °C ⁻¹
Grashof number, $Gr_w = g \beta_L \Delta T_c W^3 / \nu_L^2$	2.52×10^{12} for $\Delta T_c = 10$ C

Saturated vapor properties

Density, ρ_v	0.68 kg/m ³
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5.6 Blanket Materials

The most important elements of the cauldron design from a materials standpoint are the following:

- o First wall portion of the structural wall
- o "Feltmetal" insulator
- o Cauldron wall
- o Condensing vapor heat exchanger
- o Tritium permeation membranes

Criteria for the selection of materials for these elements and the initial selections that have been made will be described in turn.

5.6.1 First Wall (Structural Wall)

The major considerations for selection of materials for the first wall are neutron activation and radiation damage effects. Hence, our current best choices for first wall materials are V-10% Ti and V-20% Ti, since both V and Ti show minimal neutron activation, and the V-10 Ti and V-20 Ti alloys suppress He gas bubble formation as contrasted with other V alloys.⁽¹⁾ We also anticipate that triton impact should not cause problems of localized tritium buildup in the first wall, since tritium should permeate very rapidly through V and its alloys. Recent work by Hisamatsu and Kanno has shown that hydrogen permeation rates are actually about ten times higher in V than in Nb at ~700 K, and second only to Pd or Pd-Ag alloys.⁽²⁾

In other considerations regarding the use of V for the first wall, we need to evaluate corrosion problems, fabrication, and material cost. Three coolants have been considered for the first wall, i.e., liquid Na, He, and organic liquids (polyphenyls) and the corrosion characteristics vary

according to the coolant. According to a compilation by Cowles and Pasternak,⁽³⁾ V shows good resistance to corrosion by liquid Li at ~1100 K, and because of the similarity in corrosion behavior by alkali metals in general, and the lower temperatures required here (~700 K), we anticipate good resistance to liquid Na or K as well. Helium should not present any corrosion problems. Nor do we anticipate corrosion with the liquid polyphenyls, but radiation damage can lead to carbon deposition in the tube passages unless the polyphenyls are continuously processed and purified to remove decomposition products that result from radiation damage.

The technology of producing V sheet or tubing and fabrication by electron beam welding is similar to that for Nb and Ta and presents no problems as long as interstitial O, N, and H are kept at low levels.⁽⁴⁾ In particular, O and N contents should not exceed 2000 ppm by weight, otherwise V will become brittle. Even this problem can be alleviated by alloying the V with Y.⁽⁵⁾

Historically, the demand for pure V has been relatively small; as a result, the production has been limited and the cost high. V resources, both in this country and in the world, are extremely large, but V does not normally occur as a pure ore and must be recovered as a byproduct of other metal manufacture - especially from titaniferous magnetite ores.⁽⁶⁾ Currently, the principal use of V is as an additive to steels, at a use rate of about 5000 Mg/year in the U.S. The current cost of pure V is \$110-130/kg in small quantities, and less than \$90/kg in large quantities. We anticipate that increased usage of V will lead to major reductions in its cost. Using a cost of \$90/kg gives an estimated raw material cost for V in the first wall (~25 Mg required) of about ten million dollars.

We are considering beta-Ti alloys as a backup material for the first wall. A clear judgment on this is yet premature because of the lack of sufficient information on the radiation damage-behavior of beta-Ti alloys. It is encouraging that the tritium (hydrogen) diffusivity in beta-Ti alloys is substantially higher than in alpha-Ti alloys,⁽⁷⁾ so that tritium concentration buildup within the material with subsequent degradation of physical/mechanical properties should be much less a problem in beta-Ti than in alpha-Ti. The beta structure can be stabilized by alloying additions of V, Cr, Fe, Mo, or Al.

A much broader range of conventional low-cost alloys (e.g., Fe-Ni based alloys) can be used for the structural wall outside of the first wall region, where neutron activation and radiation damage effects are greatly reduced.

5.6.2 "Feltmetal" Insulator

Requirements for the insulator between the outer structural wall and the cauldron wall are that it have good resistance to neutron activation and radiation damage in the first wall region, that it provide the necessary thermal insulation, that it have good compressive strength to support the cauldron wall, that it have low shear strength to provide for differential expansion of the inner and outer walls, and that it provide a porous channel for removal of tritium that permeates through the walls. The commercial fibrous metal insulator called "Feltmetal" seems to essentially meet all of these requirements for material with a bulk density of ~40% of theoretical. Further, it should be possible to fabricate Feltmetal from any of the ductile metals. From the standpoint of neutron activation, the best candidates are V and Ti, and because of the lower cost of Ti, it is the preferred material. Either a fully or partially beta-stabilized alloy should be used to maintain material

integrity over the full temperature range (the alpha-beta transformation in Ti occurs at 1155 K). The alloys Ti-13V-11Cr-3Al, Ti-2.5Al-16V, and Ti-7Al-4Mo are our current selections, based on existing commercial alloys. However, creep may be a problem with these alloys at the upper end of the temperature range where the Feltmetal is in contact with the cauldron wall. This can be alleviated by developing new Ti alloys specially designed for high temperature strength or by using a layered or graded structure in the Feltmetal, varying from a Nb alloy next to the cauldron wall to a Ti alloy next to the structural wall.

5.6.3 Cauldron Wall

For the cauldron wall, the main materials considerations are: neutron activation and radiation damage in the region near the plasma, corrosion and long-term creep strength for the entire wall region, and the existence of a fabrication technology for the cauldron structure. As with the Feltmetal, we have selected the beta-Ti alloys for the cauldron wall, to minimize neutron activation and radiation damage and to minimize the use of the more costly V.

Pure Ti metal shows good corrosion resistance to liquid Li at 1000-1200 K,⁽⁸⁾ and similar behavior can be expected with liquid Na or K. Alloying additions of V, Cr, Fe, or Mo to stabilize beta-Ti should not affect the corrosion resistance, since these elements also have excellent resistance to corrosion by alkali metals. Al, by itself, is reactive with the alkali metals, but when alloyed in low concentrations in Ti, can be expected to have a major reduction in reactivity because of a lowering of its chemical activity. Thus, we believe that a few percent Al is acceptable as an alloying additive.

It is difficult to fully assess the problems of long-term creep in beta-Ti alloys at ~1200 K at this time. Although the stress levels on the cauldron wall are quite low, there is no experimental creep data on beta-Ti alloys at the temperatures of interest. Short-term yield strengths generally remain quite high for Ti alloys at temperatures in the vicinity of 1200 K, e.g., for MST 881 a Ti-8Al-8Zr-1(Nb+Ta) alloy,⁽⁹⁾ the short-term tensile yield strength (0.2% deformation) is 33,200 psi (2.3×10^8 Pa) at 1200 K.

Fabrication and welding of both alpha and beta-Ti alloys into large complex structural units using electron beam welding techniques has been well established as a technology by the aerospace industries.⁽¹⁰⁾

5.6.4 Condensing Vapor Heat Exchanger

The condensing Na (or K) vapor heat exchanger at the top of the cauldron dome can be accommodated in two ways: (1) using heat exchanger tubes containing liquid Na or K or gaseous He, or (2) using heat pipes with Na as the working fluid. If heat pipes are used, a secondary loop would still be required with circulating Na, K, or He to carry the heat to the thermochemical plant. A high thermal conductivity, good corrosion resistance, creep resistance, and ease of fabrication are important considerations for the condensing vapor heat exchanger.

Molybdenum alloys, particularly TZM, have a distinct advantage in heat pipe and heat exchanger applications where high thermal conductivities are needed to minimize temperature drops. Molybdenum alloys also have high strengths and outstanding resistance to corrosion by liquid alkali metals.⁽⁸⁾ Small-scale (up to ~3 cm in dia.) welding of molybdenum heat pipes and laboratory test

capsules is well established, but large component welds are not currently part of our technology. We have, therefore, selected TZM as the best candidate for heat pipe or heat exchanger applications in the cauldron dome; however, it has low tritium permeability for the heat pipe application.

We are also considering Nb alloys, Ta alloys, and In-800 (or In-800H) as possible alternate materials. The Nb and Ta alloys have excellent corrosion resistance to the alkali metals, while that for In-800 may be marginal. The common Nb alloys, Nb-1Zr and Nb-10Ti-10Mo have markedly lower creep strengths than TZM. D-43, a developmental grade alloy of composition Nb-9W-1Zr, has a creep strength comparable to TZM,⁽¹¹⁾ and should be considered as a viable alternative if it goes into commercial production. TZM still has the advantage of a higher thermal conductivity. The Ta alloys T-111 (Ta-8W-2Hf) and T-222 (Ta-10W-2.5Hf) also have creep strengths comparable to TZM, but again are only developmental alloys. Fabrication of the Nb and Ta alloys should present no problems. An area of concern, however, is the presence of Zr and Hf in these materials, since they have an affinity for hydrogen that may lead to a holdup of tritium in the alloys. In-800 and In-800H are attractive because of their low cost, ease of fabrication, and good creep resistance for an Fe-Ni based alloy, although much lower than for TZM. Dropping the use temperature to 1100 K for In-800 greatly improves its creep strength and corrosion resistance properties. Thus, creep strength for 1% creep in 100,000 h becomes 200 psi $\pm$ 50% (1.4×10^7 Pa),⁽¹²⁾ and corrosion in liquid Na, as determined by Nevzorov for an alloy with a composition similar to In-800,⁽¹³⁾ was found to be 7.5×10^{-3} mg/cm² - h in a 300 h run. Conservatively assuming a linear corrosion rate gives 83 μ m corrosion in one year, while assuming a parabolic (diffusion-controlled) rate, gives 1.5 μ m corrosion in one

year. In either case, good corrosion resistance is indicated for In-800 at 1100 K.

5.6.5 Tritium Permeation Membranes

Tritium released from the boiling Li-Na pool will need to be recovered through several paths. A portion can be recovered from the Feltmetal channel, a portion will pass into the heat exchange loop or heat pipes, and a portion can be intentionally removed through permeation membranes in the vapor dome. Without pursuing design specifics, it should be clear that materials that have a high permeation rate for tritium and that will withstand corrosion by the liquid metal environment will be needed for tritium recovery in this system.

V alloys are the best choice for permeation membranes from the standpoint of tritium permeation rates.⁽²⁾ V also shows good resistance to corrosion by liquid Li at temperatures of ~1100 K,⁽³⁾ and we anticipate that it will be resistant to liquid Na and K as well. Other candidate materials are Nb and beta-Ti alloys. Nb alloys are to be preferred where high strength and low creep rates are required.

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5.7 TRITIUM AND HELIUM RECOVERY

5.7.1 Introduction

Two blanket types have been considered: (1) a Li-Na Cauldron boiler and (2) a lithium oxide microsphere moving bed. For both of these approaches, we have considered several aspects of current technology in handling the tritium. In this section we show, that either the Li_2O flowing blanket or the Cauldron pool boiler can be coupled to the Sulfur-Iodine thermochemical cycle using a sodium or a helium heat transfer loop. The tritium is generated within the blanket volume where its concentration is held to 10 ppm (atomic parts per million) by a scavenging system consisting mainly of metal permeation membranes. The tritium that gets into the heat transfer loop is held to 1 ppm in that loop by a scavenging system. In this design concept we have held blanket inventory to 1 kg tritium, kept losses to the thermochemical plant to 5 Ci/d, environmental losses to ~ 10 Ci/d, and held total plant inventory at risk to 7 kg tritium.

The general background on tritium--its present control technology, standards for safety, environmental guidelines, etc., together with the supporting technical bases for the results given in this section are given in the Supplement, available from the authors by request.

5.7.2 Design Criteria

Tritium control in the tandem mirror reactor (TMR) in this project was set to meet the goal of emissions to the environment below 10 Ci/d. The basis for this is discussed in the Supplement. Figure 5.7.1 summarizes our goals for the various tritium leak source terms and the associated processing loops.

The tritium bred in the blanket will permeate through the heat exchanger tubes in the Li-Na Cauldron (or from the Li_2O helium sweep gas) and eventually get into the sodium or helium loop feeding the Incoloy-800H SO_3 process units within the thermochemical plant.

We have set a limit of this leakage of tritium into the SO_3 decomposer at 5 Ci/d, or for 400,000 m^3/h of H_2 production based on a 3,000 MW_e reactor. This would produce a maximum of 0.5 $\mu\text{Ci}/\text{m}^3$ of HTO in the H_2 product, which approximates the permissible 0.4 $\mu\text{Ci}/\text{m}^3$ limit for a breathable atmosphere for the general public. It is also necessary that blanket module failure or sodium loop failures must not raise the tritium leakage into the thermochemical process to unacceptably high levels.

5.7.3 Lithium-Sodium Vapor-Space Processing System with a TZ In-Blanket Condenser

We outline several strategies for handling tritium in the Li-Na Cauldron blanket concept and illustrate the key results. Based on our earlier work⁽¹⁵⁾ with solid Li-aluminate blanket designs, the cases we consider are: Case A-1 -- TZM CVHX and Na loop permeator, Case A-2 -- Incoloy-800H CVHX and Na loop permeator, Case B -- TZM CVHX Na loop getter bed, Case C -- TZM heat pipes. Incoloy-800H and molybdenum alloy TZM are candidates for the tubes of the condensing vapor heat exchanger (CVHX). We have looked at two approaches for tritium removal: one using a niobium permeator, and the other using a metallic getter in a moving bed.

The relationship between the tritium concentration (i.e., inventory) in the Li-Na Cauldron and the tritium partial pressure must be established. There are no data that we know of covering hydrogen or its isotopes in the Li-Na binary system. Heuman and Solomon⁽²⁾ and Maroni and co-workers⁽³⁾ have measured partial pressures of tritium, deuterium, and hydrogen over Li from 973 to 1273K. Tritium exhibits a very different dependence of partial pressure, p , on concentration, x , than does either deuterium or hydrogen, but we believe the data⁽³⁾ indicate that Sievert's law ($p \propto x^2$) applies. Impurities in the system during the tritium measurements may have caused these results to be anomalous and recent data analyses⁽³⁾ indicate Sievert's law is to be preferred. Consequently, we have used Sievert's law as representing the base case situation:

$$p(\text{atm}) = (x/K_s)^2$$

where $K_s = 0.159 \text{ atm}^{-1/2}$ at 1273K and x = mole fraction of tritium.

If the total of all Cauldron modules has a 1256 m³ volume and contains 1 kg of tritium, we estimate the molar concentration of T at about 10 ppm. For this situation the partial pressure of tritium as T was calculated from Sievert's law as 4×10^{-9} atmospheres, assuming that all of the tritium is in the pure atomic species. The impurities, HT0, CH₃T, LiOT, NaOT, etc., are expected to be constituents in the heat transfer loop at relatively low levels and will be removed by the processors.

Available permeation data⁽⁴⁻⁹⁾ for various metals and alloys are shown in Figure 5.7.2. It is obvious for these applications at around 1223K that alloys low on the plot are preferred owing to their low tritium permeation.

5.7.4 Li-Na Cauldron: Case A-1 (FIGURE 5.7.3), TZM CVHX; Case A-2 (FIGURE 5.7.4), Incoloy-800H CVHX; Both with Incoloy-800H Process Heat Exchanger, and Na-Loop Permeator

In this section we outline (a) the Li-Na Cauldron vapor space tritium processing unit, and (b) then the main heat transfer loop tritium scavenging system. First we calculate the Li-Na Cauldron vapor scavenging system permeator area based on the use of small diameter niobium tubes with 0.4 mm thick walls. Low-flow helium sweep gas at 2 atm flows on the shell side at 1273K (which is maintained hotter than the Li-Na vapor in order to keep the surface clean). This permeator can be placed external to the blanket with the Li-Na vapor space gases circulated through the tubes. For a 3,000 MW_t system about 16 g/h of tritium will have to be recovered. With a permeation coefficient⁽⁴⁾ of 0.2 std cm³-mm/h-cm²-atm^{1/2}, a permeator area of 733 m² is required, assuming no degradation of surface activity from dirt and impurities.

FIGURE 5.7.2 Permeation of hydrogen through various alloys.

FIGURE 5.7.3 Lithium-sodium vapor-space processing system with a TZM condenser.

FIGURE 5.7.4 Lithium-sodium vapor-space processing system with an Incoloy-800H in-blanket condenser.

For the assumed mole fraction of tritium in the Li-Na mixture of 10 ppm the partial pressure is 4×10^{-9} atm. At this pressure the tritium in the Li-Na boiler will permeate through a 4000 m^2 TZM condensing vapor heat exchanger with 1 mm walls at a rate of 600 Ci/h, assuming that the gas species is either DT or HT and not T_2 . This permeation of tritium into the sodium loop will continue to raise the tritium concentration on the sodium loop side to a level at which permeation into the thermochemical process could become a problem. We believe that controlling this loss into the process to 5 Ci/d is mandatory. If we are to limit the permeation loss through the $40,000 \text{ m}^2$ area Incoloy-800H SO_3 decomposer process exchanger to 5 Ci/d, the sodium loop tritium partial pressure (i.e., DT or HT) must be held below 2×10^{-12} atm. This partial pressure would be relaxed significantly if an oxide layer, such as chromium oxide forms on the heat exchanger tube surfaces, since such oxides are known to have very low permeation rates for hydrogen isotopes or their oxides. Indeed, with the Incoloy-800, a metal oxide layer is expected to form on the thermochemical process side as the result of oxidizing conditions in the stream. For this situation a permeation coefficient of 1.0×10^{-3} is expected.⁽⁹⁾ The details of these results are discussed elsewhere in the Supplement.

We can hold the partial pressure in the sodium loop down to the desired 2×10^{-12} atm. by means of a sodium loop permeator with an area of 120 m^2 . This additional permeator would also be a niobium tube exchanger with hot helium sweep gas on the shell side and sodium on the tube side. Again, the tube would be kept at 1273K, which is hotter than the sodium in order to keep the surface clean. The hot helium would be scavenged of its low level tritium by a conventional catalytic oxidation and a mole sieve adsorption process train.⁽¹⁰⁻¹⁶⁾

The concentration of tritium in the sodium loop corresponding to a partial pressure of 2×10^{-12} atm. can be estimated from NaH data⁽¹⁷⁾, assuming Sievert's law, at 2.6 ppb.

These data and our methods of extrapolation are only approximate, but at least an estimate is helpful. If it were not for the strength limitations, Incoloy-800H alloy for the in-blanket condenser would be a viable alternate to TZM shown as Case A-2 in Figure 5.7.4. Tritium permeates through this material at 3.5 times the rate in TZM. Thus, the transport of tritium from the Li-Na boiler into the sodium loop will increase to 2090 Ci/h. If we still restrict losses to the process to 5 Ci/d, then we calculate from permeation rates that the sodium loop permeator must be increased in area to 730 m^2 . No other units in the process train need to be altered. We believe this larger permeator is still feasible. Thus, Incoloy-800H is a viable option if the temperature can be kept at 1100K in order to get acceptable strength.

5.7.5 Case B: Li-Na Boiler Vapor Space Processing with a Getter Bed (FIGURE 5.7.5)

This case is identical to case A-1 except that the Li-Na boiler permeator is replaced by a getter bed. Here the getter bed must handle 16 g/h of tritium. From a mass balance computation, it appears feasible if the bed is continuously moved through a regenerator process circuit at a rate of about 38 kg/d for bed loadings of 1% by weight or 380 kg/d for 0.1%. For a bed residence time of 10 h, this would amount to an inventory of only 160 g tritium. With beds of lengths of several meters and diameters of around 0.3 m, containing 1 cm diameter coated particles the pressure drops would be very small.

Although we only have experimental data on the performance of a yttrium getter bed with lithium⁽¹⁸⁾ at ppm tritium levels at 473K, we believe we need to process the inert gas vapor space at 1173K and attain ppb levels. We believe that the use of yttrium as a getter is not a workable approach here. Singleton, et al.,⁽¹⁹⁾ showed that good purification factors (i.e., inlet/outlet concentration ratios) of 10-20 were achievable with cerium in the tritium concentration range from 0.1 ppb to 100 ppb in argon, covering 298K to 623K. Since we are seeking levels of 10 ppm tritium in the Li-Na melt, these performance tests are very encouraging. It appears⁽¹⁹⁾ with adequate vessels (with lower tritium permeation rates than in 316 stainless steel) a cerium getter bed can be operated in excess of 873K with no decline in performance.

5.7.6 Case C: Li-Na Boiler--Tritium Removal Heat Pipes,
Na Loop Permeator (FIGURE 5.7.6)

This case is identical to case A-1 except that the Li-Na boiler permeator is replaced by tritium-concentrating heat pipes. The principle has been previously discussed,^(20,21) and involves a conventional heat pipe which is gas-buffered where the sweep action of liquid metal vapor inside pumps the gas and concentrates it at one end. If the gas contains tritium,⁽²¹⁾ this principle allows tritium-concentration at one end at partial pressures around 3×10^{-3} atmospheres, where permeation out through a small window into the tritium purification process can proceed rapidly.

In the original concept,⁽²¹⁾ the heat pipe contained lithium which under a neutron fluence would breed tritium. In this case, however, we are breeding tritium in the Li-Na

FIGURE 5.7.5 Lithium-sodium vapor-space processing system with a moving getter bed.

FIGURE 5.7.6 Lithium-sodium vapor-space processing system with tritium-removal heat pipes.

boiler and would utilize this heat-pipe concept simply as a heat-driven concentrator using sodium. We first examined the feasibility of replacing the condenser tubes by the heat-pipes with a body of Incoloy-800H. The permeability of this alloy, as in the Case A-2, with a tube area of 6000 m^2 is 2090 Ci/h. The Li-Na boiler tritium inventory is 1 kg at a partial pressure of 4×10^{-9} atm.

The condenser end would permeate tritium at the required rate into the tritium purification system through a small niobium permeation area window at the end of the tube. This is possible since the tritium within the heat pipe would build up to a partial pressure of about 3×10^{-3} atm. This design requires a heat transfer or permeation area of 1100 m^2 at the evaporator end of the heat-pipe, as did the condenser which is as large as in the Case A-2.

Based on our calculations in back-diffusion given in the Supplement we believe that the heat pipe can operate with a large tritium partial pressure difference between the ends (i.e., below 4×10^{-9} atm at the evaporator end to around 3×10^{-3} atm at the condenser end), since the large driving force for back-diffusion of tritium is against the sodium vapor velocity.

We can estimate the extent of this back-diffusion by first estimating the binary diffusion coefficient of tritium in sodium vapor. Critical properties for sodium vapor are available⁽²²⁾ as follows:

Temperature:	$T_c = 2800K$
Pressure:	$P_c = 510 \text{ atm}$
Volume:	$V_c = 150 \text{ cm}^3/\text{gmole}$
Density:	$\rho_c = 0.1533 \text{ g/cm}^3$
Compressibility:	$Z_c = 0.327$

Using techniques in the literature and the above sodium vapor properties we estimate⁽²³⁾ the binary diffusion coefficient at 1200K to be:

$$D_{HT-Na} = 2.23 \text{ cm}^2/\text{s}$$

If we use 100,000 heat pipes about 2 m long and 2 cm in diameter, the sodium vapor flux will be about $4 \text{ g/cm}^2\text{-s}$, or a linear velocity of 116 m/s within the tube. We find with the condenser end reaching 2×10^{-2} atm tritium; the back diffusion of the tritium is exactly matched by the bulk flow of the sodium vapor in the opposite direction to obtain an acceptable rate of 16 g/h of tritium. We can construct windows at the ends of the tubes with a total area calculated from permeation to be 30m^2 . These windows would be simply a 1 mm thick disk of niobium 2 cm in diameter fitted into the end of the tube. This permeation window design would hold the tritium at the condenser end to a partial pressure of 1×10^{-3} atm which is safely below the 2×10^{-2} atm level where back-diffusion starts to become a problem.

This design concept offers the large advantage of concentrating and separating the tritium. It appears to us at this time that this tritium-concentrating heat pipe would be excellent for this blanket design where tritium breeding can be undertaken in the lithium sodium pool and removed within the heat-pipe to a separate part of the process.

5.7.7 Removal of ^4He Using a Fused Silica Permeator

The removal of ^4He is critical to the operation of the blanket. ^4He is generated at a rate equal to that of the tritium and the ^4He will not permeate through metal permeators. We find at 1200K that helium has a permeation coefficient through fused silica of about ten times that of tritium. (24-26) The ^4He partial pressure is maintained at 0.45 atm in the blanket in order to provide ^4He permeation rates equal to tritium.

In order to utilize a fused silica permeator, we need to use a partial condenser to remove Li or Na vapor and other impurities that could condense out on the fused silica surface (or the niobium surface as well). By condensing out these impurities, and keeping the silica tubes hotter than the gas processed to avoid significant impurity adsorption, we believe that we can insure a long lifetime for such a permeator unit. This proposed technique would have to be tested, however, since even small amounts of non-condensed Li or Na might limit the lifetime of the fused-silica permeator. We may want to expand the function of this partial condenser to involve a processing loop capable of removing more complex impurities through the use of special getters or chemical scavengers. But for now we show this unit as a partial condenser.

With the above approach ^4He could be removed with a 1100 m^2 hot fused silica window, through which about 16 g/h of helium would pass less than 0.1 Ci/h of tritium. The nearly pure ^4He could then be fed to a standard catalytic oxidation/molecular sieve adsorption unit to recover the residual tritium for the fuel purification process. The ^4He would then be cleaned to levels of tritium about 0.1%

of that of the incoming stream, or less than 0.1 Ci/d. We believe at this point the ^4He could be sent up a stack - adding less than 0.1 Ci/d to the total estimated tritium loss of around 10 Ci/d.

5.7.8 Tritium Recovery in a Helium Loop System

As an alternate to the sodium loop system, we also examined the use of high pressure (i.e., 30 atm) helium. For the Li-Na Cauldron, helium flows inside the CVHX, or for the flowing Li_2O blanket the helium stream sweeps past the Li_2O pellets becoming contaminated with the tritium which is generated by breeding from within the Li_2O pellets and diffuses outward into the helium stream. In both blanket designs we maintain the tritium within acceptable limits, in the He loop. Our concept is to remove a small portion of the helium as a slip stream for tritium scavenging. We envision a tritium scavenging processor has been provided that will remove tritium from a 2% stream of the main helium coolant flow for a tritium production rate from the blankets of 16 g/d.

The problem now becomes how to size the slip-stream tritium processor as well as specify its performance to keep the tritium levels in the recirculating helium coolant loop low enough to obtain acceptable losses to the environment through the thermochemical process heat exchangers. The properties that control permeation are the area, permeability of the alloy, wall thickness, and average wall temperature (see discussion in Section 5.7.4).

The heat exchangers are constructed of Incoloy-800H to take advantage of lower tritium permeation through the inherent oxide film.⁽³⁷⁻³⁹⁾ Although there has been considerable controversy as to the extent and mechanism for this oxide

barrier formation and its effectiveness in tritium permeation reduction, there has been verification of the laboratory studies^{37,38} in the full-scale power plant at Peach Bottom. This experimental verification was made by Ling Yang of General Atomic Company³⁹ by taking actual samples of the Incoloy-800H from the steam generators and measuring the tritium permeation through the oxide barrier-coated alloy and through the cleaned alloy. Substantial agreement was obtained with Strehlow's laboratory measurements.³⁸

In applying these results to our design, there is a problem in assessing the functional dependence of permeation rate on the partial pressure driving force (i.e., on the amounts or contributions from square root and linear dependencies). The oxide barrier-controlled permeation is characterized by a near-linear pressure dependence. When temperature transients occur, this barrier is temporarily reduced in its effectiveness owing to the formation of cracks or other imperfections in the coating.³⁷ Hence, we feel it is risky to extrapolate the experimental data, at around 1 Torr pressure, back to 10^{-3} Torr using a linear pressure dependence. Such an extrapolation would result in the very low value of 0.05 Ci/d tritium permeation losses into the stream.

For this design, therefore, we use a more conservative extrapolation procedure as follows: take the full credit for the oxide barrier permeation reduction, which has been observed as 200 fold reduction at around 1 Torr,³⁷ but use the conservative square root dependence to extrapolate to lower pressures (see Figure 5.7.7).

Using the above criteria for obtaining the oxide-protected Incoloy-800H permeation rates for tritium into the stream, we

FIGURE 5.7.7 Permeation rate vs temperature. (7-9)

FIGURE 5.7.8 Slip-stream processing in a recirculating system.

can now size the helium coolant flow slip-stream processor in order to reduce these tritium losses to acceptable levels. Figure 5.7.8 shows the schematic relationship of the slip-stream processor. Such system designs are frequently incorrect because the buildup of tritium in a recirculating system processed by a small slip-stream flow is neglected. The problem results from the fact that the tritium leakage into the helium flow system will build up the tritium concentration to higher levels where there is obtained at steady state a mass balance between tritium leakage source strength and the removal rates by permeation to the stream and the removal rate into the slip-stream processor. The solution for T , the tritium concentration, is obtained by trial and error in the relationship:

$$R + P = 0.99(0.02T) + T^{1/2} = 16 \text{ g/d}$$

where involves the geometric factors for permeation. Neglecting the buildup condition results in an error of a factor of 50 in tritium concentration in the helium flow. From the above relation, we calculate a tritium loss to the thermochemical plant of 5 Ci/d.

The slip-stream processor has been designed and sized (see Figure 5.7.9) as a two-stage catalytic oxidizer, condenser, and molecular sieve adsorber. The catalyst bed has a small volume, i.e., only 12 liters for a 99.5% tritium removal efficiency, and the molecular sieve beds have a volume of 18 m³. The process cost is estimated at \$300,000 including instruments and automatic control and safety systems.

An evacuated (10^{-6} Torr) reactor hall (i.e., containing no air) could be scavenged of tritium more easily and at far

FIGURE 5.7.9 Helium coolant slip-stream two-stage processor.

less cost, because the mole fraction tritium concentration would be raised from the maximum credible accident level of 600 ppm (1500 Ci/m^3) in the air-containing reactor hall to essentially unity. Similarly a 750 ppt ($2,000 \text{ Ci/m}^3$) level is raised to 36 mole%, or a 0.37 ppt (1 Ci/m^3) level is raised to 0.03 mole%. Thus, the tritium processor requirements and costs for maintaining low-level reactor hall tritium would indeed be trivial. There remains the cost tradeoff between constructing and maintaining the reactor hall structures at 10^{-6} Torr and the costs of the reactor hall processor and operation and maintenance of the large, tritium contaminated vacuum pumps. Evidently there must be other additional reasons for justifying an evacuated reactor hall since the tritium processor savings is likely to be small in comparison.

As an alternate to the catalytic-oxidation, molecular-sieve adsorption technique, a moving bed getter of zirconium pellets can be used to selectively remove tritium from the helium coolant flow slip stream taken off the main flow. Such getter systems⁽¹⁹⁾ typically have capacities of $60 \text{ cm}^3 \text{ T}_2/\text{g}$ of getter at STP. Thus a moving getter bed would remove 80% of the tritium (loading at 3.8 wppm) per pass from the helium coolant flow. Material in the getter bed would move at 176 g/d, tying up less than 1 g of tritium as inventory in the 7-liter bed.

5.7.9 Other Tritium Sources

The next major source of tritium leakage is from the tritium neutral beam injector, where 55 keV energetic tritons implant tritium into the surface of the electrode tubing. Once within the tube metal, we calculate that the tritium will

diffuse, producing a flux of tritium into the electrode helium coolant flow at about 70,560 Ci/d. A 10% fraction of the coolant flow is then processed to remove 99% of the tritium and the resulting residual tritium leaks into the heat exchangers or steam generators at about 3 Ci/d, where it eventually passes into the cooling water and then to the environment. The deuterium injector uses deuterium with a maximum 1% tritium impurity level and it creates a leak to the process of 0.3 Ci/d. See Supplement for a more detailed discussion.

A similar tritium loss phenomenon occurs in the TMR's direct converter where 55 keV energy tritons produce a loss of 8,000 Ci/d the helium flow. Again a 99.9% processor is used, handling 5% of the flow to reduce this source of tritium into the process to less than 3 Ci/d. See the Supplement for details.

All of the remaining miscellaneous leaks from the nuclear island equipment and piping or ducting are assumed to leak into the reactor hall at a maximum total of 180 Ci/d. The reactor hall atmosphere is processed and kept at low humidity to maintain tritium gas levels of below $40 \mu\text{Ci}/\text{m}^3$ and tritium water of below $10 \mu\text{Ci}/\text{m}^3$. This reactor hall processing design is expected to result in routine as well as credible accident losses through the reactor hall containment of less than 1 Ci/d. The detailed basis for these results can be found in the Supplement.

In this way, the tritium loss to the thermochemical process and thus to the environment is designed to be around 12 Ci/d, made up of 5 Ci/d from the blanket, 3 Ci/d from injectors, 3 Ci/d from direct converter, and less than 1 Ci/d from reactor

hall containment permeation. This level is consistent with other studies^(10-15,27-32) and NRC regulations^(33,34) for all nuclear fuel cycle operations, but is about the level suggested by the "As Low As Reasonably Advisable" (ALARA) cost/benefit analysis^(16,33-36), based on \$1000/man-rem or \$20/Ci-Y released, amounting to an expenditure cutoff at the \$66,000 level. Using the ALARA criterion we found that no further reductions in emissions could be accomplished by an expenditure of \$66,000 for additional oxidizer-molecular sieve units.

5.7.10 Conclusions

The tritium will be generated within the Li-Na Cauldron or the Li_2O flowing blanket designs and the concentration will be allowed to build up to a 1 kg inventory. This inventory corresponds to 10 ppm on a molar basis or about 1000 Ci/m³. The tritium partial pressure will be held at about 4×10^{-9} atmospheres by a scavenging system continually removing tritium from the vapor space above the lithium-sodium pool. For a tritium breeding ratio in slight excess of unity, this scavenging system must handle tritium at a rate of ~ 15 g/h or 150,000 Ci/h for a 2500 MW_t reactor. The key to the scavenging system is the use of a 0.4 mm thick niobium tubular array permeator system held at high temperature (i.e., 1273K). The surface area requirements are found to be 750m². The interior of these tubes are swept with 1273K helium in order to keep the tubes hot, clean, and low in tritium. The tritium is processed by conventional oxidation/adsorption processes. This technique holds the tritium inventory in the blanket to ~ 1 kg. Helium, the other by-product of the neutron/lithium reaction is removed from the blanket vapor space by a fused silica permeator of

✓ 1000 m² tube area. A vapor condenser protects the silica tube from residual liquid metal impurities. The helium loop system operates nearly identically to the sodium loop; however, for helium the slip-stream processor has a two-stage oxidizer/adsorber unit which is designated to meet our 5 Ci/d goal.

Tritium losses from the blanket through the heat exchangers into the thermochemical cycle must be controlled within acceptable (0.4 $\mu\text{Ci}/\text{m}^3$ or 5 Ci/d limits. In one of our conceptual designs heat is extracted from the Li-Na Cauldron by condensation of sodium vapor on Incoloy-800H tubes in the vapor dome region of the blanket. Tritium will leak out of the blanket via this path into the liquid sodium coolant flowing within these condenser tubes. The permeation leak rate is calculated to be 2000 Ci/h. Such a large permeation leakage can be controlled by installing another niobium tube permeator on the liquid sodium heat transfer loop side using a tube area 730 m² to remove tritium at 2000 Ci/h. The partial pressure of tritium in the sodium loop will increase until the sum of the tritium removed by the sodium-loop permeator and the tritium that leaks by permeation out of the process Incoloy-800 heat exchanger exactly match the permeation into the sodium-loop via the in-blanket condenser tubes. We calculate that the partial pressure of tritium in the sodium levels out at about 2×10^{-12} atm. We assume there is an oxide barrier in the Incoloy-800 process exchangers in the thermochemical plant interface (e.g. the SO₃ decomposer), and that this oxide barrier greatly reduces the tritium permeation rate. Actual high temperature gas cooled reactor (HTGR) experience at Peach Bottom has shown the Incoloy-800 to offer excellent tritium retention properties by the formation of a protective oxide barrier

with a permeation reduction factor of 200. With the oxide barrier, the tritium loss to the thermochemical process would be around 5 Ci/d which meets our environmental goals. Further reductions of tritium losses are possible. However, they are not as cost-effective from the man-rem dose in the NRC's as low as is reasonably achievable (ALARA) approach at \$1000/man-rem or \$20/Ci-y released.

We also explored in this section several accident modes. We found that a sodium loop between the fusion blanket and the process functioned effectively as an isolation loop. A 1% leakage of the tritium breeding rate (16 g/h) would only add a 1600 Ci/h load to the sodium loop permeator and only increase tritium loss to the thermochemical process by 28 Ci/d. This 1% leakage appears to meet the present day NRC criteria for fission plants. Likewise, gross failure of the in-blanket condenser would only dump the Li-Na blanket material from one module (i.e., $\approx 1/150$ of the blanket inventory) into the sodium coolant loop which would not result in explosive accidents and only add 20 Ci/d to the tritium loss. All Li-Na vessels contain dump valves and are surrounded with graphite spheres, and the Na piping is contained within inerted pipeways, steel-lined to prevent Li concrete reactions.

The remaining tritium losses were calculated for permeation and leakage from other process component designs and found to be small (i.e., 3 Ci/d from direct converter, 3 Ci/d from neutral beam injectors, etc.). Tritium inventory within onsite, remote storage tanks was taken as 16 kg and for the cryopumps was estimated at 4 kg, cryogenic distillation columns at 1.4 kg, and the remaining cryo-panels, molecular sieve beds, etc., totaling altogether another 0.5 kg. The in-process tritium inventory at risk (not including storage tanks separated and remote from the reactor), therefore, is expected to be around 7 kg.

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5.8 Li-Na Containment and Handling

5.8.1 Review of Available Experiments

In this section we examine the experimental background covering the behavior of hot liquid lithium and sodium so that we can start to develop the engineering details of the approaches described in the previous section, and to evaluate their expected performance. Reviews of the literature on liquid lithium⁽¹⁾ and sodium⁽²⁾ are available and we will use these reviews to briefly summarize the most important properties as they impact our designs.

Both lithium and sodium are corrosive at high temperatures to most common alloys, and the effect is aggravated by high flow or turbulence conditions. In general, the refractory metals such as niobium, titanium, vanadium, and molybdenum are our preferred choices as base metals with alloying elements such as titanium, zirconium, and chromium.

The important high temperature lithium⁽¹⁾ and sodium properties⁽²⁾ are presented in Table 5.8.1. Lithium is generally considerably more reactive than sodium and the reactions occur at higher temperatures. In general, residual water vapor in He, Ne, CO₂, etc., catalyzes the reactions substantially.

Explosive conditions are possible between hot, molten lithium or sodium and liquid water arising from either the vaporization of large amounts of water and the generation of high pressures resulting in a boiler-like containment explosion or from the extreme rapidity and violence accompanied by the release of heat from the reaction between the finely divided metal and water reaction itself. Coating the surface of the water pool with oil, grease, or tar prevents trapping of water between the metal and the container, and this in turn prevents such explosions. Of course, the best way is not to leave open pools of water.

TABLE 5.8.1

HIGH TEMPERATURE PROPERTIES OF LITHIUM AND SODIUM

<u>PROPERTY</u>	<u>LITHIUM</u>	<u>SODIUM</u>
Melting point (K)	453	97.8
Boiling point (STP) (K)	1620	1156
Heat of vaporization (kcal/kg)	4680	901
Heat capacity at 900-1200K (kcal/kg-K)	0.995	0.30
Ignition temperatures in reaction with nitrogen (K)	440-720	no
Ignition temperature in air (K)	450-910	no
Water reaction to form H ₂	less reactive	more reactive
Reaction with concrete and ceramics	rapid	slow

Known extinguishing agents:

- Graphite
- MET-L-X
- Salt eutectic
- Inerted gas filled sumps

The Li-Na combustion-generated aerosol (1,3,9) particles are expected to be small (i.e., $< 10 \mu\text{m}$) and easily airborne. Kazimi et al.⁽⁴⁾ examined the consequences of lithium fires in the fusion reactor containment system, and proposed mitigating measures.⁽⁵⁾ In Kazimi's studies^(4,5) the UWMAK-I Tokamak with a 316-stainless steel and UWMAK-III with a TZM first wall were examined. In this case the accident scenario involved the volatilization of activated TZM and stainless steel corrosion products from the first wall by means of high temperatures produced by a lithium fire, and transport of these activation products outside of the containment structure. Stainless steel was found to be superior to TZM both in its Biological Hazard Potential (BHP) and its slower melting characteristics. The authors' results were based on a lithium air peak flame temperature of 2373-2473K.

Experimentally, Hanford⁽¹⁾ measured actual flame temperatures in the lithium-air combustion of a molten lithium pool and found them to be around 1533K some 13 minutes after the test. The airborne aerosol was typically at a concentration of 12 g of aerosol per m^3 . Lithium pool temperatures were typically ~1210K after 40 minutes.

Lithium-nitrogen experiments were also done at Hanford⁽¹⁾ with very little reaction observed at ambient conditions. The pool temperature reached only 495K and peaked at 815K with the pool initially at 805K. However, when the pool started at 1116K, more vigorous reactions occurred to result in a pool temperature of 1233K in four minutes. Again, aerosols were around $1 \mu\text{m}$. Only at this 1233K temperature did the reaction consume 100% of the lithium.

Reduced oxygen concentrations in the range of 4 to 8% N_2 cut the flame temperatures from around 2373K to 1583K-1803K. A lithium fire computer code, developed at Argonne, was used to simulate the temperature-time history of a potential lithium fire. A UWMAK-III failure of one blanket module, (1/18 of the total lithium inventory) produced lithium pool temperature peaks of about 1653K and containment temperature around 1373K. The containment internal pressure peaked at around 2 atmospheres.

The compounds of lithium, such as LiH, Li_2O , LiOH, and Li_3N are stable, but extremely reactive and corrosive. These compounds are not directly used in our Li-Na Cauldron pool boiler concept, but can be used (i.e., Li_2O) in the flowing blanket design now being examined by the University of Washington/Exxon Nuclear team. Particularly, Li_2O is highly reactive with water, CO_2 , and most oxides or refractory compounds.

Lithium-concrete reactions are another area of concern in our designs. Experimental studies at Hanford⁽¹⁾ showed that liquid lithium at 600K reacted in inert atmospheres with the concrete via the release of the hydrated water. Temperatures at the surface were typically above 1373K, there was hydrogen offgassing and 5 to 8 mm penetration into the concrete. The combustion gases contained large quantities of $\sim 1 \mu m$ sized aerosols. These aerosols were similar to those generated in lithium-air combustion, with a similar ability to carry any airborne activation products contained within the original lithium.

When pool temperatures exceeded 1245K, large quantities of hydrogen were evolved, resulting in large increases in the vessel internal pressure. The hydrogen pressure increased over the 30-hour test, even after 10 hours when the pool temperature had peaked at 1243K. Most of the water reacted with the lithium to eventually produce H_2 . Large quantities of H_2 gas are similarly evolved when sodium reacts with a pool of water.

Dense, white, and opaque smoke results when molten lithium is suddenly dumped in air at 1073K into a metal catch pan. Splattering of hot metal onto other combustible materials is also a problem. The fumes are irritating, and are produced in large quantities as the combustion propagates. To prevent or control this type of accident, the metal catch pans should be converted into inerted dump tanks designed for rapid draining of the hot lithium should there be an equipment failure or accident that could lead to a lithium/sodium fire. The operation of this dump valve is, however, an active control means. The goal is to control the fire by cooling the reacting mass as soon as possible and preventing the propagation of the hot reacting gases to other combustibles, thus involving those areas as well.

Conventional fire extinguishing agents, if used by mistake, seriously worsen the lithium or sodium fire since lithium and sodium react with water, and lithium reacts with CO_2 , sodium carbonate, aqueous foam, soda-acid mixtures, and carbon tetrachloride. A number of effective fire extinguishing agents have been identified⁽¹⁶⁾; the better ones appear to be lithium chloride, graphite spheres, pyrene G-1 powder, Lith-X, boric acid, and graphite in #30 motor oil, G.E. (silicone oil), trimethoxyboroxine, celogen blowing agents, eutectic of $BaCl_2$, $NaCl$, and KCl , and mixtures of celogen with the eutectic salt.

There has been concern that the release of neutron induced activation products might be of greater concern from the safety and environmental standpoint than are accidental releases of tritium⁽⁴⁻⁸⁾. It was concluded^(5,7) that the release of activation products in severe accidents involving UWMAK-I Tokamak reference designs could deliver acute off-site doses appreciably larger than those from even a very large tritium release (more than 700 MCi) when stainless steel or molybdenum alloys are involved. The consequences of such accidents are shown^(5,7) to be comparable to those from fission reactor accidents, although the probabilities for such fission accident may be very much less.

5.8.2 Development of Containment Design Concept

Several solutions emerge: (1) a blanket module dump valve and surrounding vault to channel the drained or leaked lithium-sodium pool into an inert gas-filled tank to reduce the amount of energy released into the containment, (2) the use of chemical fire extinguishers (i.e., graphite, pyrene G-1, and Lith-X), (3) subatmospheric containment pressures (and/or low oxygen concentration), and (4) active post-accident cooling of the containment gas and aerosol filtering of any vented gases. Since, even under the worst case accident condition, it appears difficult⁽¹⁰⁾ to generate enough heat from a Li fire to melt the activated first-wall material, implementation of one or more of these engineering solutions should greatly reduce the probability of this kind of accident.

In our Tandem Mirror driven thermochemical plant we have attempted to address these concerns by providing design features which will greatly reduce both the consequence and the probability of such releases over Tokamak designs. First, our cooler first-wall design allows us to use vanadium and titanium with characteristically low activation.⁽⁵⁾ Next, the simpler TMR blanket cylindrical geometry allows us to modularize, contain and encapsulate the Li-Na mixture. The lithium is not used in a flowing system, and thereby has a lower probability for leakage compared to a pipe or pump failure. Although the stored chemical energy (releasable only by reaction with oxygen, water, or water of hydration) is not reduced in this design, we can reduce the opportunity for the release of this energy by reducing the likelihood for mutual contact between Li and Na and the critical reactants O_2 and H_2O .

We address the scenario that the activation products can only escape by release of the Li-Na mixture through a failure of one or more of our blanket module capsules or piping. Catching the hot contaminated liquid Li-Na from a module failure or from a piping failure into the sodium coolant would be straightforward. The problem develops if there is a Li-Na fire wherein the combustion products become airborne and spread throughout the containment structure or ventilation systems.

Filtration of both the lithium and sodium aerosols appear straightforward, capturing nearly all of the airborne particles with commercial HEPA filters.

We have also provided a passive control means as a backup. This concept utilized Hanford's^(15,16) finding that graphite spheres are an effective means to quickly reduce the temperature of the melt and quench the combustion reaction, as shown in Figure 5.8.1. We have also provided an inert gas purge to help insure that a fire that might occur at the top surface of the graphite bed would not propagate downward through the graphite bed and involve the lithium pool.

The amount of graphite spheres required to quench $10 \text{ m}^3/\text{m}$ of reactor length of molten Li-Na in the TMR to below 573K, where no further reactions are likely, would be 10 Mg/m or about 200 Mg for the entire reactor length. These graphite spheres could be contained, as shown in Figure 5.8.1, within a boundary some 33 cm wide outside of the solenoid coils and extending up to the condensing vapor heat exchanger. This envelope would cover the full width of the module and includes a large safety factor.

The area around these blanket modules, shown in Figure 5.8.1 is densely packed with piping and electronic cabling and complicates the use of graphite spheres in this region. One concept, that appears practical, is to use small (3mm dia) graphite spheres that can be pneumatically conveyed into and out of this outer vault area. This is the kind of pneumatic technology that is being used in the new double-glazed solar windows where the space in-between can be made clear or opaque at the flip of a switch by blowing dark beads into and out of the space.

Fig. 5.8.1 Cross-Section of the Cauldron Blanket Module Showing Surrounding Graphite Spheres

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Conclusions and Observations on the Cauldron

This section has analyzed and begun to put together, in a cohesive way, some of those elements related to the preliminary design of the Cauldron blanket module. There is substantial work remaining but some significant conclusions and observations can be made.

1. The relatively simple configuration of the blanket module with its integral heat exchanger provides full plasma coverage when assembled into unit cells composed of one solenoidal coil and four blanket modules. The unit cells provide an attractive means for assembly/disassembly.
2. Neutronically the LiNa "boiler" portion of the module provides integral tritium generation and tritium recovery at the desired process temperature of 1200K rather than having to resort to a discrete but lower temperature zone in which the tritium is produced. This is an impressive feature of this design and makes it possible to achieve values for the energy fraction recovered at high temperature which are considerably greater than those typically reported previously.

The neutronics to date has assumed a LiNa bath that is homogenous and uniform. The heat transfer processes that occur, boiling in the bath and subsequent condensing on the integral heat exchanger will influence neutronics by bubble voids, temperature variations, etc., so that more elaborate, two dimensional representations will have to be performed.

We do not yet know precisely how to make these representations but in our FY81 work it is planned to fabricate and test a scale model of a Cauldron module to determine its heat transfer performance and characteristics. We believe that these data will then be useful to the neutronic studies in addition to verifying their principal objective.

3. The "cool container" concept of the Cauldron module; that is, housing the 1200K LiNa fluid in a 700K structural envelope appears to work quite well using the commercial "feltmetal" to establish the desired temperature gradient. The benefits are at least threefold:

- The container need not be made from super-alloys and can, in fact, be made from materials with low residual activity such as vanadium and titanium.
- The porous feltmetal allows gas sweep circuits that help in tritium removal and control.
- The division of energy between the structural container and the pool can be dominantly in the high temperature pool (95% - 5%) as mentioned in 2. Energy partitioning is excellent.

The difficulty with the "cool container" is in selecting adequate materials for the membrane which separates the bath from the feltmetal and the structural envelope. Although the stresses are low in this membrane it is subjected to the 1200K bath temperature and it is difficult to assess the problems of long term creep on materials such

as beta-titanium at this temperature. This concern about membrane material and long term creep strength also applies to some fraction of the fuel metal immediately next to the membrane.

4. Fabrication and welding of large structural units such as the Cauldron using electron beam welding has been well established as a technology by the aerospace industries.
5. In the "cool container", which doubles as a first wall heat exchanger, (FWHX) there is as yet no clear cut choice of a coolant. With sodium there is an MHD pressure drop induced of ~ 10 atm. due to B field effects. Some degradation of the blanket neutronics also occurs because of the up front sodium volume. Fortunately coolant tube diameters of 2 centimeters, required for other reasons, keep the sodium volume low. For helium as the coolant the neutronics is better and helium is always attractive as a relatively inert gas. However, working pressures must be high, of the order of 50 atm, and stresses (diametral) created by large film temperature drops characteristic of gas flow, must be watched carefully. The organic coolants, not subjected to MHD effects and capable of operating at low pressure, have stability problems in a radiation field and the cure against stability may be more difficult than simply resorting to helium or sodium. The coolant choice for this FWHX needs further study and will certainly be influenced by coolants we use elsewhere, particularly in the condensing vapor heat exchanger where the bulk of the flow occurs.
6. The condensing vapor heat exchanger (CVHX) located in the dome of the Cauldron module is a fairly straightforward design problem. If sodium is the coolant then pressures

can be substantially balanced between condensate and coolant and stresses will be low. The 1200K process temperature level is also highly conserved for delivery to the chemical process equipment because of low film temperature drops both in the flowing coolant and in the condensate film. However, delivering large volumes of high temperature sodium (or potassium) to the process chemistry plant is a difficult safety problem. Distances are likely to be long, ~100m and volumetric flow rates are high. Furthermore, bringing together in very close proximity (separated only by a barrier wall) the sodium and the chemical process fluids and vapors such as SO_3 may preclude the use of sodium. It is difficult to find a single metal that can exist in both a sodium and a SO_3 environment. If helium is used as the coolant in the CVHX then there are pressure penalties and pumping power penalties one must accept.

7. We have placed the operational safety of the Cauldron module at the highest priority and have paid serious attention to this area since the handling of high temperature liquid metals is very difficult. In the course of our overall study we have given equal voice to the allegedly benign solid blankets, in our case Li_2O . Interestingly enough the Li_2O flowing microsphere concept heat exchanges with liquid sodium outside of the blanket so the safety problem is physically relocated but nonetheless still of concern. In the Cauldron module the binary liquid metal LiNa is isolated within the module itself. One safety isolation barrier is the condensing vapor heat exchanger located in the dome. This heat exchanger can provide further isolation by the use of heat pipes between the condensate and the coolant flow. The "cool container" enveloping the boiling LiNa fluid provides isolation via

the membrane plus the tube wall itself. All the modules in the reactor are contained within a vacuum envelope. The exterior structure in which the total reactor is housed is imagined to be a steel lined, two meter thick reinforced concrete building also under vacuum. Other safety features include quick acting, gravity activated, dump valves for liquid metal removal from a module. Temperature and pressure sensors are provided in the dome and elsewhere to continuously monitor performance.

8. Plasma-produced ^4He can be removed economically using a Li-Na pool boiler blanket vapor dome processor which includes a Na vapor condenser and a quartz permeator.
9. Plasma-produced tritium can be removed using conventional catalytic oxidation and mole-sieve adsorber processes operating on the blanket vapor dome and holding the total combined blanket inventory to 1 Kg. A slip-stream processor, again of the oxidizer-adsorber type, operating on the helium coolant, can hold the tritium loss into the H_2 plant to 5 ci/d thus meeting the NRC's ALARA criteria for cost/benefit and the 5 ci/m³ limit for a 10% H_2 gas product for a domestic home cooking or heating market.
10. How well the Cauldron idea will work as a heat transfer device is key to the success of this particular fusion/synfuel tie study. There are insufficient data, both analytical and experimental, in this area of bulk fluid heating by internal heat generation complicated further by the MHD effects of the B-field. Experimental verification of the Cauldron module performance is clearly essential before we can actually design an advanced pool-boiling fusion reactor blanket with high confidence. We have begun the design of a scale model which we plan to test in FY81.

APPENDIX 5A

EQUATIONS USED FOR HEAT TRANSFER CALCULATIONS - FIRST WALL HEAT EXCHANGER

- A. Energy deposition in the coolant flow in the tube due to wall load W_L for the heated length L . ($L = \text{Const.} = 2 \text{ M}$ for Ref. Design)

$$q_c = \frac{(d - 2W)^2 L W_L (P_y)}{4}$$

where P_y = power density in the coolant
for helium $q_c = 0$.

- B. Neutron heating in the tube wall material

$$q_t = 12 \pi d W_L$$

- C. Neutron heating in the felt metal

$$q_F = 7.7 (d L t W_L \rho_F)$$

($t = 0.005$ and $\rho_F = 0.4$ are assumed constants)

- D. Neutron heating in the membrane

$$q_m = 7.5 (d L \delta W_L)$$

E. Total energy flow into the coolant

$$q_T = A + B + C + D$$

F. The mass flow rate of the coolant

$$\dot{m} = \frac{q_{TOTAL}}{C_p \Delta T_L}$$

where ΔT_L is the temperature rise in the tube length L .

G. The flow velocity of the coolant

$$v = \frac{\dot{m}}{\bar{\rho} A} = \frac{4\dot{m}}{\bar{\rho} \pi (d_o - 2w)^2}$$

where for helium

$$\bar{\rho} = \left[(0.14 \times 350) / \left(350 + \frac{\Delta T_L}{2} \right) \right] P$$

and

P = helium pressure in atmospheres. 350K = assumed inlet reference temperature

H. The Reynolds number

$$N_R = \frac{d v \bar{\rho}}{\mu}$$

I. The Hartmann number

$$H = B_L \left(\frac{d-2w}{2} \right) \left(\frac{\sigma}{\mu} \right)^{1/2}$$

For helium the Hartmann number is zero.

J. The Hartmann pressure gradient in a rectangular channel

$$\left(\frac{-dp}{dx} \right) = \left[\frac{H^2 \tanh H}{H - \tanh H} - 3 + p_0 + \frac{H^2 C}{1 + C} \right] \frac{\mu V}{\left(\frac{d-2w}{2} \right)^2}$$

For $H \geq 3$, $\tanh H \rightarrow 1.0$

$$\left(\frac{-dp}{dx} \right) = \left[H - 3 + p_0 + \frac{H^2 \times 0.1}{1.1} \right] \frac{\mu V}{\left(\frac{d-2w}{2} \right)^2}$$

For round tubes rather than rectangular channels and for the large Hartmann numbers that will be encountered for liquid metal coolants

$$\left(\frac{-dp}{dx} \right) = 1.3 \left[H + \left(\frac{H^2 C}{1 + C} \right) \right] \frac{V}{\left(\frac{d-2w}{2} \right)^2}$$

Finally for the point design

$$\Delta P = 1.3 \left[H + \left(\frac{0.1 H^2}{1.1} \right) \right] \frac{VL'}{\left(\frac{d-2w}{2} \right)^2}$$

where L' is the tube length in the transverse field only = 1.6 L

C = conductance ratio ≈ 0.1

K. For helium and the organic coolant the pressure drop in the length L'' is:

$$P = \frac{2f\dot{m}^2}{\rho} \frac{2.6L}{d} \left(\frac{\pi d_i^2}{4} \right)^{-2}$$

where for helium the friction factor, f , = $0.043 (N_R)^{-0.2}$

and for the organic fluid $f = 0.011 (N_R)^{-0.2}$,

$L'' = 2.6L$ = Length in transverse and parallel field.

L. The pumping power

$$PP = \frac{\Delta P \dot{m}}{\rho}$$

M. The pumping power, as a percentage of total power in the length L.

$$\% = \frac{PP}{q_{TOTAL}} \times 100$$

N. The Nusselt number Nu for Hartmann flow when

$$H > \frac{N_R}{500}$$

$$Nu = \frac{hd}{k} = 7.0$$

For the helium and the organic coolant the Nusselt number is

$$Nu_{He} = 0.0182 N_R^{0.8}$$

$$Nu_{ORG.} = 0.043 N_R^{0.83}$$

O. The film temperature drop

$$\Delta T_{FILM} = \left[\frac{q_{TOTAL} - (q_c + q_t/2)}{hdL} \right]$$

P. The temperature drop across the interior tube wall (on the Li-Na side)

$$\Delta T_{WALL} = \left[\frac{q_{TOTAL} - (q_c + q_t)}{kdL} \right] W_L$$

APPENDIX 5B

EQUATIONS USED IN THE CVHX SINGLE TUBE ANALYSIS

- (1o) The heat input in watts, Q_{in} , into the coolant in the tube length L

$$Q_{in} = q_t \pi d_o L$$

- (2o) The mass flow rate m in kilograms/sec.

$$\dot{m} = Q / c_p \Delta T$$

For the helium

$$\dot{m}_{he} = \frac{Q}{5.2 \times 10^3 \Delta T}$$

For the sodium

$$\dot{m}_{na} = \frac{Q}{1.2 \times 10^3 \Delta T}$$

- (3o) The flow velocity V in m/s

$$V_{he} = \dot{m} / \bar{\rho} A P$$

where

$\bar{\rho}$ = mean density of helium at 1 atm

P = pressure, atms

We establish a reference density, ρ_{ref} , at the specified outlet temp. of 1200K. This $\rho_{ref} = 0.0405$ for helium. The mean density is taken at $(1200K - (T/2))$.

For the helium

$$\bar{\rho}_{He} = .0405 \times \frac{1200}{1200 - \Delta T/2}$$

The inside diameter of the coolant tube is taken as $0.9 d_o$.
Finally, the flow velocity of the helium is:

$$v_H = \frac{4\dot{m}}{0.81 d_o^2 \rho_{he}}$$

For sodium, the density ρ_{na} is also referenced at 1200K and corrected to the mean temperature $1200 - \Delta T/2$.

$$\rho_{na} = \left[0.73 + (2.36 \cdot 10^{-4} \times \frac{\Delta T}{2}) \right] 10^3$$

and the sodium velocity is:

$$v_{na} = \frac{4 \dot{m}}{0.81 d_o^2 \rho_{na}}$$

(4o) The Reynolds Number for helium:

$$R_e = \frac{d_i v_{he} (\bar{\rho} P)}{\mu}$$

The reference viscosity at 1200K is equal to

$$= 512 \times 10^{-7} \text{ kg/ms and at } (1200 - \Delta T/2)$$

$$\mu = \left[512. - (0.33 \times \Delta T/2) \right] 10^{-7}$$

for the sodium the Reynolds Number

$$R_e = \frac{d_i v_{na} \bar{\rho}_{na}}{\mu_{na}}$$

Where the viscosity term is:

$$\mu = (1.5 + 1.33 \times 10^{-3} \times \Delta T/2) \times 10^{-4} \text{ kg/ms}$$

- (5o) The friction factor, f , for fully developed turbulent flow in smooth tubes is proportional to the Stanton N_S when the Prandl number is ~ 0.5 to 100. So that $f = 1.5 N_S$. The Stanton number is:

$$N_S = 0.023 (Pr)^{-2/3} (Re)^{-0.2}$$

For helium the Prandl number is 0.72 over the temperature range of interest and the friction factor for helium is:

$$F_{he} = 0.043 (Re)^{-0.2}$$

For sodium, the Prandl number $\frac{C_p}{k}$ is small, ~ 0.003 and the friction factor will be calculated using the relation

$$f_{na} = .046 (Re)^{-0.2}$$

- (6o) The pressure drop ΔP in newtons per m^2 (Pascals) is: for helium:

$$\Delta P_{he} = 2 f \frac{L}{d_i} (\bar{\rho} P) V_{he}^2$$

(Ref. Kreith
Princ. of Heat
Transfer p. 343)

and for sodium

$$\Delta P_{na} = \frac{2 f L}{d_i} \rho_{na} V_{na}^2$$

$d_i = 0.9 d_o$ in either case

- (7o) The pumping power in watts is:

$$PP = \text{Pressure drop} \times \text{Volumetric flow} = \Delta P (AV)$$

- (8o) The percentage of thermal power using for pumping power is

$$\% = \frac{PP}{Q} \times 100$$

- (9o) The Nussult number, Nu , for helium and for constant heat rate when $0.5 < Pr < 1.0$ is:

$$Nu_{he} = 0.0222 (Re)^{0.8} (Pr)^{0.6}, \text{ The } Pr \text{ number} = .72$$

$$Nu_{he} = 0.0182 (Re)^{0.8}$$

For sodium the Nussult number is:

$$Nu_{Na} = 7.0 + 0.025 (RePr)^{0.8}$$

- (10o) The film coefficient $\bar{h}_c$ is calculated from the Nussult number. First we establish the mean conductivity, $\bar{k}$ of the helium at temperature

$$\bar{k} = 0.368 - \left(\frac{\Delta T}{2} \right) \times 2.35 \times 10^{-4}$$

and then

$$\bar{h}_c = \left(\frac{\bar{k}}{0.9d_0} \right) Nu$$

(11o) The temperature drop across the film is then determined:

$$\Delta T_{\text{film}} = \frac{q_t}{h_c}$$

(12o) The temperature drop across the tube wall when the tube wall thickness is 5% of the tube diameter is:

$$\Delta T_{\text{wall}} = q_T \times \frac{0.05 d_o}{k_{\text{wall}}}$$

APPENDIX 5C

During operation of the reactor the coolant tube of the FWX is heated unequally, receiving energy from one side only. (We had previously assumed the charged particles deposited no surface energy on the first wall.) The energy sources causing this unequal heating of the tube are the energy generation in the membrane and the energy generation in the fuel metal. We assume that the very nearly uniform neutron heating of the tube wall itself induces no stress. Because of the one sided heating the tube tries to deform but is constrained to retain its shape. Consequently bending stresses arise. If the tube had been unrestrained it would have bowed into an arc.

To assess this stress due to constraining the tube to remain straight or in a constant arc we consider a unit length of tubing ($s = 1.0$) of diameter d , subjected to heating from one side only so that there is some differential growth, Δs , where

$$\Delta s = \alpha \Delta T. \quad (1)$$

Figure 5AC1 illustrates the differential growth. Without constraint the tube develops a radius of curvature r from the unequal heating. We wish to determine the value of r as a function of d , the tube diameter, and Δs , the differential growth due to the diametral temperature difference. We know that for arcs, $s = r\theta$ (θ in radians) and from the Fig. 5AC1

$$r = \frac{s + \Delta s}{\theta} \quad (2)$$

$$r = \frac{1.0 + \alpha \Delta T}{\theta} \quad (3)$$

The α term is the coefficient of thermal expansion of the tube material. Knowing the tube diameter d allows r to be calculated. The greater the value of the tube diameter, d , the more severe is the arcing.

The ΔT in equation 3 represents how much hotter one side of the tube is than the other side (the diametral difference in temperature). For our case, as Figure 5AC2 indicates

$$\Delta T = 1/2 \Delta T_{\text{WALL}} + \Delta T_{\text{FILM}} \quad (4)$$

Fig. 5AC1 - Radius of curvature θ determined as function of tube diameter d and differential growth s .

Figure 5AC2 Section through coolant tube showing the diametral temperature difference.

Fig. 5AC3 - Calculation of the deflection, b , of a tube subjected to non-uniform heating.

From Fig. 5AC1 we see that,

$$\tan \frac{\theta}{2} = \frac{\alpha \Delta T}{2d} \quad (5)$$

$$\frac{\theta}{2} = \tan^{-1} \left(\frac{\alpha \Delta T}{2d} \right) \quad (6)$$

$$\theta \text{ radians} = 2 \tan^{-1} \left(\frac{\alpha \Delta T}{2d} \right) \quad (7)$$

The radius of curvature of the tube due to non-uniform heating is

$$r = \left(\frac{1.0 + \alpha \Delta T}{\theta} \right) \quad (3)$$

Figure 5AC4 Tube deflection, b , created by nonuniform heating.

We require b , the displacement of the tube, to determine the stress. From Figure 5AC3 we see that in terms of b :

$$\cos \theta = \frac{a}{r} = \frac{r - b}{r} \quad (8)$$

The stresses that arise may be calculated assuming the tube is a cantilevered beam with an end couple deflecting the amount b , as shown in Figure 5AC4. If we use this value of b we can then determine the stress. The equation for the deflection, b , of the beam with the end couple is:

$$b = \frac{1}{2} \frac{ML^2}{EI} ; \quad (9)$$

The bending moment of the beam is

$$M = \frac{2EIb}{L^2} ; \quad (10)$$

The desired bending stress is

$$\sigma_B = \frac{Mc}{I} = \frac{2Ebc}{L^2} . \quad (11)$$

To this bending stress we must vectorally add three additional stresses, the local thermal stress σ_T across the thickness of the tube wall, the hoop stress σ_H , and the longitudinal stress, σ_L . These stresses and the directions in which they act are illustrated in Figure 5AC5.

Figure 5AC5 Components of stress in a pressurized tube subject to heating from one side only.

The thermal stress across the thickness of the tube wall, σ_T , is

$$\sigma_T = \frac{E\alpha\Delta T_{\text{wall}}}{2(1 - \nu)} . \quad (12)$$

The hoop and the longitudinal stresses are

$$\sigma_H = \frac{PR}{t} \quad (13)$$

$$\sigma_L = \frac{PR}{2t} \quad (14)$$

APPENDIX 5D

DETERMINING THE TEMPERATURE GRADIENT ACROSS THE FELTMETAL

$$\frac{d^2x}{dt^2} = -\frac{\dot{q}}{k_e}$$

where q = internal heat generation in the feltmetal W/m^3
 k_e = effective thermal conductivity of the feltmetal

$$\frac{dt}{dx} = -\frac{\dot{q}x}{k_e} + C_1$$

$$t = -\frac{\dot{q}x^2}{2k_e} + C_1x + C_2$$

Fig. 5AD1 Temperature Gradient Across Feltmetal

$$t = t_1 \text{ at } X = 0$$

$$t = t_2 \text{ at } X = L$$

and

$$dt/dx = 0 \text{ at } X = L$$

$$C_1 = \frac{\dot{q}L}{k_e}$$

and

$$C_2 = t_1$$

$$t_2 = -\frac{\dot{q}L^2}{2R} + \frac{\dot{q}L^2}{k_e} + t_1$$

$$\Delta T = t_2 - t_1 = \frac{\dot{q}L^2}{2k_e}$$

Example

$$\dot{q} = 7 \times 10^6 \text{ W/m}^3$$

$$L = .005 \text{ m}$$

$$\Delta T = 500$$

$$k_e = 0.175 \frac{\text{W}}{\text{mK}}$$

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SECTION 6
FLOWING MICROSPHERE BLANKET AND
MODULE INTERMEDIATE HEAT EXCHANGER

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FLOWING MICROSPHERE BLANKET AND INTERMEDIATE HEAT EXCHANGER MODULE

Two basic blanket module concepts were considered in this study. One is the Li-Na selective distillation blanket discussed in Section 5.0. The other is the flowing microsphere design which is discussed in this section.

Flowing particulates are used for a variety of industrial process heat applications and the concept has been considered for application in nuclear fission (pebble bed) reactors^(1,2) and fusion reactor blankets^(3,4). The concept for fusion blankets uses flowing spheres as both the heat generation and heat transport medium. While the spheres are in the blanket region, they are internally heated by neutron capture. The hot spheres flow out of the blanket to a heat exchanger where they give up their heat to a working fluid. The spheres are then returned mechanically to the blanket to close the loop. The specifics of sphere size, flow rate, temperature and heat exchange depend on the specific design concepts.

The SOLASE⁽³⁾ concept considered the gravity flow of Li_2O microspheres (100-200 μm) through the blanket of a laser fusion reactor as illustrated in Figure 6.1. The blanket was divided into zones and the flow controlled in proportion to the radial power distribution to achieve a more uniform outlet temperature. The microspheres entered the blanket at 400 C and exited at 600 C. Maximum sphere temperature was 850 C. due to peaking from radial power gradients. The heat exchanger was of shell and tube design with Li_2O in the shell side. Steam generated on the tube side was intended for electric power production. An Archimedes screw was considered for the return of the spheres to the top of the blanket.

The ANL concept⁽⁴⁾ considered the gravity flow of MgO spheres (~1 cm) through vertical channels. The inlet temperature was 300 C and the average outlet temperature was about 1000 C. The intended application was process heat for synthetic fuel production. Radial power gradients produced substantial temperature peaking toward the exit of the channel. Two radial zones were used to reduce temperature peaking.

The ANL program has proceeded concurrently with the study reported here with their primary emphasis on thermal optimization of the blanket concept for the tokamak fusion driver. This objective is to produce a 1100 C heat gas stream for a synfuel process. A two-region blanket concept is being considered where a separate Li blanket region provides tritium breeding and sphere preheat via a process stream and heat exchanger. The second region provides the remaining high temperature heating of the spheres. Magnesia and alumina were the candidate sphere materials. The heat was transferred by direct contact to air (or steam) at low pressure. No work had been done on a method of returning the spheres to the top of the blanket nor has the overall system been considered.

In a previous report to this study, a flowing microsphere design was considered which involved the production of fissile fuel and process heat to assist a catalytic coal gasification process⁽⁵⁾. The basic idea was similar to that of SOLASE as shown in Figure 6.2. Microspheres (~400 μ m) of uranium carbide flowed by gravity through a radially zoned blanket. The microspheres were heated from 183 C to 816 C with a maximum of 1369 C due to radial power peaking. A shell and tube heat exchanger was considered for heat transfer to helium at 5.52 MPa. The microspheres were returned to the top of the blanket pneumatically. Tritium breeding was done in a region surrounding the particulate region.

Figure 6.2 Flowing UC Blanket Concept for Fissile Fuel Production and Coal Gasification.

The primary motivation for using solid microspheres is as follows:

1. Ceramics have no interaction with the magnetic field
2. The absolute pressure can be low;
3. Ceramic materials are generally more desirable for high temperature;
4. Continual processing of microspheres enhances removal and reduces inventory of tritium (or removal of other products);
5. The microspheres can provide high plasma coverage.

The primary design issues are concerned with defining a system which can deliver thermal energy at the desired high temperature and breed tritium for fusion driver refueling. Specifically they are concerned with:

1. Mechanical design and selection of materials that permit sufficiently high tritium breeding ratio (>1);
2. Reduction of temperature peaking effects by the radial power gradient;
3. Reducing heat generation and resulting temperatures in the blanket materials of construction;
4. Selection of materials with low internal heat generation and with sufficient strength properties at high temperature and neutron flux.
5. Sintering and related material issues of ceramic spheres or microspheres;
6. High temperature heat exchange to a working fluid;
7. Transport of bulk solids, flow control, return to blanket, wear, attrition.

6.1 OVERALL DESCRIPTION

The flowing microsphere unit cell shown in Figure 6.3 consists of two major components. The first is the blanket module which absorbs high energy neutrons from the plasma. Those neutrons provide the necessary heat generation and tritium breeding. The second major component is the integral heat exchanger which transfers heat from the hot Li_2O microspheres to the heat transport and power conversion system discussed in Section 7. Continual heat generation and removal is accomplished by allowing the microspheres to flow by gravity through the blanket and heat exchanger. The cooled microspheres are then mechanically returned to the top of the blanket to complete the flow loop. The microspheres are used for both the heat generation in the blanket and transport from the blanket.

6.1.1 Module Design Requirements and Assumptions

The primary functions of the blanket module are to breed tritium and to provide thermal energy for the hydrogen process while maintaining materials at suitable temperatures. The thermal heat transport and power conversion system, which interfaces with the thermo-chemical hydrogen production process, requires delivery of the working fluid at 1160 K with a return temperature of 615 K. The working fluid for delivery of heat to the chemical process is assumed to be sodium (or helium).

The geometric envelope of the blanket is defined by the geometry of the tandem mirror driver and field coil layout as shown in Figure 6.4. The principal dimensions of the blanket envelope are assumed to be a 1.4 m radius to the first wall and a 2.4 m radius to the outer wall. The blanket thickness is nominally 1.00 m and contains the first wall, Li_2O breeding region and reflector. Optimum dimensions depend upon a com-

Figure 6.3 Flowing Li_2O Blanket Concept Schematic for Thermal-Chemical Hydrogen Production.

Figure 6.4 Blanket Geometric Constraints Based on Fusion Driver Dimensions.

TABLE 6.1

BLANKET DESIGN PARAMETERS AND ASSUMPTIONS

Tandem Mirror Reactor

Wall Loading (14.1 Mev/n)	2.0 MW/m ²
First Wall Radius	1.4 m
Blanket Region Thickness	1.0 m

Blanket Module

Thermal Power (Delivered)	75 MW
Length	4.0 m
Coolant	Li ₂ O microspheres
Pressure	1 atm helium
T _{in}	655 K
T _{out}	1210 K
Flow Rate	48 kg/sec

Intermediate HX

Coolant	Na
Pressure	few atm
T _{out}	1160 K
T _{in}	615 K
Flow Rate	110 kg/sec

bination of neutronic, thermal-hydraulics and mechanical design analysis. The blanket module is assumed to be 4.00 m long. The first wall loading is assumed to be 2.0 MW/m^2 for this design study. Table 6.1 summarizes the basic design parameters and assumptions and Figure 6.3 illustrates the overall design concept and functional requirements.

6.1.2 Blanket Design Options

The radial power distribution in fusion blankets is material dependent and spatially nonuniform--it generally decays "exponentially" through a given material. This has an important effect on the design of the flowing microsphere blanket where it is desired to have uniform microsphere exit temperature. The primary result is that severe temperature peaking in the structure and particles occurs unless some design feature is introduced to reduce its effect. The basic options for reducing the peaking due to the radial power distribution are: 1) use multiple flow zones such that each zone has the same average power-to-flow ratio; 2) provide lateral mixing; 3) minimize the amount structural material; and 4) use materials with low heat generation (low neutron capture). This minimization of temperature peaking is very important for this application where the average exit microsphere temperatures must be on the order of 1200 K. Many high temperature alloys of construction lose their strength at or beyond this temperature.

Two microsphere blanket concepts are investigated in an attempt to reduce temperature peaking. The first is a ten-zone baffled flow region where both uncooled and cooled baffles are considered. The second is a three-zone cooled tube concept where static mixers are used to reduce temperature gradients in the flowing microsphere bed.

Baffled Flow Channel Concept

In this concept the blanket consists of an independently cooled first wall structure and ten Li_2O zones followed by a graphite reflector as shown in Figure 6.5. Ten flow zones are used to maintain the radial power induced temperature peaking to less than about 1.25. The baffled flow channels must both surround the plasma and be located inside the bore of the magnetic field coils. This requires special sloping and arrangement of channels flowing into and out of the bore of the magnet. Thus, the blanket has high plasma coverage except for reduced coverage in the region above and below the plasma which is next to the coils. Additional coil shielding may be necessary near the coils. The channels are sloped at the angle of sliding. Flow control orifices are located at the bottom of each channel. A brief discussion of particulate flow is included in Appendix 6D.

Cooled Tube Blanket

This concept is motivated by the desire to move the flow baffle constructional components out of the high temperature Li_2O . The cooled tube blanket consists of flow tubes which surround the plasma as shown in Figure 6.6. Each flow tube consists of: a) an outer structural support tube; b) a coolant flow annulus; c) insulation; d) an inner protective liner; and e) the flowing Li_2O . Each tube also contains an internal static flow mixer to help equalize the Li_2O temperature. Eccentric placement of the insulation and coolant channel are for reasons of heat generation and are discussed later.

The tubes are nested into the space surrounding the plasma but inside the field coils. This requires the special tube

Axis Elevation Side Elevation

Figure 6.5 Schematic Description of a Ten-Zone Flow Microsphere Baffled Blanket.

Figure 6.5 Continued. Schematic Description of a Ten-Zone Flow Microsphere Baffled Blanket.

Figure 6.6 Schematic Description of a Four-Zone Coupled Tube Blanket

arrangement as shown in Figure 6.6. A triangular pitch is used to reduce neutron streaming and to provide maximum blanket coverage. The region above and below the plasma inside the field coils has less complete coverage and may need additional coil shielding.

6.1.3 Heat Exchanger Design Options

A heat exchanger is necessary to transfer heat from the microspheres to the working fluid of the chemical process. Because of the high temperatures involved, it is desirable to use a low pressure fluid to reduce stresses on the pressure boundaries of the heat exchanger. This could be done with low pressure gases or with liquid metals. The working fluid is presently taken to be sodium. Helium has also been considered; however, it does not appear feasible because the piping pressure drop and associated pumping power is large for reasonably sized pipes at a few atmospheres of helium pressure. At higher pressures helium may be the best choice.

The functional purpose of the heat exchanger is to heat the sodium from 615 K to 1160 K. The heat transfer could be done through an intermediate wall or by direct contact with the Li_2O microspheres. Both have been considered and have their advantage of a direct contact heat exchanger is its reduced size. The primary advantage of the intermediate wall (shell and tube) is tritium isolation from the heat transport loop.

Figure 6.7 shows a conceptual design arrangement of the shell and tube heat exchanger. The design concept is the same as that reported for SOLASE⁽³⁾ and uses fluted tubes to increase the heat transfer area. The heat exchanger is rather large

Figure 6.7 Conceptual Design of Shell and Tube Heat Exchanger

Figure 6.8 Conceptual Design of Direct Contact Heat Exchanger

because of the low heat transfer coefficient to the microspheres. Heat transfer is augmented with the fluted tube arrangement by increasing the heat transfer area.

The alternate concept is a direct contact heat exchanger where the microspheres exchange heat by direct contact as they fall through a pool of liquid sodium. The heat exchanger is shown schematically in Figure 6.8. The heat exchanger size is reduced substantially ($\sim 2x$) because of the high heat transfer coefficient between the microspheres and liquid sodium.

6.2

BLANKET DESIGN ANALYSIS

Blanket design for a given concept is an iterative process between neutronics, thermal-hydraulics, materials and structural analysis. The discussion presented here represents the results of several scoping calculations to identify the capabilities and limitations of the two blanket design concepts. Emphasis has been placed on neutronics, thermal-hydraulics, and materials. No structural analysis has been performed.

6.2.1 Neutronics

A general discussion of the neutronics variables, types of results of interest, and the calculational methods used is given in Part 5.4 of Section 5. In this section the neutronics modeling of and results for the flowing microsphere blanket are presented. Two Li_2O flowing microsphere approaches have been considered, the baffled blanket and the tube blanket. The motivation behind these designs and detailed descriptions of them are given in Sections 6.1.1 and 6.1.2. It is convenient to treat the neutronics of these two approaches separately. Neutronics calculations were also performed to assess radiation damage to the first wall and to determine the performance of the blanket/shield for protection of the superconductor.

Neutronics Model - Baffled Blanket

Initial scoping calculations were performed for a 3-zone baffled blanket to determine the relative power density in the blanket materials. It was found from these calculations that the radial power gradient led to excessive temperature peaking in the Li_2O regions. For that reason final calculations were performed for a 10-zone blanket to reduce the power peaking to about 1.2. Results for both cases are presented for comparison.

The composition of a three-zone baffled blanket is given in Table 6.2.

The first wall is a composite structure consisting of an inner wall 2 mm thick followed by a single bank of tangent tubes having an OD of 6 mm and a wall thickness of 1 mm. The tubes in this case are filled with lithium and are followed with an outer wall 2 mm thick. The structure throughout the blanket is assumed to have the neutronics properties of stainless steel although it is not a suitable material for use at these temperatures. The tritium

TABLE 6.2

COMPOSITION OF THREE-ZONE BAFFLED BLANKET FOR NEUTRONIC ANALYSIS

<u>Zone</u>	<u>Outer Radius (m)</u>	<u>Composition</u>
1	1.500	Vacuum (source O.R. = 0.75 m)
2	1.502	Stainless Steel (St. St.)
3	1.508	St. St. & Lithium 6 mm OD tubes, wall thickness 1 mm, filled with Li coolant
4	1.510	St. St.
5	1.770	Li ₂ O (60 v/o)
6	1.772	St. St.
7	2.032	Li ₂ O (60 v/o)
8	2.034	St. St.
9	2.294	Li ₂ O (60 v/o)
10	2.296	St. St.
11	2.596	Graphite

breeding ratios and power distributions are not strongly dependent upon the choice of structure. Radiation damage is treated in the sections below covering the tube blanket including more realistic structural material options.

The breeding region consists of three zones of Li_2O each 0.26 m thick and separated by 2 mm of structure. The packing fraction was assumed to be 60%. A 0.30 m graphite reflector is located outside the breeding region. A shield was not included in order to save computer time. Shielding calculations are included in the sections below covering the tube blanket in order to determine heating rates and radiation damage rates in the superconductor.

As noted below in the section describing results, the three-zone design suffers from severe power peaking problems. For this reason a similar design in which the three 0.26 m Li_2O zones are replaced with ten zones of thickness 0.08 m was analyzed. The composition and zone identifications for this blanket are given in Table 6.3.

Analysis Results - Baffled Blanket

The zone heating and tritium breeding results for the three-zone case are given in Table 6.4. A fairly small fraction (6%) of the energy is deposited in the composite first wall (zones 2-4). Most of the energy (65%) is produced in the innermost breeding zone (zone 5).

The overall tritium breeding ratio of 1.387 is more than adequate although the known error in the ^7Li reaction will cause a "corrected value" to be roughly 1.35. If no further compromises were required and a minimum value of 1.1 is considered acceptable, the minimum effective blanket coverage that could be tolerated would be approximately 80%.

TABLE 6.3

COMPOSITION OF TEN-ZONE BAFFLED BLANKET FOR NEUTRONIC ANALYSIS

<u>Zone</u>	<u>Outer Radius (m)</u>	<u>Composition</u>
1	1.500	Vacuum (Source O.R. = 0.70 m)
2	1.502	Stainless Steel (St. St.)
3	1.508	St. St. & Lithium 6 mm OD tubes, wall thickness 7 mm, filled with Li coolant
4	1.510	St. St.
5	1.590	Li ₂ O (60 v/o)
6	1.592	St. St.
7	1.672	Li ₂ O (60 v/o)
8	1.674	St. St.
9	1.754	Li ₂ O (60 v/o)
10	1.756	St. St.
11	1.836	Li ₂ O (60 v/o)
12	1.838	St. St.
13	1.918	Li ₂ O (60 v/o)
14	1.920	St. St.
15	2.000	Li ₂ O (60 v/o)
16	2.002	St. St.
17	2.082	Li ₂ O (60 v/o)
18	2.084	St. St.
19	2.164	Li ₂ O (60 v/o)
20	2.166	St. St.
21	2.246	Li ₂ O (60 v/o)
22	2.248	St. St.
23	2.328	Li ₂ O (60 v/o)
24	2.330	St. St.
25	2.630	Graphite

TABLE 6.4

ZONE HEATING AND TRITIUM BREEDING FOR THREE-ZONE BAFFLED BLANKET

<u>Zone</u>	<u>Heating (MeV/n)</u>			<u>Tritium Breeding (T/n)</u>		
	<u>Neutron</u>	<u>Gamma</u>	<u>Total</u>	<u>T₆</u>	<u>T₇</u>	<u>T</u>
1	0	0	0	0	0	0
2	0.098	0.152	0.250	0	0	0
3	0.221	0.204	0.425	0.005	0.007	0.012
4	0.085	0.150	0.235	0	0	0
5	8.869	1.601	10.470	0.511	0.377	0.888
6	0.011	0.074	0.085	0	0	0
7	2.549	0.669	3.218	0.278	0.066	0.344
8	0.002	0.030	0.032	0	0	0
9	0.851	0.260	1.111	0.132	0.011	0.143
10	2.6(-4)	0.012	0.012	0	0	0
11	0.033	0.135	0.135	0	0	0
TOTAL	12.719	3.287	16.006	0.926	0.461	1.387

It may be desirable to replace the lithium coolant in the first wall with some other coolant such as sodium. An additional analysis was performed for which the composition was identical except that sodium replaced lithium in the first wall. Neither the tritium breeding nor the heating results were significantly affected. The result with sodium is not surprising since, as shown in Table 6.4, the first wall contribution to tritium breeding is unimportant.

The power density for the three-zone case is plotted as a function of radius in Figure 6.9 for a wall loading of 1 MW/m^2 . The results can be scaled linearly for other assumed wall loadings. The spatial dependence is approximately exponential with a relaxation length of about 0.2 m. The power densities plotted are per unit volume for the zone. However, the power is actually concentrated in the Li_2O particles so that the localized value in a particle is greater by a factor $(0.6)^{-1} = 1.67$. Furthermore, the power density peaking factors (peak/average) for the three Li_2O zones are large, 1.88, 1.77, and 1.59, respectively.

The zone heating and tritium breeding results for the ten-zone baffled blanket are given in Table 6.5 and the power density is plotted in Figure 6.10. All of these results are quite consistent of Li_2O between the first wall and the graphite reflector. The thin structural membranes have only a minor effect on either heating or tritium breeding. The greater amount of structure required for the additional subdivisions in the ten-zone case produces a slightly greater heating rate and a slightly lower tritium breeding ratio.

The major difference between these two blankets is the reduced power peaking produced by small zone sizes. The results for the ten-zone cases are given in Table 6.6. The anomalously large value for the outermost zone results from localized thermal flux peaking near the reflector boundary. The power density is low in this region so appropriate design modifications would not be difficult.

Fig. 6.9

TABLE 6.5

ZONE HEATING AND TRITIUM BREEDING FOR TEN-ZONE BAFFLED BLANKET

Zone	Heating (MeV/n)			Tritium Breeding (T/n)		
	Neutron	Gamma	Total	T_6	T_7	T
1	0	0	0	0	0	0
2	.098	.154	.252	0	0	0
3	.221	.208	.429	.005	.007	.012
4	.084	.150	.235	0	0	0
5	4.067	.662	4.729	.182	.137	.319
6	.041	.123	.164	0	0	0
7	2.648	.513	3.161	.166	.106	.272
8	.022	.096	.118	0	0	0
9	1.786	.337	2.123	.148	.062	.210
10	.012	.072	.084	0	0	0
11	1.213	.236	1.449	.117	.036	.153
12	.007	.053	.060	0	0	0
13	.825	.208	1.033	.092	.020	.112
14	.004	.034	.038	0	0	0
15	.559	.150	.709	.070	.012	.082
16	.002	.028	.030	0	0	0
17	.378	.108	.486	.052	.007	.059
18	.001	.020	.021	0	0	0
19	.256	.078	.334	.038	.004	.042
20	.001	.014	.015	0	0	0
21	.178	.056	.234	.024	.002	.026
22	3.2(-4)	.010	.010	0	0	0
23	.189	.041	.230	.034	.001	.035
24	1.8(-4)	.008	.008	0	0	0
25	.023	.098	.121	0	0	0
TOTAL	12.616	3.457	16.073	0.928	0.394	1.322

Fig. 6.10

TABLE 6.6
POWER DENSITY PEAKING FACTORS FOR TEN-ZONE BAFFLED BLANKET

<u>Zone</u>	<u>Peak/Avg.</u>
5	1.26
7	1.21
9	1.22
11	1.21
13	1.21
15	1.21
17	1.21
19	1.20
21	1.19
23	1.45

In summary, the Li_2O baffled blanket concept has some very attractive features. In order to keep power peaking factors at reasonable values the blanket must be highly subdivided. Ten zones, however, appear to be an adequate number and the resulting amount of structure is not excessive. Adequate tritium breeding ratios can easily be achieved. The baffles between zones in such a design will, if not cooled, operate at very high temperatures. Stainless steel is clearly not a viable candidate and it may be difficult to identify a credible choice. It was primarily for this reason that the cooled tube design was considered.

Neutronics Model - Cooled Tube Blanket

The neutronic description of a cooled tube blanket design is given in Table 6.7, and a physical description is shown in Figure 6.4. The design includes a separately cooled first wall similar to that used in the baffled blanket design. The Li_2O microspheres flow through four tube layers each of which consists of an outer structural support tube, a helium-cooled flow annulus, an insulation region, and the Li_2O flow tube. A discussion of the optimization of the design is given in Section 6.2.2. It should be noted that the eccentricity of the Li_2O flow tube design described in Section 6.2.2 has not been modelled neutronically. Similarly, the entry and exit angles of the flow tubes at the top and bottom of the blanket have been ignored, i.e., the dimensions given in Table 6.7 are assumed everywhere. Neither of these approximations should significantly affect the accuracy of the neutronics results, although the tube entry and exit regime are three-dimensional design problem areas which must include adequate shielding for the superconducting coils.

Analysis Results - Cooled Tube Blanket

A summary of neutronics performance for five different cooled tube blankets is given in Table 6.8. Cases 4 and 5 differ in that the latter is optimized for thermal-hydraulic design as noted above.

TABLE 6.7

COMPOSITION OF COOLED-TUBE BLANKET FOR NEUTRONIC ANALYSIS

<u>Zone</u>	<u>Outer^a Radius (m)</u>	<u>Composition^b</u>
1	1.500	Vacuum (Source O.R. = 1.35 m)
2	1.502	First Wall Complex - tube structure (inner)
3	1.512	First Wall Complex - coolant
4	1.513	First Wall Complex - tube structure (outer)
5	1.515	Tube Layer 1 - structure (inner)
6	1.533	Tube Layer 1 - coolant
7	1.539	Tube Layer 1 - insulation
8	1.687	Tube Layer 1 - Li ₂ O packed spheres
9	1.693	Tube Layer 1 - insulation
10	1.711	Tube Layer 1 - coolant
11	1.713	Tube Layer 1 - structure
12	1.715	Tube Layer 2 - structure (inner)
13	1.725	Tube Layer 2 - coolant
14	1.732	Tube Layer 2 - insulation
15	1.867	Tube Layer 2 - Li ₂ O packed spheres
16	1.874	Tube Layer 2 - insulation
17	1.885	Tube Layer 2 - coolant
18	1.886	Tube Layer 2 - structure
19	1.883	Tube Layer 3 - structure (inner)
20	1.895	Tube Layer 3 - coolant
21	1.906	Tube Layer 3 - insulation
22	2.041	Tube Layer 3 - Li ₂ O packed spheres
23	2.051	Tube Layer 3 - insulation
24	2.058	Tube Layer 3 - coolant
25	2.060	Tube Layer 3 - structure
26	2.062	Tube Layer 4 - structure (inner)
27	2.066	Tube Layer 4 - coolant
28	2.084	Tube Layer 4 - insulation
29	2.236	Tube Layer 4 - Li ₂ O packed spheres
30	2.254	Tube Layer 4 - insulation
31	2.258	Tube Layer 4 - coolant
32	2.260	Tube Layer 4 - structure
33	2.460	Reflector (Graphite)
34	3.410	Shield (St. St. - B ₄ C (50%))
35	3.430	Void
36	3.480	Dewar
37	3.980	Superconductor
38	4.130	Dewar

^aCase 5, Table 6.8: dimensions vary slightly for different cases

^bTube layer structure either Incalloy 800 or Niobium-Alumina (50%) composite

This causes Case 5 to have a decreasing void fraction and a greater amount of structure in the outer portions of the blanket.

The results in Table 6.8 reflect the fact that these configurations have a relatively high volume fraction of structural material ($\sim 40\%$). The tritium breeding ratios are marginal. It is probably necessary to use lithium as the first wall coolant in order to maximize T, but even in this case the result is probably not adequate, especially since subsequent correction in the ${}^7\text{Li}$ cross section data will lower it further.

The values of the blanket energy multiplication factor, M, tend to be rather low compared with fusion reactors generally. This result is due in part to the fact that these configurations are relatively optically thin from a neutronics standpoint. For example, in Case 5 an additional 2.25 MeV/n is deposited in the shield. If the blanket were to be made thicker and this energy recovered, the M value would increase by 16% to 1.12. Such a design modification is clearly feasible without increasing the overall size. As noted below, the shield thickness can be decreased, thus preserving, or perhaps even decreasing, the dimensions of the blanket + shield combination.

The value of the fraction of the energy recoverable at the peak temperatures are lower than those for the Li/Na cauldron designs described in Section 5. This is a further consequence of the large amount of structure in the cooled-tube designs. The values in Table 6.8 are, however, larger than those of the high temperature fraction typically reported for synfuel blankets in the literature.^{1,2}

The radial distribution of the power density is shown in Fig. 6.11 for Case 5. The distributions for the other cases are similar. These results are based on a wall loading of 1 MW/m^2 and will, of course, increase linearly with higher wall loadings.

TABLE 6.8

NEUTRONIC PERFORMANCE SUMMARY OF COOLED TUBE BLANKETS

<u>Case</u>	<u>Structural Material</u>	<u>1st wall Coolant</u>	<u>T</u>	<u>M</u>	<u>High Temp.^a Fract.</u>	<u>Energy Deposition (MeV/n) 1st Wall/Tube Layers 1-4</u>
1	Inc-800	Na	1.03	1.12	0.64	0.87/7.45/3.99/2.29/1.34
2	Nb-Al ₂ O ₃ (composite)	Na	0.97	1.05	0.70	0.84/6.59/3.80/2.26/1.50
3	Nb w/Al ₂ O ₃ coating	Na	1.02	1.01	0.79	0.82/5.83/3.83/2.23/1.63
4	Inc-800	Li	1.07	1.12	0.64	0.93/7.39/3.98/2.29/1.34
5	Inc-800	Li	1.01	0.96	0.74	0.87/5.06/3.62/2.42/1.53

^aThe fraction of the power recovered at peak temperatures

POWER DENSITY VERSUS RADIUS FOR FOUR LAYER COOLED-TUBE BLANKET

Fig. 6.11

The power deposited in the superconductor is approximately 8×10^{-8} of the total. This easily satisfies the criterion of less than 10^{-6} of the total in order to avoid unacceptable costs of power for refrigeration. Similarly, the dose rate to the superconductor insulation has been computed and found to be acceptably low. If G-10CR, an epoxy resin with SiO_2 added,³ is used, the maximum dose rate is 3.0×10^3 Gy/yr (3.0×10^3 rad/yr) at a wall loading of 1 MW/m^2 . If the criterion of a total dose less than 5×10^7 Gy is applied, then the lifetime of the superconductor is not limited in this respect.

All of the Li_2O blanket calculations were performed using stainless steel as the first wall material. This choice does not significantly affect the neutronics performance of the remainder of the blanket. Radiation damage rates can, however, depend heavily upon the choice of first wall material. The maximum atom displacement rates and hydrogen and helium production rates are listed in Table 6.9 for Case 5 as well as other relevant designs. As noted in Section 5.4 stainless steel and vanadium alloys behave almost identically with respect to displacement rates. Gas production rates are, however, much higher for stainless steel as compared with vanadium alloys. For this reason, and others as well,⁴ vanadium-titanium would appear to be an attractive first wall material for the Li_2O blanket design.

In summary, of the two Li_2O blanket configurations studied, the baffled design is neutronically superior to the cooled-tube. The large fraction of structure in the latter invokes penalties in both tritium breeding and the fraction of energy recoverable at peak temperatures. The blanket energy multiplication factor could, however, be increased by increasing blanket thickness and correspondingly reducing shield thickness. Such a change could be made without exceeding limitations on either superconductor heating or radiation damage to the superconductor insulation.

TABLE 6.9
FIRST WALL RADIATION DAMAGE RESULTS^a

<u>Blanket</u>	<u>Material</u>	<u>Displacement Rate (DPA/yr)</u>	<u>H Prod. Rate (Appm/yr)</u>	<u>Hc Prod. Rate (Appm/yr)</u>
Li ₂ O-cooled tube	Stainless steel	12.6	525	167
Li/Na Cauldron	Vandium- Titanium (10%)	12.0	251	58
Li(Ref. 4)	Stainless steel	11.3	532	147
Li(Ref. 4)	Vandium- Chromium (15%) Titanium (5%)	11.3	240	57

^aMaximum values (at inner surface), $W = 1.0 \text{ MW/m}^2$

REFERENCES FOR SECTION 6.2.1

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3. D.J. Dudziak, EBTR Study, Los Alamos Scientific Laboratory, Personal Communication, Aug. 26, 1980.
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6.2.2 Thermal-Hydraulics Analysis

The purpose of the thermal-hydraulic analysis is to determine the thermal and temperature performance within the blanket. The starting point is the heat generation in the blanket materials as defined by the neutronics analysis. The radial power gradient is particularly significant since it is highest at the inner radius but can decrease by two orders of magnitude over the blanket thickness. The implication is that the blanket must be divided into radial flow zones and the flow through each zone controlled if uniformity of average microsphere exit temperature is to be obtained. Complete uniformity is an ideal which is not attainable; however, the degree of nonuniformity (temperature peaking) can be controlled by the number of flow zones and/or by mixing of the microspheres. Two different blanket concepts are considered.

Baffled Blanket

In this concept the Li_2O flow space is divided radially into flow zones and the flow through each zone is controlled to give the same average exit temperature as illustrated in Figure 6.5. Ten zones were selected to limit temperature peaking to about 1.25. The neutronics analysis provided the heat deposition in the blanket. Table 6.10 shows that the majority (94%) of the power is generated in the Li_2O and the imbedded structure. A comparatively small amount (6%) of power is generated in the first wall and a very small amount (1%) is generated in the reflector and shield. The average flow of Li_2O through the blanket is at a rather low velocity of about 0.7 cm/sec.

Table 6.11 summarizes additional analysis results for the ten zone Li_2O blanket. The items of interest here are the values

TABLE 6.10

THERMAL-HYDRAULIC ANALYSIS RESULTS FOR TEN-ZONE Li_2O BLANKET

	First Wall	Li_2O	Graphite	Total (or average)
Inner Radius, m	1.400	1.410	2.230	1.400
Outer Radius, m	1.410	2.230	2.530	2.530
Volume, m^3 (a)	0.353	37.508	17.945	55.806
Power Fraction	0.057	0.935	.008	1.0000
Power, Mw	4.57	75.04	0.61	80.22
Power Density, Mw/m^3	12.95	2.00	0.03	1.44
Coolant		Li_2O		-
T_{in}, K		665		-
T_{out}, K		1210		-
Flow Rate, kg/sec		48		-
Flow Area, m^2		6.40		-
Avg. Velocity, m/sec		.006		-

(a) Module length of 4.000 m.

TABLE 6.11

THERMAL-HYDRAULIC ANALYSIS RESULTS FOR SELECTED Li_2O ZONES

	Zone 1	Zone 4	Zone 10	Total (or avg.)
Inner Radius, m	1.410	1.656	2.148	1.41
Outer Radius, m	1.492	1.738	2.230	2.23
Volume, m^3	2.990	3.497	4.511	37.508
Flow Area, m^2 (c)	0.64	0.64	0.64	6.40
Power Fraction (a)	0.304	0.094	0.015	0.933
Power, Mw	24.41	7.53	1.19	75.04
Power Density, Mw/m^3	8.17	2.154	0.26	2.00
T_{in} , K	665	665	665	665
T_{out} (avg.), K	1210	1210	1210	1210
Flow Rate, kg/sec	15.5	4.8	0.8	17.7
Avg. Velocity, m/sec	.020	.006	.001	.006
Temp Peaking Factor (d)	1.28	1.21	1.45	-
Max T_{out} (b)	1360	1320	1455 (e)	-

(a) Fraction of total module power.

(b) melting temperature of Li_2O is 1973K.

(c) 0.002 m radial allowance for structure per zone.

(d) $\Delta T_{\text{max}}/\Delta T_{\text{avg}}$ along channel, no flow or mixing factors included.

(e) Zone 10 peaking from graphite reflection can be reduced by rezoning.

of power, flow and temperature for selected zones. Since the power density decreases "exponentially" with radius and it is desired to produce a uniform exit temperature, the Li_2O flow rate for each zone is set in proportion to its power. The results give flow velocities ranging from 1.34 to 0.15 cm/sec from Zone 1 to Zone 10, respectively. Temperature peaking due to the power gradient is 1.28 near the first wall, decreases to 1.21 in the inner zones and increases to 1.45 in Zone 10 due to neutron reflection from the graphite as shown in Figure 6.12. The important result of this analysis is the maximum exit temperature of the Li_2O . The average exit temperature* is 1210 K, but with power peaking and assumed "plug" flow, the maximum temperatures would range from 1360 to 1445 as shown in Figure 6.12. These are high temperatures compared to the Li_2O melting temperature of 1973 K. Sintering of the microspheres is of concern since that could lead to agglomeration and eventual channel plugging. Sintering of the microspheres is discussed in Section 6.2.3.

The division of the blanket into flow zones requires "baffles" which must operate in the high temperature environment of the flowing Li_2O . In addition, the baffle structure is heat generating and that heat must be transferred to the Li_2O if there is no baffle cooling. An estimate of the baffle temperature was made in a radially oriented baffle in Zone 1. Another temperature estimate made for the baffle between Zone 1 and Zone 2. In both cases the baffle was assumed to be 2 mm thick and have a thermal conductivity typical of niobium (53 W/m-C). The heat transfer from the wall was calculated for fully developed heat transfer by using $\text{Nu} = 12$ based on "plug" flow between the baffles. This gave a heat transfer coefficient of $77 \text{ W/m}^2\text{-C}$ based on a particulate bed thermal conductivity of 0.82 W/m-C and a hydraulic diameter of 0.128 m.

*Temperatures are based on 1160 K exit from module heat exchanger.

Figure 6.12 Exit Temperature Distribution from Ten-Zone Blanket.

The peak power density in the radial wall in Zone 1 was calculated to be 10.9 MW/m^3 from the neutronics calculation. The resulting temperature rise above the particulate average temperature was about 140°C where the temperature rise in the metal was less than 1°C . The heat transfer is dominated by the conductance across the interface between the baffle and the microspheres. The radial baffle could therefore be expected to be about 140°C higher than the Li_2O temperature as shown in Figure 6.13.

Heat transfer across the baffle separating Zone 1 and 2 was calculated using the same information except the temperature on the Zone 2 side was 1400°K and on the Zone 1 side it was 1100°K . The results gave a maximum baffle temperature of 1390°K or 10°C lower than the hot side temperature. The hot side flux was 710 W/m^2 and the cold side flux was $22,000 \text{ W/m}^2$. The difference between these two is the heat flux from internal heat generation in the baffle. This result indicates that the zone separation baffles would operate at temperatures between those on each side of the baffle, but tend toward the higher side in regions of high power density. In the outer zones, where the power density is low, there would be greater heat transfer between zones and reduced temperature peaking.

The maximum operating temperature of the baffles can be higher than the Li_2O temperature and this aggravates the problems of mechanical design and material selection. A cooled baffle could be of possible benefit, however, it would remove heat from the Li_2O . An estimate of the heat loss was made by assuming heat transfer to a cooled baffle with a temperature difference of 400°K to limit the baffle temperature to about 1000°K . Using the same heat transfer parameters as before, the wall heat flux is about $31,000 \text{ W/m}^2$. The wall area for

Figure 6.13 Illustration of Temperature Distributions through Uncooled Baffle Sections.

a typical zone is about 140 m^2 , therefore, the heat loss is 4.3 MW per zone. This heat loss is about 50% of an average zone power and substantially higher than the individual power in the outer zones. It is, therefore, not feasible to consider cooled baffles without the use of insulation.

The heat loss could be reduced using a cooled structure that has lower surface area and heat transfer coefficient. This could be accomplished by using larger size flow zones and thermal insulation but this would require lateral mixing to reduce temperature peaking from the radial power gradient. This is considered in the next section where static mixers are assumed to mix the microspheres.

Conclusions regarding the baffled blanket are as follows:

1. The technical feasibility is closely tied to materials performance at high temperature. High temperature structural materials are required to form the flow channel dividers. Some of the structural divider temperatures are higher than the surrounding Li_2O .
2. Sintering, agglomeration or vapor transport of the slowly flowing Li_2O is a serious question.
3. Multiple radial flow zones are necessary to reduce temperature peaking. Flow control is necessary to achieve uniformity of average exit temperature from the blanket.
4. The independently cooled first wall and structural envelope can operate at intermediate temperatures ($\sim 700 \text{ K}$ maximum) and should pose no particular problem.
5. Abrasion and wear of the flow channel dividers needs to be investigated.
6. Structural analysis to determine stresses from non-uniform heating is also required.

Cooled Tube Blanket

The cooled tube concept was considered as an alternate to the baffled blanket to reduce the temperature of the flow channels carrying the Li_2O microspheres. The basic idea of this concept shown in Figure 6.6 is to contain the hot Li_2O in an insulated tube which is cooled by helium. Few flow zones could be used by using static flow mixers to reduce the power gradient induced temperature peaking.

The neutronics analysis of this concept for four tube layers used a one dimensional analysis to define the energy deposition in the various material components of the blanket. The results of that analysis are summarized in Table 6.12. The fraction of energy deposited in each material was obtained by normalizing the computed value of energy deposited per source neutron. The actual power was based on a first wall loading of 2 MW/m^2 and a blanket energy multiplication of 1.12. The important result from Table 6.12 is that the fractional energy deposited in the Li_2O and structure changes substantially from that of the baffled blanket where nearly all of the heat was generated and removed by the flowing Li_2O . The magnitude of heat generated in the structure and removed by the coolant in the tube blanket is sufficiently large that it would alter the energy transport and conversion concepts presented in Section 7.0.

A summary of more detailed thermal-hydraulic analysis results for the four tube layer blanket is presented in Table 6.13. Heat is primarily removed from the blanket in two ways. The first is the structural cooling stream. It consists of: 1) all heat generated in the tube wall; 2) heat generated in the insulation; and 3) the heat transfer through the insulation from the Li_2O . This amounts to 27% of the total blanket heat generation or 21.2 MW. The second heat removal is

TABLE 6.12

OVERALL HEAT DEPOSITION AND
HEAT TRANSFER IN FOUR-LAYER COOLED-TUBE BLANKET

	<u>Energy Deposited</u>		<u>Energy Removed</u>	
	<u>Fraction</u>	<u>MW</u>	<u>Fraction</u>	<u>MW</u>
First Wall	0.054	4.3	0.054	4.3
Tube Walls	0.113	9.0		-
Coolant Channels	nil	nil	0.267	21.2
Insulation	0.046	3.6		-
Lithium Oxide	0.637	50.6	0.529	42.0
Graphite/Shield	<u>0.150</u>	<u>11.9</u>	<u>0.150</u>	<u>11.9</u>
Totals	1.000	79.4	1.000	79.4

TABLE 6.13

THERMAL-HYDRAULIC ANALYSIS RESULTS
FOR FOUR-LAYER COOLED-TUBE BLANKET

Item	Tube Layer				Average or
	1	2	3	4	Total
Tube Wall:					
Power Fraction	0.047	.032	.021	.013	.113
Power, MW	3.73	2.54	1.66	1.03	8.96
Volume, m ³	0.236	0.267	0.292	0.319	1.114
Average Power Density MW/m ³	15.8	9.5	5.7	3.2	8.0
Insulation:					
Power Fraction	.012	.012	.011	.011	.046
Power	0.95	0.95	0.87	0.87	3.64
Thickness, mm	6	8	12	18	-
Volume, m ³	0.71	1.07	1.75	2.85	6.38
Average Power Density MW/m ³	1.3	0.9	0.5	0.3	.6
Heat Loss Flux (q'') MW/m ²	0.025	0.019	0.013	0.008	-
Area, m ²	118	133	146	158	-
Heat Loss (q), MW	2.95	2.52	1.90	1.26	8.63
Heat Load to Coolant:					
Power, MW	7.63	6.01	4.43	3.16	21.23
Coolant	He	He	He	He	He
T _{in} , K	400	400	400	400	400
T _{out} , K	900	900	900	900	900
Flow Rate, kg/sec	2.9	2.3	1.7	1.2	8.1
Flow Area (@ 200 m/s, 1 atm), m ²	0.20	0.16	0.12	0.08	-
Channel Thickness, mm	8	6	5	3	-
Channel Pressure Drop, atm	0.16	0.16	0.16	0.16	0.16

Lithium Oxide:

Power Fraction	0.260	0.184	0.120	0.073	0.637
Power, MW	20.64	14.61	9.53	5.80	50.58
Heat Loss (q), MW	2.95	2.52	1.90	1.26	8.63
Net Power, MW	17.69	12.09	7.63	4.54	41.95
T _{in} , K	665	665	665	665	665
T _{out} , K	1210	1210	1210	1210	1210
Flow Rate, kg/sec	11.2	7.7	4.8	2.9	26.6
Flow Diameter, m	0.168	0.168	0.162	.154	-
Flow Area, m ²	0.89	0.89	0.82	.75	3.35
Average Velocity, m/sec	0.010	0.0070	.0048	0.0031	0.0064
Peaking Factor, (estimated) ($\Delta T_{max}/\Delta T_{avg}$)	1.10	1.10	1.10	1.10	-
T _{max} , K	1265	1265	1265	1265	-

Dimensions:

Number of tubes/row	40	40	40	40
Diameter, m	.2	.2	.2	.2
Centerline Radius, m	1.50	1.67	1.85	2.02
Length	4.7	5.3	5.8	6.3
Surface Area, m ²	118	133	146	158
Tube Thickness, mm	2	2	2	2

by the Li_2O . It represents 53% of the total or 42.0 MW. The total of these two is 80% of the blanket energy deposition or 63.2 MW. This compares to 94% and power of 75.0 MW for the baffled blanket.

Table 6.13 also presents the results for each tube layer. The strong effect of the radial power gradient is shown for all components. Nearly one-half of the energy is deposited in materials of the first tube layer and a comparatively small fraction is generated in the fourth layer. The Li_2O microsphere rates and velocity are in proportion to the tube powers and range from 0.010 to 0.003 m/sec.

The helium coolant flow rate is also in proportion to the coolant channel heat load but the flow area is adjusted to maintain an average velocity of 200 m/sec. At one atmosphere, the pressure drop through the coolant channel is estimated at 0.16 atm. The major pressure drop would probably occur in the manifold and its pressure drop have not been considered for this preliminary analysis. An optimum design might benefit from somewhat higher helium pressure. The basic conclusion is that low pressure helium cooling of the structure appears feasible.

The helium flow and temperatures were selected with the thought of maintaining the structure temperature less than about 1000 K. The selected exit temperature of 900 K allows for a 100 C temperature difference between the coolant and structure. A more complete analysis would require optimization of, pressure, flow rate, heat transfer coefficients and coolant temperatures. This was not considered necessary for this preliminary analysis.

Heat transfer through the insulation in the cooled tube concept is a rather difficult three dimensional problem. It can, however, be approximated by a one dimensional solution for heat transfer through a heat generating plate with unequal boundary conditions (Appendix 6C). The analytical solution shows that the amount of heat transfer depends upon the conductance from the Li_2O through the insulation to the coolant and upon the heating contribution in the insulation. The following sketch shows some temperature profile possibilities.

Note that if the insulation is "too good", the peak temperature can occur in the insulation. Since that is not acceptable, the insulation thickness must be tailored for a given q'' and k .

An estimate of the heat transfer to and from the insulation layer can be made by considering a temperature difference from the inner and outer surface of the insulation. In that case $B_1 \rightarrow \infty$ and $B_2 \rightarrow \infty$ for the solution in Appendix 6C and the surface heat fluxes are the sum (or difference) of the heat generation and conduction term. They are given by

$$q''_{x=-a} = -q''_a - k \left(\frac{T_1 - T_2}{2a} \right)$$

and

$$q''_{x=a} = q''_a - k \left(\frac{T_1 - T_2}{2a} \right)$$

Note that $q''_{x=a}$ can be positive or negative depending on the magnitude of the two terms.

To maintain the hot side temperature at or below Li_2O temperature it is necessary that $q''_{x=a} \leq 0$; therefore

$$2a \leq \left(\frac{2k(T_1 - T_2)}{q''} \right)^{1/2}$$

When the equality is satisfied the heat flux

$$q''_{x=-a} = -2aq''$$

or

$$q''_{x=-a} = (2q''k(T_1 - T_2))^{1/2}$$

The implication of this result is that the insulation thickness must vary inversely with the square root of the power density.

Since the power density varies with radial distance from the plasma, the heat flux from the tube wall and insulation would

depend on both the radial distance from the plasma and the circumferential distance around the tube. The result would be nonuniform heating of the insulation and coolant. These effects could be reduced by using a variable thickness of insulation around the circumference. A variable channel thickness could also be used to produce a more uniform coolant temperature and the tube wall temperature to reduce thermal stresses.

For purposes of the results shown in Table 6.13, the insulation was assumed to be uniform thickness niobium "felt-metal"⁽¹⁾ with a thermal conductivity of 0.5 W/m-K. This is about two orders of magnitude lower than the value for solid niobium and appears feasible based on experience with other metals. The insulation was assumed to have a 0.15 solids fraction per unit volume. The temperature difference across the insulation was assumed to be 300 K. The insulation thickness was chosen such that the previous inequality was satisfied to assure that the insulation temperature would be less than the Li_2O temperature. Note that the average insulation thickness increases with each tube layer from the plasma.

The above approximations were used to obtain the estimate of the heat losses caused by heat transfer through the insulation in Table 6.13. A more complete analysis would require inclusion of surface heat transfer coefficients. The overall result is that the splits for heat removal from the structure and Li_2O change from that deposited by the amount of heat transferred across the insulation.

In conclusion the cooled tube concept has some advantages:

1. The temperature of the flow channel structure is less than the Li_2O temperature and the primary structural tube could be maintained at temperatures less than 1000 K.

2. There are only a few (four) radial zones.
3. Low pressure helium cooling of structure appears feasible.

There are also some unresolved problems and issues:

1. The Li_2O temperatures are still high implying that sintering, agglomeration, or vapor transport could be problems.
2. The flow distribution through inclined flow tubes containing static mixers has not been evaluated.
3. The effectiveness of the static mixers is not known for inclined tubes.
4. The fractional heat split between the Li_2O and structure alters the coupling to the synfuel process.
5. Questions of material performance and structural performance need to be considered.

As a final remark, mixing by fluidization of the microsphere bed is a possibility which has not been explored in this study but may warrant further consideration. If the proper fluidization conditions could be established, many problems of the radial gradient might be overcome. Fluidization might also be considered for return (or recirculation) of the particulates should a recirculation concept be retained.

6.2.3 Materials Consideration

As described in Section 6.2.2, the feasibility of the flowing microsphere blanket module is determined by the high temperature performance of the lithium oxide and the structural materials. The major issue is whether various material transport mechanisms will interfere with the desired flow behavior, either by causing the microspheres to adhere to each other (sinter) or by plugging the channel (e.g., condensation of volatiles in the low temperature portion of the path). The sintering issue also affects the tritium release behavior of the microspheres, since under conditions where inter-particle sintering is expected, intra-particle sintering could interfere with the transport of tritium to the microsphere surface. Additional issues are the chemical and mechanical interactions between the lithium oxide microspheres and the structural materials of the blanket module, and the selection of the latter.

6.2.3.1 Sintering

The approach initially taken was to assume the temperature limits, velocities, etc. derived from thermal-hydraulic considerations, and to assess the sintering question under these conditions. Since the results indicated a high probability of sintering, the effects of temperature reductions on other portions of the overall system were assessed. The sintering process has been expressed as a function of temperature and other input parameters to permit evaluation of the severity of the problem under revised conditions.

Previous studies of the particulate blanket concept have used a fraction of the absolute melting point⁽¹⁾ (0.60-0.67) as a sintering temperature limit, and have subsequently reduced this temperature to allow for the effect of radiation on diffusional processes⁽²⁾, or raised it in view of the use of protective coatings and large diameter particles⁽³⁾. Since models for the early stages of sintering exist in the literature, an attempt was made to collect the appropriate material property data for lithium oxide. Very few of the data required by the models were available; therefore, it was necessary to estimate some values and ignore the contributions of some mechanisms altogether e.g. viscous flow, due to the lack of a viscosity-vs-temperature curve. The probable effects of the fast neutron irradiation on the volume diffusivity have also been neglected due to lack of information. The predictions of the models were found to be rather insensitive to the estimates of material properties as compared to their sensitivity to e.g. particle radius and temperatures.

The material transport paths of the sintering models employed are illustrated in Figure 6.14. Sintering is considered to result from growth of a bond area ("neck") at the contact zone of two spheres. The rate of neck growth depends on the sum of the material transport rate due to the various mechanisms. The relevant equations for the neck growth processes are tabulated below:

$$\text{Volume diffusion (Kingery \& Berg)}^{(4)} \frac{x}{r} = (80\Omega D_{yt}/kTr^3)^{1/5}$$

$$\text{Surface diffusion (Kulczinski)}^{(5)} \frac{x}{r} = (28\gamma a^4 Dt/kTr^4)^{1/7}$$

$$\text{Grain Boundary diffusion (Coble)}^{(6)} \frac{x}{r} = (96\delta D_{yt}/kTr^4)^{1/6}$$

$$\text{Evaporation/Condensation (Kingery \& Berg)}^{(4)} \frac{x}{r} = 3.76 (M/NkT)^{1/2} (\gamma Pt/\delta^2)^{1/3}$$

The quantities x and r are defined in Figure 6.14, the remaining quantities are:

Ω - atomic volume

D - appropriate diffusivity

γ - surface energy

δ - width of grain boundaries

a - atom diameter

P - vapor pressure

t - interparticle contact time

and M , N , k and T have their customary meaning.

The quantities which will most strongly affect the predictions of the models are the diffusivity, D and the vapor pressure, P . These quantities depend exponentially on temperature, and can vary many orders of magnitude within groups of analogous materials. Other quantities, such as surface energy, density, etc. are either well known or do not vary so extensively. The small exponents

Figure 6.14 Neck growth mechanisms and corresponding paths
 $\left(\frac{Y}{R}\right) = \text{Hertzian contact circle ratio}$ $\left(\frac{X}{R}\right) = \text{neck ratio}$

on the right-hand side of the equations make the predicted value insensitive to such inputs - e.g. an order of magnitude error results in a 1.4 to 2.2 factor in the predicted (x/r) ratio.

The only diffusivity data revealed by a computer-aided literature search was a value of 42.5 Kcal/mole for the activation energy of lithium diffusion, and a jump frequency of $2.5 \times 10^{13} \text{ sec}^{-1}$, determined by Oei and Richtering⁽⁷⁾. However, Oishi et al⁽⁸⁾ argue on crystal structure grounds that the anion diffusion should be rate-determining in lithium oxide. We thus will consider the Oei data as a maximum value, that is, the minimum activation energy and maximum jump frequency; and determine the effect of significantly smaller values separately.

Appropriate diffusivity values are also required for the grain boundary and surface diffusion mechanisms. The activation energy for diffusion consists of defect formation and defect migration terms; on surfaces and boundaries sufficient defects exist that only the migration term applies. However, in certain structures, appropriate defects exist as a result of nonstoichiometry; in these structures, diffusion rates on surfaces and boundaries are not large enough to be observed against the background of volume processes. Oishi⁽⁸⁾ found that boundary diffusion of lithium in lithium oxide was not significant; this is consistent with their crystal structure arguments. Although no information on the composition range of lithium oxide is available, it appears unlikely that sufficient defects would exist to compensate deviations in both directions from stoichiometry (i.e., cation and anion rich); thus, we assume that boundary and surface processes will have smaller activation energies than volume diffusion.

The prediction of sintering behavior involves the selection of a critical neck ratio above which pairs of particles will survive the disrupting contact forces with other spheres, and the estimation of the contact time which is the period between contact-disrupting impacts. The former quantity depends not only on the estimated strength of the interface, but on the estimate of the magnitude of the disrupting forces. Previous authors⁽⁹⁾ have used the strength of the bulk material for the interfacial strength, and gravity (the weight of one sphere) as the disrupting force. We make the somewhat less conservative assumption that the flow pattern of the particles results in impact loading equivalent to several gravities; thus:

$$\text{bond area} \times \text{strength} = \text{volume} \times \text{density} \times \text{gravities}$$

$$\pi x^2 \sigma = \frac{4}{3} \pi r^3 \rho N g$$

$$\frac{x}{r} = \left(\frac{4 r \rho N g}{3 \sigma} \right)^{1/2}$$

The resulting neck ratio (x/r) is 4×10^{-3} for a sphere radius of .02 cm, an N of 3, and a strength (at high temperature) of 10^7 dyne/cm². Sintering rates increase more rapidly with temperature than strength decreases, so this is the critical case.

An initial contact area will exist due to deformation of the spheres under gravitational forces. The area may be calculated from the Hertian relationship⁽¹⁰⁾.

$$p = \frac{\text{Pressure}}{\frac{\text{spheres}}{\text{unit area}} \times \frac{\text{contacts}}{\text{sphere}}} = \frac{f \rho g h}{2} \left(\frac{1}{2r} \right) (\sim 6)$$

Where:

f = packing fraction (estimated at 0.6)

ρ = density
 g = gravity
 h = depth of bed

Combining the above, and using the density of lithium oxide and assumed modulus of 10^{11} dynes/cm².

$$\frac{x}{r} = 10^{-3} h^{1/3}$$

This result is based on hydrostatic pressure. The actual pressure in a static particulate bed increases from zero at the surface to a value approximating the hydrostatic at a depth of 2 diameters⁽¹¹⁾; the pressure can approximately double under flow conditions⁽¹²⁾. Due to the zoned blanket, the effective diameter is only ~0.1m, so the Hertzian neck ratio reaches a maximum value of 4×10^{-3} .

The equality of the Hertzian neck ratio and the critical neck ratio calculated previously suggests that the material transport process controlling adherence of the spheres would be diffusion across the contact interface. That is, sufficient contact area to cause adherence exists at zero time, and the requirement for adherence involves the development of a bond in this area rather than the radial growth from an initial point. Intuitively, the former, with a lesser characteristic length and greater active cross section, would be a more rapid process. Thus, the sintering estimates obtained from the neck growth models are considered to be lower bounds.

The calculated contributions of the sintering mechanisms are shown as functions of temperature in Figure 6.14 for the indicated system and material parameters. For these parameters, particle adherence is predicted at 1200K as shown by the uppermost curve, which sums the mechanisms.

Surface diffusion is the primary mechanism, contributing $\sim 2/3$ of the total growth rate. Grain boundary and evaporation/condensation account for nearly all of the balance, as volume diffusion is negligible.

The influence of the parameter choices on the predicted sintering behavior is shown in Figure 6.16, where the sum of all contributions is plotted for with a single parameter change at a time. Curve A is replotted from Figure 6.15, and represents the reference assumptions. Curve B indicates that a ten-fold increase in contact time reduces the temperature at which adherence becomes critical by $\sim 100\text{K}$. Curve C shows the effect of a ten-fold increase in particle radius; this is extremely effective in reducing the sintering process rates. The actual effect would be larger than shown because the critical value of (x/r) increases with r (page 6); this increase is indicated by the symbol at the upper left of the Figure. Unfortunately, the increase in particle size is not an acceptable solution because of thermal shock and tritium release considerations. Curve D, E and F illustrate the effects of various changes in our diffusion activation energy estimates. For curve D, values 10 Kcal/mole greater than those in curve A were assumed; this increases the critical temperature by $\sim 200\text{K}$. For curve E, it was assumed that boundary and surface diffusion were controlled by the original volume energy estimate of 65.5 Kcal/mole; this results in a $\sim 300\text{K}$ critical temperature increase. Finally, the reverse assumption (volume, boundary and surface energies all equal at 42.5 Kcal/mole) is shown to have negligible effect, due to the relative insignificance of volume diffusion.

Figure 6.15 Neck growth as a function of temperature for the various mechanisms and indicated parameters. The dashed horizontal line corresponds to the threshold neck ratio for adherence.

Figure 6.16 The effect of changes in system parameters and errors in material parameters on the predicted neck growth as a function of temperature.

A. Figure 6.14	E. $E_{\text{surf}} = E_{\text{vol}} = 65.5 \text{ Kcal/mole}$
B. $t = 10xt$	F. $E_{\text{surf}} = E_{\text{vol}} = 42.5 \text{ Kcal/mole}$
C. $R = 10xR$	G. Refractory Coating
D. $E = E+10 \text{ Kcal/mole}$	

While considering the effect of errors in estimated quantities, it should be pointed out that a factor of two error in the critical value of the (x/r) ratio results in a change of $\sim 200\text{K}$ in the predicted critical temperature. The dashed curve (G) in Figure 3 shows the anticipated effect of reducing the rates of the solid state processes by a refractory coating, such as that shown in Figure 6.17 while not interfering with the Kingery-Berg evaporation/condensation process. The latter condition is required both from mechanical constraints and the need to extract the bred tritium from the particles, i.e. any coating used must be highly permeable. The coating raises the critical temperature about 250K .

To summarize, Figures 6.15 and 6.16 indicate that the particles would sinter together at the selected peak blanket outlet temperature of 1200K , for the best estimates of system and material parameters. If the contact time is shorter or the activation energies larger than estimated, or if the particle diameter can be increased by a factor of two; then sintering may not be a problem except in localized "hot spots", where peaking effects may increase temperatures up to 10% . If the duration of the particles presence in such areas is sufficiently small, any bonds formed may be fractured after the particles leave the hot spots. A refractory coating would also protect the particles from sintering by the mechanisms discussed here, up to temperatures of $\sim 1400\text{K}$, where the evaporation/condensation contribution becomes dominant.

6.2.3.2

Other Material Transport Mechanisms

Two additional material transport mechanisms are expected to be operative in the blanket as a result of the steep thermal gradients ($\sim 300\text{K/n}$) parallel to the flow direction.

Figure 6.17 Protective coating to reduce rate of neck growth at contact points.

The first of these is based on the vapor pressure of Li_2O , using the data of Ihle et al⁽¹³⁾; the second mechanism depends on the total pressure of hydrogen isotopes (bred tritium as well as hydrogen from moisture, etc.) and the volatility of LiOH .

The evaporation/condensation model Kingery and Berg⁽⁴⁾ considered only the difference in vapor pressure arising from variation in the radius of curvature under isothermal conditions. Here we consider the absolute value of the pressure as a function of temperature, and estimate the vapor flux via the Langmuir-Childs equation:

$$n = \frac{k P}{\sqrt{MT}}$$

where n = molecules/cm² sec

p = vapor pressure (atm)

$k = 2.66 \times 10^{25}$

and M , T have their usual meaning.

The equation will provide an overestimate because it assumes free evaporation, whereas the in-blanket case there will be a reverse flux due to the presence of other surfaces and the helium atmosphere.

Inserting the values for the Li and Li_2O species, and expressing the result in monolayers/sec, we obtain the results tabulated below:

<u>T, K</u>	<u>Li</u>	<u>Li₂O</u>
1000	~0	~0
1100	~0.1	~0
1200	2	1
1300	34	16
1400	332	209

The evaporation rate at 1200K corresponds to a weight loss of 0.1% per meter of travel of the spheres or about 0.5% in the overall hot zone. Note that these numbers become 2 and 10% respectively, at 1300K; illustrating the need to minimize thermal streaking. The evaporated material would condense on the cooler spheres up-and-downstream, as well as on the containment walls and zone baffles with the latter effect being sensitive to the wall configuration with respect to active cooling and thermal insulation. (See Section 6.2). The total flux of material can be calculated from the surface area of spheres at the maximum temperature ($\sim 3000 \text{ m}^2$ per module); the result $\sim 10^{15}$ molecules/cm² sec at 1200K, is an over-estimate since it is based on free evaporation. In the particulate blanket, a given evaporation site "sees" mostly surfaces at the same temperature, so most of the flux should be redeposited on similar surfaces. Only spheres in the upper portion of the temperature gradient (i.e. near T_{max}) and/or near a wall or baffle, would lose material to the extent indicated. However, the transfer between spheres near T_{max} is expected to contribute to adherence, and the net flux to the lower temperature regions is expected to extend the sintering temperature range to lower values than calculated according to the models discussed previously. Again, since this is a vapor-based mechanism, it cannot be inhibited by a refractory coating.

The second vaporphase transport mechanism involves the hydration reaction:

where the state of the product will depend on pressure and temperature. The thermodynamic data from the JANAF⁽¹⁴⁾

tables were used to derive the information tabulated below, where the standard free energies of the hydration reactions and the vapor pressure of LiOH are shown as a function of temperature in the range of the particulate blanket.

T, K	$\Delta F^\circ \text{ LiOH (sol)} \frac{\text{Kcal}}{\text{mol}}$	$\Delta F^\circ \text{ LiOH (gas)} \frac{\text{Kcal}}{\text{mol}}$	VP LiOH atm
700	-19.36	+37.35	1.4×10^{-9}
900	-21.12	+28.46	9.5×10^{-7}
1100	-14.04	+16.99	8.2×10^{-4}
1300	-12.03	+11.62	1.0×10^{-2}

The main implication of these results for the particulate blanket is that if the hydration reaction proceeds at any temperature to the extent that it generates a LiOH pressure greater than 1.4×10^{-9} atm, the lowest temperature region of the blanket will condense this vapor and become a sink for the reaction products.

The water vapor pressure necessary to generate a LiOH pressure of 1.4×10^{-9} atm can be derived from the tabulated data and the relations:

$$K = \frac{(P_{\text{LiOH}})^2}{(P_{\text{H}_2\text{O}})} = \exp \left(- \frac{\Delta F^\circ}{RT} \right)$$

The result is that the necessary water vapor pressure decreases rapidly with increasing temperatures; (10^{-6} atm at 700K; 5×10^{-13} atm at 1100K) which in effect means that the rate of the reaction will be determined by the availability of water vapor rather than equilibrium considerations. This is consistent with the data reported by Johnston⁽¹⁵⁾ on the weight loss of Li_2O as a function of water vapor pressure. The weight loss rate was proportional to $P^{1/2}$; also, the weight loss occurred with

1

In this reference, the data was attributed to Carter⁽³²⁾ who further attributed it to Ostrovshko et al.⁽³³⁾ The original (?) reference 33 is not presently available.

water vapor pressures less than the free energy equation indicates would be necessary under isothermal conditions.

It is also noted that the hydration reaction producing a solid product requires a water vapor pressure of $\sim 10^{-6}$ atm at 700K; the requirement increases rapidly with temperature (1.6×10^{-3} atm at 1100K). The two reactions can occur simultaneously only at the lowest temperature; the vapor phase product is favored at the higher temperatures. The availability of water vapor in the blanket is difficult to assess. There will be contributions from real and virtual leaks, from tritium breeding, and from any makeup helium and lithium oxide. There will also be a contribution due to outgassing of the blanket walls and the particles both on initial start-up and after any shut-down conditions. It will probably be necessary to outgas the particle in an isolated batch mode on these occurrences, since it would not be feasible to either heat the entire blanket simultaneously or maintain sufficiently dry conditions to prevent hydration at room temperature. ($P_{H_2O} \sim 2 \times 10^{-22}$ atm, although somewhat higher values may be tolerable because of kinetic limitations at low pressures. However, the rate of hydration becomes limited by the availability of H_2O (Langmuir collision rate) below $\sim 10^{-10}$ atm.)

An estimate of the leak rate to be expected in the blanket can be obtained by scaling data from industrial vacuum vessels by the approximate ratio of surface area, and making an allowance for the non-isothermal conditions of the blanket, which would increase the probable leak rate. On the other hand, much of the surface area of the blanket structure may be protected by a guard layer (vacuum or

before it has an opportunity to condense in the coolest portion of the blanket.

Thus, the hydroxide vapor transport mechanism is not expected to cause flow plugging problems if the lithium oxide itself can be loaded into the blanket in a sufficiently dry condition. The recent observations of several workers (17-18), however, indicate that this is far from a trivial undertaking.

Any source of hydrogen would result in a third material transport mechanism, since the reaction

has a large negative free energy under blanket conditions. (Both products have effective sinks; the LiOH in the low temperature portion, the LiH via dissociation at high temperatures to $\text{Li} + \text{H}_2$, which returns one-half the hydrogen for further reaction). The only source is the low pressure in the plasma adjacent to the first wall, which does not appear large enough to cause problems even if the first wall was highly permeable. If hydrogen was in fact a problem, a thin oxide coating on the first wall should provide an adequate barrier.

6.2.3.3 Internal Sintering of Microspheres and Tritium Removal

The conclusions of the inter-particle sintering discussion imply that intra-particle sintering will proceed rapidly in the high temperature portions of the blanket and heat exchanger. Although the models discussed above only apply to the first stage of neck growth ($\frac{x}{r} < 0.2$) they may be used to consider the effects of the relevant parameters in the intra-particle case. First, the radius is much smaller, being at most one-half of the grain size, and frequently less due to non-spherical grain shapes. The small radius enhances sintering rates, both directly and through its effect on the surface area/volume

ratio with respect to the surface diffusion contribution.

The non-spherical grain shapes produce large variations in the radius of curvature, including negative values in re-entrant corners; this increases the driving force for the evaporation/condensation mechanism.

The consequences of intra-particle sintering for tritium removal are not clear. It was thought that removal depended on open porosity and short paths to grain boundaries.⁽¹⁹⁾ The results of Tanaka⁽²⁰⁾ who found a rapid decrease in tritium removal kinetics with a density increase from 88.5% to 91.5%, tend to confirm this view. However, the recent results of Tanifuji⁽²¹⁾, who found that the activation energy for the removal process corresponded to that of the surface desorption step for both powders and single crystal specimens, suggests that sintering may not be important, at least for tritium. It will, however, affect helium release, which has implications for swelling and the internal stress state of the microspheres; particularly, if a refractory coating is employed.

6.2.3.4 Wear of Particles and Walls

The wear problem has been discussed by the originators of other particulate blanket concepts^(9,22) and by the present authors⁽²³⁾ for the case of a fertile blanket for a hybrid fusion fission reactor. The following discussion is largely adapted from those sources, with modifications required by the low density and relatively unknown mechanical properties of lithium oxide.

The operative wear mechanism depends on a large number of parameters including the relative mechanical properties of the materials, the velocity and angle of relative motion, the temperature and normal forces, etc. In the case of elastic materials, approximated by refractory oxides or carbides at moderate temperatures, wear would be expected to proceed via crack formation around contact points. Plastic materials, such as metals (or compounds at high homologous temperatures or with long contact times), would wear by an adhesion-transfer process. Intermediate conditions would produce both types of behavior.

The low velocity and generally parallel flow (small angle of incidence) of the particulate blanket, together with the low density of lithium oxide (which will reduce the contact loads) and the spherical shape, are expected to minimize the wear problem.

The velocity of interest is the relative velocity of the contact points, and includes rotation and vibration as well as the bulk translation. Although the flow behavior is not sufficiently characterized to predict this velocity, it appears unlikely that it would be more than an order of magnitude greater than the bulk velocity. This is still small when compared to the threshold of velocity-dependent wear effects.

The contact load depends on the depth in the particulate bed, and the flow velocity. In the static case, the pressure increases for the first two diameters⁽¹¹⁾ and remains constant at greater depths due to load transfer to the container walls. The pressure may approximately double under flow conditions⁽¹²⁾. The low velocity of the particles in the blanket probably corresponds best to the static case.

The mechanical properties of lithium oxide are not available (particularly at elevated temperatures) but it is expected that the material is sufficiently soft that the adhesion-transfer process will dominate the particle-particle wear rate, and that the wear rate of wall materials which have mechanical properties adequate for structural requirements will not be excessive. These expectations would be reversed if the lithium oxide microspheres were coated with a refractory compound to reduce their adherence, in that case, abrasion would become significant in both wear of the walls inter-particle and particle-wall contacts.

The adherence of such a coating would dominate the wear process due to the catastrophic effect of even a small volume fraction of thin, hard and sharp-edged coating fragments. A high level of coating adherence under the conditions of the particulate blanket (e.g. thermal cycling, dissimilar materials) would require careful attention to the cleanliness of surfaces during the coating operation. The requirement that the coating be permeable to tritium (for its recovery) and helium (to relieve pressure buildup) could best be achieved by a columnar micro-structure with less than bulk material density. The interaction between this requirement and the adherence one is not well defined.

6.2.3.5 Selection of Structural Materials for the Blanket

The materials selection process involves a correspondence between service conditions and available properties. When a satisfactory correspondence is not immediately achieved, some compromise in service conditions is necessary in the short term, and, for the long term, some developments in materials are postulated. In the present case, the severity of the service conditions relative to the capabilities of existing structural materials has led to both outcomes.

The particulate blanket structure experiences a large flux of fast neutrons, a moderate stress due to the low coolant pressure, the coolant chemical environment, and a temperature of ~1300K unless actively cooled.

The general classes of materials available for the maximum temperature case are: nickel-base superalloys, refractory metals, and ceramics (i.e. refractory oxides, carbides, etc.). The consensus of several study groups⁽²⁴⁻²⁶⁾ on these materials appears to be as follows:

1. The nickel base superalloys are subject to extensive radiation damage because of helium generation as well as the mechanisms common to other metals. They are marginal in terms of mechanical properties at 1300K, particularly for long times; but are more compatible with the anticipated chemical environment than the refractory metals. They may not be fully satisfactory in this respect, since their normal protective mechanisms require an oxygen potential much greater than will exist in the blanket. Also, reactions with Li_2O may be significant at least in the case of alloying elements^(27,28). However, nickel is the only candidate material empirically established as compatible with Li_2O at 1300K.

2. The refractory metals are unacceptable because of (a) their embrittlement by interstitials at the level the latter will probably be present in the helium stream and (b) the lack of fabrication and, particularly, joining technology applicable to the shapes and sizes of interest here. The former consideration applies mainly to the Group V elements Nb and V; the latter to Group VI, Mo and W. Both groups are known or expected to be reactive with Li_2O at 1300K.
3. Ceramic materials offer high temperature strength and minor activation problems; and have adequate tolerance to helium impurities. Little is known of their irradiation damage behavior, although swelling is a problem in oxide ceramics at the temperatures considered here⁽²⁹⁾. Also, current technology is not capable of fabricating and joining modules of the size required here⁽³⁰⁾. The limited plasticity of these materials prevents stress relaxation around the inevitable defects; together with their poor thermal stress behavior this property makes catastrophic failure a likely possibility upon uneven cooling from the service temperature.

Accepting the foregoing as representing the current status, it appears that the most practical solution is to reduce the temperature sufficiently to permit the superalloys to be utilized. This could be done either by accepting the reduction in the temperature of the heat delivered to the hydrogen production process, or by actively cooling the walls. In the latter case, the walls should be insulated both to maintain the overall efficiency and to avoid the need for high pressure coolants to extract the heat. A temperature reduction of 150-200K should be sufficient in either case.

A second potential compromise in service conditions involves the chemical environment. It is generally stated that it is not feasible to improve on "reactor grade" helium in a practical, large scale system sufficiently that the refractory metals could be employed⁽³¹⁾. However, this refers to a high pressure, high flow rate system used to transport large amounts of heat. The purge stream in the particulate blanket is at a nominal pressure of one atmosphere and has a low flow rate. It would not seriously impact the thermal performance of the blanket if a substantial part of this stream were continuously extracted and gettered, even if it were necessary to cool it to ambient temperature. If a gettering rate slightly exceeding the in-leakage rate could be achieved, the group V refractory metals could be employed. Guard vacuum would be required for protection of the exterior surface.

Materials developments which may have an impact on the selection of blanket structure, and which appear feasible within the time frame appropriate for the realization of a reactor concept, are discussed below;

1. Superalloy Development

Although the conventional superalloy field appears limited by the law of diminishing returns after 40 years of effort, there remains a significant opportunity in the areas of dispersion strengthening and metal-matrix composites. Although it is not likely that large increases in operating temperature can be achieved, since the matrix is at three quarters of its absolute melting point at current use levels, it may be possible to achieve higher strengths at equal temperatures. More significantly for the present application, it may be possible to achieve these properties in a material consisting of nickel and inert dispersants, thus minimizing the corrosion problems with lithium oxide

and the common alloying additions. Potential examples are further developments of the TD (thoria dispersed) nickel and the tungsten-wire reinforced nickel types.

2. Protective Coatings

This is currently a very active field, in which vapor deposition processes are coming into competition with pack cementation and plating processes. The new processes permit new coating compositions to be employed, with the result of rapid and significant improvement in protective ability. Much of this work has involved superalloys for gas turbine application, and may be directly applicable to the present blanket concept, with the limitations imposed by the low oxygen potential.

Further in the future, these developments may be applied to the refractory metals for protection from trace interstitials in generally inert atmospheres.

3. Ceramic Fiber Reinforced Composites

A second long range prospect is the application of the knowledge gained in the development of fiber reinforced metal matrix composites to the more difficult case of the refractory metals. New methods will be required, since liquid infiltration is less than feasible with melting points of $>2500^{\circ}\text{C}$ and high reactivity with containment materials. The vapor phase processes developed for deposition of coatings may be candidates for the formation of such composites.

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6.3 HEAT EXCHANGER DESIGN ANALYSIS

The following discussion is concerned with the integral heat exchanger which transfers heat from the hot Li_2O to the working fluid of the thermal transport and energy conversion system discussed in Section 7.0. The basic design assumptions and requirements are summarized in Table 6.14.

The design analysis results presented here are concerned with the thermal-hydraulic performance and with materials of construction. No structural analysis has been performed.

6.3.1 Thermal-Hydraulics

The thermal-hydraulic analysis has considered two basic heat exchanger concepts. One is a vertical shell and tube design with sodium inside the tubes. The other is a direct contact heat exchanger where larger spheres (2 mm vs 400 μm) fall through a nearly stagnant pool of sodium. In either case the thermal-hydraulic analysis is concerned with sizing the heat exchanger such that it can transfer the required heat at the desired temperature difference. The overall heat transfer can be expressed by

$$q = UA\Delta T_m$$

where A is the heat transfer surface area, ΔT_m is the log mean temperature difference and U is the overall heat transfer coefficient. The design analysis processes is one of defining U for a chosen heat exchanger geometry such that the heat transfer area and overall size can be determined.

Shell and Tube Heat Exchanger

The overall heat transfer coefficient based on tube inside area can be written as

TABLE 6.14
DESIGN REQUIREMENTS FOR MODULE HEAT EXCHANGER

General

Total Heat Transferred	75 MW
Mean Temperature Difference	50 K

Li_2O Side

Mass Flow Rate	48 kg/sec
T_{in}	665 K
T_{out}	1210 K
Pressure (helium cover)	~1 atm

Sodium Side

Mass Flow Rate	110 kg/sec
T_{in}	615 K
T_{out}	1160 K
Pressure	few atm

$$U_i = \frac{1}{\frac{1}{h_i} + \frac{A_i \ln r_o/r_i}{2\pi AL} + \frac{A_i}{A_o} \frac{1}{h_o}}$$

where the three components of the heat transfer coefficient are those at the inside wall, tube wall and outside wall. Preliminary analysis showed that the heat transfer between the tube wall and the particles was the most limiting factor controlling heat exchanger size. It was concluded that the design should maximize the area on the Li_2O side of the heat exchange surface. For that reason the Li_2O was put on the outside of the tubes. Enhancement of the outside surface area would also be of benefit to reduce size. A concept of this type was suggested for the SOLASE fusion reactor steam generator and was also adopted for this study. The heat exchanger is a vertical shell and tube design where the sodium is inside the tubes and the particles flow by gravity on the shell side. The tubes are fluted to increase the outside heat transfer area by a factor of two and to reduce the hydraulic diameter used for the particle Nusselt number. The selected outside channel size is 1 cm which is sufficiently large to allow free flow of the 400 μm microspheres. The flutes also provide additional structural rigidity and strength for the tubes. The flutes are intended to contact each other to provide integral support for the structure. Periodic annular rings are included to provide tube support should there be misalignment of the flutes. Axial thermal expansion is accommodated by the unfluted tube bends at the end of the heat exchanger. Baffles at the end of the tube sections channel the particulates into and out of the heat exchanger.

Now consider the individual heat transfer processes in the heat exchanger. The inside heat transfer of sodium can be calculated from

$$Nu = 4.82 + 0.0185 (Re Pr)^{0.827}$$

where the Nusselt number is hD/k . Taking $Re = 10,000$,
 $Pr = 0.0038$,

$$Nu = \frac{hD}{k} = 5.2$$

For $D = 1.0$ cm and $k = 59.7$ W/m-K,

$$h_i = 31,000 \frac{W}{m^2 K}$$

and

$$\frac{1}{h_i} = 3.2 \times 10^{-5} \frac{m^2 K}{W}$$

The tube wall is assumed to have a thermal conductivity comparable to a chrome-nickel superalloy ($k = 15$ MW/m-K). The inner and mean outer diameters were taken to be 1.0 cm and 1.4 cm; thus,

$$\frac{A_i \ln r_o/r_i}{2\pi k L} = 11.2 \times 10^{-5} \frac{m^2 K}{W}$$

Heat transfer in flowing particulate beds involves both the particles and the fluid in the spaces between particles. For static beds the heat transfer has been modelled by several investigators to define the effective thermal conductivity for the bed treated as a continuum as discussed in Appendix

6B. For the Li_2O and helium mixture considered here, the results of four different models had amazingly good agreement as shown in Appendix 6B. A value of 0.82 W/m-K for the effective thermal conductivity was selected for this study and was also assumed to apply to slowly moving beds. The heat transfer coefficient from a wall to a bed of moving particulates has been developed by several investigators by using a continuum model for the particle bed (Appendix 6B). Most investigators also generally assume that the bed is a semi infinite media and this leads to high heat transfer coefficients for initial stages of heat transfer such as at the entrance to a channel. If the flow is fast enough, or if the thermal conductivity is low enough, developing heat transfer could exist along the entire channel. In the heat exchanger, however, the entry region is of modest length and the heat transfer is mostly fully developed. Appendix 6B has additional discussion of this topic.

Assuming fully developed heat transfer and a uniform velocity profile ("plug-flow") the Nusselt number is 8. For $k = 0.82 \text{ W/m-K}$,

$$h_o = \frac{8k}{D} = 656 \frac{\text{W}}{\text{m}^2\text{K}}$$

and

$$\frac{A_i}{A_o} \frac{1}{h_o} = 76.2 \times 10^{-5} \frac{\text{m}^2\text{K}}{\text{W}}$$

The total result is

$$U_i = \frac{10^5}{3.2 + 11.2 + 76.2} = 1100 \frac{\text{W}}{\text{m}^2\text{K}}$$

Note that the sodium and tube-wall contribution are relatively small compared to contribution of the Li_2O on the outside of the tubes.

The total heat transfer area for 75.0 Mw is

$$A_i = \frac{Q}{U_i \Delta T_m} = 1360 \text{ m}^2$$

This would require 43,300 m of tubing per module. The unit volume per meter of tube is $0.000314 \text{ m}^3/\text{m}$, thus the heat exchanger volume is 13.6 m^3 . Assuming a vertical dimension of 6 m the cross section area would be 2.27 m^2 and the diameter of a circular exchanger would be 1.7 m. There would also be about 7200 tubes. Table 6.15 presents a design summary for the heat exchanger.

This is a large piece of equipment for transferring 75 MW. The reason for the large size is the poor particle side heat transfer. The size could be reduced by increasing the mean temperature difference or the heat transfer coefficient. Since the process temperatures are already high, it appears that some method of additional heat transfer enhancement would be preferred. Benefit might be obtainable by using static mixers to promote more radial mixing of the microspheres. Fluidization is also a possibility for enhanced heat transfer.

The microsphere flow area for the heat exchanger is 1.13 m^2 which for 48 kg/sec gives an average velocity of 0.035 m/sec. This is a low velocity and comparable to that in the blanket. The sodium flow area, assuming the flow is vertical and parallel to the tubes, is 0.57 m^2 . For a sodium flow rate of 110 kg/sec, the velocity is 0.25 m/sec. The Reynolds number is

TABLE 6.15

DESIGN SUMMARY FOR SHELL AND TUBE HEAT EXCHANGER

General

Total Heat Transferred	75.0 MW
Diameter	1.7 m
Height	6.0 m
Tube ID	1.0 cm
Tube Pitch (Triangular)	1.9 cm
Heat Transfer Area (Inside Tubes)	1360 m ²
Number of Tubes	7200
Overall Heat Transfer Coefficient	1100 W/m ² -K
Mean Temperature Difference	50 K

Shell Side

Working Fluid	flowing Li ₂ O spheres
Mass Flow Rate	48 kg/sec
T _{in}	665 K
T _{out}	1210 K
Heat Transfer Coefficient	656 W/m ² -K

Tube Side

Working Fluid	Sodium
Mass Flow Rate	110 kg/sec
T _{in}	615 K
T _{out}	1160 K
Heat Transfer Coefficient	31000 W/m ² -K

11,000 which indicates that the flow is turbulent. The sodium friction pressure drop through the heat exchanger tubes can be calculated from

$$\Delta P = \frac{fL}{D} \frac{\rho v^2}{2}$$

where fL/D represents the tube friction. A probable value for fL/D for the tubes is 36 based on a friction factor of 0.03 and tube length of 12 m. The resulting pressure drop for 0.25 m/sec velocity is 0.9 kPa or 0.1 psi.

Inlet and outlet hardware loss coefficients would also exist and a typical total loss coefficient (equivalent to fL/D) of about 2.0 could be expected based on pipe diameters connecting to the heat exchanger. The pressure loss based on a connecting pipe velocity of 5 m/sec would be 19.5 kPa or 2.8 psi. This is very large compared to the above value for the tubes. This indicates that the major pressure drop would be in the inlet and outlet hardware and not in the heat exchanger. The total heat exchanger friction pressure drop is estimated at about 20.4 kPa or 2.9 psi.

It is also instructive to determine the pipe sizes and pressure drop of the connecting piping for each module heat exchanger. assuming a velocity of 5 m/sec the mass flow would require a 0.187 m ID pipe. The pressure drop per 100 m assuming a friction factor of 0.015 is 78.2 kPa or 11.3 psi. Thus, if the thermo-chemical process is located 100 m from the driver the pressure drop excluding the process heat exchangers would be about 175 kPa or 25 psi. This would require a pressurization of the heat transport system to at least 3 or 4 atm to accomodate the transport system pressure drop. The recircula-

tion pump would probably be located at the exit of the last process heat exchanger to place it at a low temperature location and to keep the high temperature zones at the lower loop pressures. The pumping power excluding the process heat exchangers would be 0.16 MW for each module which is small compared to the 75 MW module power.

The possibility of using helium as the transport fluid would be highly desirable as compared to sodium, however, the pressure drop and pumping power to recirculate helium is high at low pressure using reasonably sized pipes. Figure 6.18 shows the relative power required to pump sodium and helium as a function of pipe diameter for selected pressures at 1150 K and for 100 m of pipe. At 10 atm which could be a reasonable upper pressure limit, the pumping power for a 0.2 m diameter pipe is of the same order as the module power. For 1.0 m diameter pipe the pumping power reduces to a few percent of the module power. Reducing the pressure increases the recirculating power consumption. It can be concluded that use of low pressure helium would only be possible using short runs of large diameter piping. This is not consistent with the anticipated physical layout of the fusion driver and thermochemical hydrogen plant.

Direct Contact Heat Exchanger

A direct contact heat exchanger was evaluated as an alternate concept where the particles would fall into a rising pool of liquid sodium and transfer their heat during the fall through the pool as shown schematically in Figure 6.8. The particles would be collected at the bottom and returned to the top of the blanket by entrainment in liquid sodium. The particles would be separated from the liquid prior to their entry to the blanket. The motivation would be to reduce the size of the heat exchanger in exchange for a unit to separate the Li_2O and sodium.

Figure 6.18 Pumping Power for Sodium and Helium as Working Fluids.

The feasibility of this concept requires that the particles be able to fall by gravity through the rising sodium. That free fall can be calculated from⁽²⁾

$$C_{D_0} Re_{D_0}^2 = \frac{4}{3} \frac{d^3 \rho_s g (\rho_s - \rho_f)}{\mu_f}$$

Assuming 2 mm particles and using $\rho_f = 780 \text{ kg/m}^3$, $\rho_s = 2013 \text{ kg/m}^3$, and $\mu_f = 0.18 \times 10^{-3} \text{ kg/m-sec}$,

$$C_{D_0} Re_{D_0}^2 = 3.1 \times 10^6$$

For $Re_{D_0} > 1000$, $C_{D_0} = .44$ and the free fall of a particle is

$$u_{D_0} = 0.614 \frac{\text{m}}{\text{sec}}.$$

Since the free fall velocity is much higher than the bed velocity, the solids fraction in the liquid would be quite small. Assume that the sodium velocity is $\sim 1/5$ of the particle velocity or 0.1 m/sec. The total sodium flow rate to transfer 75 MW is 110 kg/sec using $C_p = 1.26 \text{ J/g-K}$ and $\Delta T = 545 \text{ K}$. The required sodium flow area is 1.52 m^2 for $\rho_f = 780 \text{ kg/m}^3$.

Similarly, for a particle absolute velocity of 0.5 m/sec, the particle flow area is 0.05 m^2 . The solids fraction is therefore 3.2% which is quite dilute. The total area of 1.57 m^2 corresponds to a diameter of 1.42 m.

Now define the vertical length of the heat exchanger. Consider the process to be heat transfer coefficient controlled. For spheres⁽³⁾

$$Nu = 2 + 0.386 (Re Pr)^{0.5}$$

Extrapolating this correlation below its lower limit of 3×10^4 for an estimated $Re = 5200$ and $Pr = 0.0038$,

$$Nu = 3.7$$

and

$$h_o = 110,000 \frac{W}{m^2 K}$$

This extrapolation is of little consequence because the internal conductance is controlling. The internal conductance in the sphere is proportional to k/d , where d is the sphere diameter. The constant of proportionality would be greater than one and on the order of 10 based on a Nusselt number analogy. If a continuous hot rod passed through the sodium it could be considered analogous to a "plug flow" and its Nusselt number would be 8. A string of spheres would be similar but with a larger value for the Nusselt number. Assuming a value of 10, a 2 mm diameter sphere, and $k = 1.73$ W/m-K, the heat transfer coefficient is

$$h_i = 8650 \text{ W/m}^2\text{-K}$$

which indicates that the internal thermal resistance of the sphere is controlling. The overall heat transfer coefficient is

$$U = \frac{1}{\frac{1}{h_i} + \frac{1}{h_o}} = 8020 \frac{W}{m^2 K}$$

and the area is

$$A = \frac{Q}{U \Delta T_m} = 187 \text{ m}^2$$

where ΔT_m is assumed to be 50 K. The area is an order of magnitude smaller than the shell and tube exchanger. This area requires approximately 15×10^6 microspheres in the heat exchanger. For a 3.2% solids fraction this requires a heat exchanger volume of 1.93 m^3 . For a vertical height of 3.0 m the diameter is 0.91 m. This is a reasonable size heat exchanger.

The above estimates are approximate, but do indicate the dramatic size reduction which is possible by using a direct contact heat exchange with sodium rather than a shell-and-tube heat exchanger.

While the size reduction is attractive, other issues must be addressed such as the chemical compatibility of Li_2O , particle attrition, tritium removal, tritium contamination of Na.

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TABLE 6.16
DESIGN SUMMARY FOR DIRECT CONTACT HEAT EXCHANGER

General

Total Heat Transferred	75 MW
Diameter	0.91 m
Height	3.0 m
Heat Transfer Area	187
Overall Heat Transfer Coefficient	8020 W/m ² -K
Mean Temperature Difference	50 K

L₁₂O Particles

Particle Size	2 mm
Particle Inventory	15 x 10 ⁶
Settling Velocity	0.6 m/sec
Absolute Velocity	0.5 m/sec
Flow Rate	48 kg/sec
T _{in}	1210 K
T _{out}	665 K

Sodium

Velocity	0.1 m/sec
Flow Rate	110 kg/sec
T _{in}	615 K
T _{out}	1160 K
Pressure	2 atm

6.3.2 Selection of Structural Materials for the Heat Exchanger

The considerations of Section 6.2.3.5 apply to the present discussion with two major exceptions. First, the heat exchanger application involves no serious radiation damage problem. This significantly reduces the uncertainties in material properties. Second, there is no possibility of reducing the service temperature by insulating/cooling mechanisms, at least in the case of the inter-fluid surfaces. The favored approach is that taken in the blanket case; to reduce the service temperature sufficiently to permit the use of the nickel-base superalloys. These materials have the best property data base at high temperatures, primarily from the gas turbine application; and are the most likely to be compatible with the lithium oxide, particularly if a dispersion hardened nickel matrix alloy is available.

CONCLUSIONS

This section has attempted to assess, in a preliminary way, the basic issues related to the design of a flowing Li_2O microsphere fusion blanket concept and associated heat exchanger. From that assessment several conclusions can be made:

1. The mechanical design of the blanket can be arranged so that high plasma coverage is possible. This could be achieved with either design concept, however, the baffled design has somewhat better coverage.
2. The neutronics analysis indicates that the tritium breeding ratio is greater than 1.0 for both designs; however, there is a greater penalty for the added structure in the cooled-tube concept. Since the cooled-tube blanket is also somewhat "thinner" from a neutronics point of view, the baffled blanket is concluded to be neutronically superior.
3. Multiple flow zones are needed to reduce temperature peaking from the "exponential" radial power gradient in the absence of some mechanism to radially mix the microspheres.
4. The neutronics calculations indicate that the neutron dose to the first-wall, shield and superconductor appear acceptable based on results for other fusion blanket concepts.
5. The average exit temperature for the flowing Li_2O microspheres is 1210 K to meet the high temperature process heat requirements. Peak Li_2O temperature is estimated at 1360 K for the baffled blanket.
6. The uncooled baffle structure imbedded in the microspheres would have peak temperatures of about 1500 K because of the internal heat generation in the structure. The cooled-

tube structure could be maintained at 1000 K but the felt-metal insulation would approach the peak temperature of the mixed Li_2O or about 1265 K. The imbedded mixer would be at about 1400 K.

7. The velocity of the Li_2O packed bed is on the order of 0.6 cm/sec and varies radially to obtain uniformity of average exit temperature. Flow control would be required at the exit of each flow channel.
8. The most feasible concept appears to be a vertical shell and tube heat exchanger with gravity flow of Li_2O microspheres on the shell side. The tubes would have external flutes to increase the heat transfer area. Heat transfer to sodium on the tube side was selected as a process working fluid because of its low pressure. Heat transfer to low or moderate pressure helium would require short runs of rather large piping which is not considered consistent with the present process configuration. While direct contact heat exchange could reduce the size of the heat exchanger, questions of chemical compatibility and tritium contamination leave that option in doubt.
9. Sintering of the bare Li_2O microspheres is probable at 1200 K based on best estimates from current analytical models. Variation of parameters or uncertainties could raise the sintering temperature, however, it would not be enough to prevent probable sintering in localized hot spots. Refractory coatings could possibly raise the sintering temperature to about 1400 K. Li_2O melts at 1973 K.
10. Two vapor-transport mechanisms further complicate the sintering issue. One is the evaporation of Li_2O from the hot particles and its subsequent condensation on cooler surfaces. The other is the formation of LiOH

(or LiOT) vapor and its condensation on cooler surfaces. These processes are both expected to be active starting at about 1200 K and could be quite active at 1400 K.

11. Wear of the structure may not be a problem for unclad Li_2O microspheres. Refractory coated microspheres would accelerate the wear problem, however, additional experimental data would be required to evaluate the issues of structural wear and particle attrition.

The overall feasibility of the flowing Li_2O microsphere concept is closely tied to materials performance at high temperature. The present temperature requirements of the thermochemical process are too high to guarantee satisfactory performance of presently identified materials. Experimental work would be necessary to define the material temperature behavior and limits with greater accuracy. The accurate determination of temperatures is also required to define the material thermal environment. Further analytical model development and experimental support would be necessary to improve thermal analysis accuracy. Assuming the present material temperature limits are valid, the service temperature of the Li_2O blanket would have to be reduced to make it feasible.

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APPENDIX 6A

TABULATION OF THERMAL-PHYSICAL PROPERTIES

MATERIAL	k (W/cm-K)	ρ (g/cm ³)	C _p (J/g-K)	ρC_p (J/cm ³ -K)	$\alpha = k/\rho C_p$ (cm ² /sec)	α^{-1} (sec/cm ²)
He	0.00275	-	5.2	-	-	-
UC ₂	0.025	10.98	0.31	3.40	0.0073	137.0
UC	0.25	13.63	0.251	3.42	0.073	13.7
Li ₂ O (1000K)	0.0173	2.013	2.89	5.82	0.003	336.4
Cu	3.86	8.954	0.383	3.43	1.126	0.888
Fe	0.73	7.897	0.452	3.57	0.205	4.89
Na (977K)	0.597	0.78	1.26	0.98	0.47	2.11

60% SOLIDS (with He cover fluid)

UC and He	0.0166	8.18	0.251	2.05	-	-
Li ₂ O	0.0082	1.21	2.89	3.49	-	-

VISCOSITY

Na (977K)	$\mu = 0.18 \times 10^{-3}$ kg/m-sec
He (800K)	$\mu = 382 \times 10^{-7}$ kg/m-sec

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APPENDIX 6B

FLOWING PARTICULATE HEAT TRANSFER

Heat transfer to flowing particulates is not well defined in spite of its importance in various industrial and food processing applications. The literature generally deals with heat transfer of fluidized beds or with heat transfer of stagnant or flowing fluids through static packed beds. The literature on flowing packed beds is meager.

The basic principles of heat transfer in fluids are usually applied to particulate beds. The bed is assumed to be a quasi continuum with a porosity (void fraction) and be analogous to fluid flow. The properties of the bed are defined in a way to preserve the properties of the composite. The following definitions are usual for two-phase or two-component flows*:

$$\text{Void Fraction: } \alpha = \frac{\text{Void (gas) Volume}}{\text{Total Volume}}$$

$$\begin{aligned} \text{Density: } \rho &= \alpha \rho_g + (1-\alpha) \rho_s \\ &\approx (1-\alpha) \rho_s \end{aligned}$$

$$\begin{aligned} \text{Mass Flux: } (\rho u) &= \alpha \rho_g u_g + (1-\alpha) \rho_s u_s \\ &\approx (1-\alpha) u_s \end{aligned}$$

$$\begin{aligned} \text{Velocity: } u &= \frac{(\rho u)}{\rho} \\ &\approx u_s \end{aligned}$$

*The approximation is made on the basis of a low density cover gas (helium). The subscripts g and s refer to gas and solid, respectively.

Heat Capacity:
$$(\rho C_p) = \alpha \rho_g C_{p_g} + (1-\alpha) \rho_s C_{p_s}$$
$$\approx (1-\alpha) \rho_s C_{p_s}$$

Specific Heat:
$$C_p = \frac{(\rho C_p)}{\rho}$$
$$\approx C_{p_s}$$

The thermal conductivity of a particulate bed is a complex combination of the individual conductivities, void fraction, packing and thermal radiation. Several correlations exist for calculating the average conductivity for static beds. The results of calculations using four different correlations are as follows:

Source	$\frac{k}{W/m-K}$
Perry (B.1, B.2)	0.82
Masamune & Smith (B.3)	0.82
Krupiczka (B.4)	0.74
Yagi, Kunii, Wakao (B.5)	0.77

The above values do not include the effects of thermal radiation. The model of Yagi, et.al, includes the effects of thermal radiation. For an emissivity of 1.0 typical results are as follows:

T (K)	Particle Dia m	$\frac{k}{W/m-K}$
cold	-	0.77
1000	400 m	0.82
1400	400 m	0.91
1000	800 m	0.87

Including thermal radiation increases the conductivity on the order of 10 to 20 percent. Increasing the temperature and particle diameter enhance the radiation. A value of 0.82 W/m-K was used for this study. No credit is taken for particle or gas motion.

Heat transfer in flowing packed beds is generally considered by most investigators to be represented by heat transfer in a quasi continuum. Examples of several approaches are contained in References 8.5 through 8.10. The two-dimensional steady-state heat transfer equation for a continuum can be written as

$$u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} = \frac{k}{\rho c_p} \frac{\partial^2 T}{\partial x^2} + \frac{k}{\rho c_p} \frac{\partial^2 T}{\partial y^2} + \frac{q'''}{\rho c_p}$$

The solution of the equation requires definition of the velocity field u and v and appropriate boundary conditions. The following discussion assumes that the u velocity profile is uniform such that $v = 0$ and that axial conduction is small compared to axial convection and radial conduction. Under these conditions the transport equation becomes

$$u \frac{\partial T}{\partial x} = \frac{k}{\rho c_p} \frac{\partial^2 T}{\partial y^2} + \frac{q'''}{\rho c_p}$$

This equation can be made partially dimensionless by letting $X = x/L$ and $Y = y/a$ where L is the channel length and a is the width; thus,

$$\frac{u}{L} \frac{\partial T}{\partial X} = \frac{k}{a^2 \rho c_p} \frac{\partial^2 T}{\partial Y^2} + \frac{q'''}{\rho c_p}$$

or

$$\frac{\partial T}{\partial X} = \frac{kL}{a^2 \rho c_p u} \frac{\partial^2 T}{\partial Y^2} + \frac{q'''L}{\rho c_p u}$$

The rate of temperature rise along the distance X is defined by the internal heat generation and the rate of heat diffusion normal to X . The dimensionless group $(k/a^2 \rho C_p)(L/u)$ represents ratio of channel transit time to radial diffusion time. When this group is large, there is not much radial diffusion of heat and this is generally the case for low conductivity particulate flows.

Several limiting solutions to the heat transport equation can be stated directly. In the absence of lateral heat diffusion, $(k/a^2 \rho C_p)(L/u) \ll 1$, the temperature rise along X is

$$T(X, Y) = \int_0^X \frac{q''(X, Y) L}{\rho C_p u} dx$$

The temperature at any X and Y only depends on the heat source term $q''(X, Y)$. This is the source of temperature peaking in the blanket flow channel. The mean mixed or average temperature is

$$\bar{T}(X) = T_0 + \int_0^X \frac{\bar{q}''(X) L}{\rho C_p u} dx.$$

The average temperature rise is proportional to the power-to-flow ratio of the channel. Temperature peaking is proportional to the radial power distribution in the absence of lateral diffusion. These are the primary effects and form the basis for the majority of blanket thermal analysis in the present study. Some lateral heat transfer processes were considered, but were based on simplified heat transfer processes. The following discussion considers some of those processes and provides some estimate of their importance.

When the particulate flow initially enters the blanket flow channel it begins to heat by internal heat generation and it also begins to exchange heat with the flow channel walls. Since the walls are heat generating, there is a heat exchange to the particulates and an increase in particulate temperature along the wall. This region of increased temperature grows as a thermal boundary layer and, if the channel is long enough, the boundary layers from each side meet to give fully developed heat transfer. If the lateral heat transfer is only in one direction as it can be in the blanket, the thermal boundary may have to traverse the entire channel width to become fully developed.

Entry region heat transfer solutions have been developed for particulate flow in the absence of sources from a solution of

$$u \frac{\partial T}{\partial x} = \frac{k}{\rho C_p} \frac{\partial^2 T}{\partial y^2}$$

which can be written as

$$\frac{T(x, y) - T_w}{T_m - T_w} = \frac{2}{\sqrt{2\pi}} \operatorname{erf} \left(y \sqrt{\frac{u \rho C_p}{k x}} \right)$$

The local wall heat transfer coefficient is defined as

$$h_x = \frac{1}{\sqrt{\pi}} \sqrt{\frac{k \rho C_p u}{x}}$$

and the mean heat transfer coefficient over a length L

$$h_L = \frac{2}{\sqrt{\pi}} \sqrt{\frac{k \rho C_p u}{L}}$$

or

$$Nu_L = \frac{\bar{h} L}{k} = \frac{2}{\sqrt{\pi}} \sqrt{\frac{L \rho C_p u}{k}}$$

Entry region heat transfer is higher than fully developed heat transfer because of the steep temperature gradients in the thin thermal boundary layer. As the boundary layer thickens, the heat transfer decreases with distance x . At the other extreme is fully developed heat transfer where $\delta T/\delta x$ is a constant. Integration of the heat transport equation for this case can be done to define a Nusselt number. For flow between flat plates ($D = 2a$)

$$Nu_D = \frac{hD}{k} = 12$$

or inside a tube

$$Nu_D = \frac{hD}{k} = 8.$$

Fully developed heat transfer is less than entry heat transfer and represents the asymptote.

Between those two extremes is a transition. The relative importance of the two heat transfer regimes along the length of the channel can be defined by the relative channel length where the thermal boundary layers from each side of the channel meet. The thermal boundary layer thickness (δ) can be estimated from an order of magnitude of terms in the heat transport equation by

$$\frac{\delta}{a} \propto \sqrt{\frac{k}{\rho C_p a^2}} \frac{L}{u} \frac{x}{L}$$

The quantity in the radical represents the ratio of channel transit time to the radial diffusion time. The heat transfer is fully developed over most of the channel when this term is much larger than one. This would be the case for high thermal diffusivity flows at low velocity. Otherwise, the heat transfer is by a developing thermal boundary layer when this term is much less than one. The heat transfer is intermediate when the term is of order one, and can involve both developing and fully developed heat transfer along the length of the channel. The situations for the flowing Li_2O particulates of the present study tend to fall in the intermediate category. This is illustrated in the following table for $\alpha = 0.24 \times 10^{-6} \text{ m}^2/\text{sec}$

Item	u (m/sec)	a (m)	$\sqrt{\frac{a}{\rho c_p \alpha}} \frac{1}{u}$	$h_L(a)$ ($\text{W/m}^2\text{-K}$)	$h_D(b)$ ($\text{W/m}^2\text{-K}$)
Blanket, Zone 1	.01	.06	.20	77	82
Blanket, Zone 10	.001	.06	.63	24	82
HX (Tube)	.01	.01	2.40	77	328

$$(a) \quad h_L = \frac{\text{Nu}_L k}{L}$$

$$(b) \quad h_D = \frac{\text{Nu}_D k}{D}$$

The table shows that the blanket Zone 1 has an important contribution from entry heat transfer where the average heat coefficient is $77 \text{ W/m}^2\text{-K}$ (using total channel length) as compared to the fully developed value of $82 \text{ W/m}^2\text{-K}$. Fully developed heat transfer would exist on the downstream portion of the channel. The outer blanket Zone 10 has a fully developed heat transfer region which is longer than the Zone 1. The heat exchanger tube channel is dominated by fully developed heat transfer and with a short entry region.

The heat transfer coefficient is much larger than the blanket channels because of the small hydraulic diameter. The relative heat transfer lengths involved are shown in Figure B.1.

The previous discussion is based on several assumptions which can not be evaluated without experimental results. The thermal conductivity of the particulate bed is based on values derived from static beds with a stagnant cover fluid. The motion of the bed would certainly enhance the lateral heat transfer but the amount is uncertain. The velocity distribution of the flowing particles is not known. It could be expected that particle velocities would be slower toward the wall as in fluid flows and that would tend to reduce the surface heat transfer coefficients.

In summary, heat transfer in the flowing particulate blanket is restricted by the poor thermal diffusivity of the Li_2O and low thermal conductivity of the Li_2O and He mixture. This leads to heat transfer process which tends to be continuously developing rather than being fully developed. This complicates analysis because multidimensional analysis is required to obtain more definitive analysis results. Heat transfer experiments would be useful to help define the heat transfer properties of flowing particulates more accurately.

Figure 6B.1 Heat Transfer Coefficient versus Distance for Blanket and Heat Exchanger Flow Channels.

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6B-12

APPENDIX 6C

HEAT CONDUCTION IN A FLAT PLATE WITH

UNEQUAL BOUNDARY CONDITIONS

The purpose of this appendix is to present an analytic solution for steady-state conduction through a heat generating plate with unequal convective boundary conditions. The heat conduction equation can be written as

$$\rho C_p \frac{\partial T}{\partial t} = \dot{q}''' + k \frac{\partial^2 T}{\partial x^2}$$

and, for a flat plate at steady-state, it can be written as

$$\frac{d^2 T}{dx^2} = -2Q''$$

where $Q'' = \frac{\dot{q}''' a^2}{2k}$
 $X = x/a$
 x = distance from plate center line
 a = plate half thickness
 $\dot{q}'''$ = internal heat generation per unit volume
 k = thermal conductivity
 T = temperature

The boundary conditions are as follows:

$$-\left. \frac{dT}{dx} \right|_{x=1} = B_1 (T - T_1) \Big|_{x=1}$$

$$\left. \frac{dT}{dx} \right|_{x=-1} = B_2 (T - T_2) \Big|_{x=-1}$$

where the Biot number is defined as

$$B_i = \frac{h_i a}{k} \quad (i = 1, 2)$$

h_i = surface heat transfer coefficient $(i = 1, 2)$

T_i = sink temperature $(i = 1, 2)$

The general solution is

$$T = -Q'' x^2 + C_1 x + C_2$$

Applying the boundary conditions and solving for C_1 and C_2 gives the following expressions for the constants:

$$C_1 = \frac{\left[\left(1 + \frac{2}{B_1} \right) - \left(1 + \frac{2}{B_2} \right) \right] Q'' + (T_1 - T_2)}{2 + \frac{1}{B_1} + \frac{1}{B_2}}$$

$$C_2 = \frac{\frac{1+B_2}{B_2} \left[\left(1 + \frac{2}{B_1} \right) Q'' + T_1 \right] + \frac{1+B_1}{B_1} \left[\left(1 + \frac{2}{B_2} \right) Q'' + T_2 \right]}{2 + \frac{1}{B_1} + \frac{1}{B_2}}$$

APPENDIX 6D

PRINCIPLES OF FLOWING PARTICULATES

The gravity flow of particulate material is contained in the literature under the general category of powder mechanics or flow of bulk solids. Literature of interest to this topic are given by References D.1 through D.12. The technology is quite empirical, however, there are some basic principles and properties relevant to the Li_2O blanket concepts considered in this report. Three definitions of importance to particulate flow are:

- Void Fraction - Ratio of the volume of void space to the volume of the particle mixture. This is typically 0.40 for a random mix of spherical particles.
- Angle of Repose - Angle from the horizontal to the free surface of a pile of particulates. This is typically 25° for spherical particles.
- Angle of Sliding - Angle from the horizontal to the shear plane for flowing particulates. This angle is larger than the angle of repose and is typically 55° for free flowing spheres. It is the minimum angle required for the sloping sides of a free flowing channel.

Particulate flow is not necessarily analogous to fluid flow. This is because the shear relationship between moving layers of dense beds does not involve a property like fluid viscosity. While there is friction, the mechanism is different. The particulates can "lockup" mechanically which prevents their free flow. This can occur in channels and orifices which

approach a few particle diameters. Generally, openings must be more than about 5 particle diameters to assure free flow. Orifices can be used to control particulate flow provided that a minimum opening criteria is not violated. Flow control orifices can only be used at the bottom of flow channels. The lockup mechanism does not generally allow particulates to be pushed through channels or openings. Particulate beds must "dilate" (expand) slightly for them to be free flowing and a compressive force can prevent this from occurring.

The implications from bulk flow technology is that flow channels must be properly sloped to assure free flow and the flow can be controlled by properly shaped orifices placed at the bottom of flow channels. The empirical nature of bulk flow technology would also require experimental evaluation of any flowing particulate blanket concept.

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SECTION 7

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COUPLING THE FUSION REACTOR TO THE THERMOCHEMICAL HYDROGEN PRODUCTION PROCESS

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7.0 COUPLING THE FUSION REACTOR TO THE THERMOCHEMICAL HYDROGEN PRODUCTION PROCESS

7.1 Introduction and Summary

The objective of this study was to assess the feasibility of producing hydrogen by utilizing a Tandem Mirror Reactor (TMR) and one of three promising thermochemical or thermo-electrochemical cycles. The complete system consists of four subsystems as illustrated in Figure 7.1. The characteristics of the fusion reactor and the reactor blanket subsystems were presented in Sections 4, 5, and 6. The characteristics of the three hydrogen production cycle subsystems are presented in Section 8. A fourth subsystem, the energy conversion and transport system, couples the fusion reactor to the thermochemical plant. This subsystem consists of the heat exchangers and coolants required to transport thermal energy from the fusion reactor blanket to the thermochemical process. It also includes the energy conversion equipment, associated thermodynamic cycle, and working fluids utilized to convert thermal energy into mechanical work or electricity. The entire subsystem is discussed in the following.

As pointed out in Section 8, of the three cycles reviewed the GA sulfur cycle was selected for preliminary evaluation of: (a) the various options available for coupling a fusion reactor to these types of cycles; and (b) the problems associated with each option. The thermal and work (or electrical) input requirements for the three different cycles are similar in many respects. Although the utilization of these energy forms within the different cycles differs significantly in some cases, the energy conversion and transport systems could possibly be quite similar.

FIGURE 7.1. Fusion-Based Hydrogen Production System.

The primary results of this aspect of the study are summarized below.

- (a) From a system arrangement standpoint, the simplest means for meeting the energy conversion and transport needs of the GA thermochemical cycle as developed by GA, utilizing state-of-the-art equipment as much as possible, is through the use of a single sodium transport loop between the fusion reactor blanket (either Li_2O or Li/Na) and the thermochemical cycle. Process heat exchangers and a steam generator are arranged in series. Process heat would be transferred from the high temperature portion of the loop, and a bottoming steam cycle operating at conditions typical of LMFBR technology would utilize the lower temperature energy for generating electricity. The sodium loop would isolate the thermochemical cycle from activation products and tritium.
- (b) The two basic energy requirements are for process heat and shaft work or electricity. The fractional division of energy together with the four primary heat exchangers required follow:

<u>Heat Exchanger</u>	<u>% Heat</u>	<u>Process Media</u>	<u>Sodium Temp.</u>		<u>Process Temp.</u>	
			<u>High</u>	<u>Low</u>	<u>High</u>	<u>Low</u>
Decomposer	19	$\text{SO}_3, \text{SO}_2, \text{O}_2$	1159°k	1056°k	1144°k	713°k
Boiler	22	$\text{H}_2\text{SO}_4, \text{H}_2\text{O}$	1056	935	713	685
Evaporator	18	$\text{H}_2\text{SO}_4, \text{H}_2\text{O}$	935	838	685	475
LMFBR Type Steam Generator	41	Steam for Electricity Generation	838	615	758	515

- (c) Limitations in the technology base center around the blanket design, the intermediate heat exchanger ($\text{Li}_2\text{O/Na}$ or Na/Na), the transport of sodium between the intermediate heat exchanger and the process, and subsequently the transport of heat from the sodium to the process streams.

The first two items were discussed in the blanket section of the report. For the latter two items, the combination of temperatures and fluids involved require a new assessment of available materials for piping and heat exchangers in terms of their mechanical properties and chemical compatibility. Possibly the development of new materials will be required. Clearly no similar commercial state-of-the-art application exists; however, there has been some work done for the space program and recent reviews for the fossil energy program on the operation of alkali metal vapor cycles for generating power at similar operating temperatures, which may be applicable to some extent. (7.1, 7.2)

- (d) Transport and conversion systems based on the use of helium, or potassium (vapor and liquid), are possibilities for coupling the TMR to the GA thermochemical cycle. Both of these options appear to be capable of higher efficiency than the simple series sodium loop option. The helium option has a technology base stemming from high temperature gas cooled reactors (HTGR's); however, high pressures (50-70 atmospheres) and high parasitic pumping power requirements would have to be dealt with in both the blanket and the transport and conversion system. the technology base for potassium appears to be less developed than for either sodium or helium. Potassium system pressures at vapor locations would be 4-5 atmospheres. This is significantly lower than the helium option, but also significantly higher than an atmospheric pressure sodium system.
- (e) The high temperatures at which process heat from the fusion reactor is required can be significantly reduced from that required by the cycle developed by GA by a simple modification. The modification involves recycling part of the

hydrogen product stream and utilizing the heat of oxidation in the decomposer. The hydrogen could be combusted external to the decomposer heat exchanger or oxidized insitu in a reaction vessel.

There is a significant reduction in overall efficiency as a result of hydrogen recycle--on the other hand, it significantly reduces the temperature/materials issues associated with (a) the blanket, (b) the transport of the thermal energy from the blanket to the thermochemical process, and (c) the heat exchanger between the reactor coolant and the high temperature process media.

- (f) System efficiencies can vary widely (from 27 to 48%) for the options evaluated depending on the approach utilized (see Table 7.1). Since there is no input of feedstocks to the combined fusion/hydrogen plant, plant efficiency is only important in terms of its indirect effect on hydrogen production costs. Minimizing hydrogen production costs (which does not necessarily imply maximizing efficiency) is the criteria upon which this plant system should be based. Attempting to obtain very high efficiency utilizing extremely high temperatures may result in high material costs, excessive down time, and higher production costs than the economic optimum. Clearly an optimization of production costs is beyond the scope of this preliminary assessment; however, it must be the basic criteria for any meaningful plant engineering design study conducted beyond this preliminary technological assessment.

TABLE 7.1
COMPARISON OF CONVERSION AND TRANSPORT OPTIONS

<u>Options</u>	<u>Maximum TMR Blanket Outlet Temperature Considered</u>	<u>Efficiency %</u>	<u>Remarks</u>
A. Single Series Sodium	1159°K	37	Undeveloped Technology at High Temperature, Technology Base Below 800°K
B. Helium Series/Parallel	1255°K	46	High Pressure, High Pumping Power, Technology Under Development
C. K Series/Parallel	1163°K	48	Limited Technology Base
D. K Parallel	1163°K	43	Limited Technology Base
E. Sodium With Hydrogen Recycle	838°K	27	Least Technological Development Required

In the subsections which follow, the following is presented: (a) energy requirements of the GA cycle; (b) energy supply characteristics of the TMR; (c) conversion and transport options; (d) assessment of the state-of-the-art of some of the related equipment; (e) thermodynamic limits; (f) estimated system efficiencies and related transport and conversion system operating conditions, and; (g) hydrogen recycle, a method of reducing the peak material temperatures that must be met by transport and conversion equipment and the fusion reactor blanket.

7.2 Energy Requirements of the GA Cycle

Information on the energy and related temperature requirements of the GA cycle is limited in detail due to proprietary considerations. Information on the energy conversion and transport system is also limited. The information made available to date by GA is illustrated and tabulated in Figure 7.2. The energy units per unit of hydrogen production are KJ/.993 g-moles H_2 . The overall efficiency of the GA system is:

$$n = \frac{\text{Higher Heating Value of } H_2 \text{ Produced}}{\text{Thermal Energy Input to Chemical Processes and Electricity Generation}}$$

$$= \frac{285}{608} = .47$$

The higher heating value of hydrogen (i.e., standard enthalpy of formation of liquid water) is 285 KJ/.993 g-moles H_2 . The total thermal energy required is shown in Figure 7.2, 608 KJ/.993 g-moles H_2 .

GA SYSTEM -- ENERGY SUPPLY AND DEMAND

FIGURE 7.2

Available information on the related temperatures of the HTGR heat source and in the process are also shown in Figures 7.2 and 7.3. The total amount of thermal energy (and the related temperatures) required from the HTGR core to produce a unit of hydrogen energy (and related temperatures) is available. The distribution of energy to three intermediate heat exchangers as the primary coolant passes around the primary helium loop is also illustrated. The amount of work (or electrical energy) extracted from the primary loop utilizing a direct cycle gas turbine is also shown. The amount of thermal energy transferred (and related temperatures) for the three secondary helium loops are as shown. In addition, the amount of thermal energy and related temperatures for the secondary loop which interfaces with the SO_3 decomposer is illustrated.

The net amount of heat and related process temperatures for the sulfuric acid decomposer, boiler, and evaporator are illustrated in different form in Figure 7.3. The quantities of thermal energy and work that are transferred between the various thermochemical process sections via heat and work recovery are illustrated in Figure 7.4. The net surpluses and deficits are accommodated by the energy conversion and transport system. As shown there, the bulk of the thermal energy supplied by the energy conversion and transport system goes to the sulfuric acid decomposition, boiling, and evaporation subsection of the process. Most of the electricity or mechanical work equivalent supplied goes to the subsection where HI is concentrated and separated from the $\text{HI/I}_2/\text{H}_2\text{O}$ solution.

The missing information is associated with the interface between the secondary helium loop and: (a) the thermochemical process, particularly the H_2SO_4 boiler and evaporator; and, (b) the generation of the electrical power required by the thermochemical cycle in addition to that produced by the direct cycle gas turbines in the primary helium loop.

H₂SO₄
PROCESS HEAT REQUIREMENTS

FIGURE 7.3

ΔH = Enthalpy Change, Reactants to Products
 Q = Heat
 W = Work
 C = Chemical Energy - Higher Heating Value of Hydrogen
 Units = Kilojoules Per 0.993 Gram - Mole H₂ Product

FIGURE 7.4. GA Energy Flow Diagram

In view of these unknowns, the following simplified approach was taken. The major thermal inputs to the thermochemical cycle are for evaporating water from sulfuric acid, boiling sulfuric acid, and decomposing sulfuric acid (or SO_3). The quantities of heat required and relative process temperatures are illustrated in Figure 7.3. Heat recovered from the thermochemical process for use in the conversion and transport system, and heat delivered from the conversion and transport system to other subsections of the thermochemical process are small in comparison as shown in Figure 7.4. The process temperatures are not defined and, therefore, these smaller heat quantities were ignored for this preliminary feasibility assessment. The work or electrical power requirements are known.

7.3 Energy Supply Characteristics and Approach Taken to Assess Coupling Options

Thermal energy at useful temperatures is generated in three locations within the TMR: (1) the blanket; (2) the first wall; and, (3) the direct converters. In addition, D.C. electrical energy is generated in the direct converters from the high energy particles which flow to those locations in the TMR. In this scoping study of the reactor design, the electrical requirements of the TMR will be provided from the output of the D.C. converters. In addition, at this point in time it appears that the temperature of the thermal energy extracted from the first wall will be limited to less than 700°K . First wall thermal output represents only about 5% of the blanket thermal output. Logically, this thermal energy would be used for generating electrical power, since the temperature level is too low for direct use in the thermochemical cycle. Thermal energy from the direct converter could potentially be made available at 900°K and will represent about 5% of the reactor energy output. This thermal energy could also be used to generate electricity or possibly as direct thermal input to the thermochemical cycle.

Following this logic path, the blanket thermal energy would then be used to meet at least 90% of the process heat and electrical requirements within the thermochemical cycle. The transport and conversion system associated with the blanket can be totally dedicated to the thermochemical cycle. The direct converter electrical output can be dedicated to the TMR. The thermal output of the direct converter and first wall can also be dedicated to the thermochemical cycle. Since the combination represents only about 10% of the total thermal energy available, these two sources were not evaluated in detail in the study. Effort in the study concentrated on the blanket as the major thermal energy source.

The blanket can be viewed as a decoupled heat source. The design of the transport and conversion system involves the selection of the following: coolants, materials, parallel and/or series arrangements for heat exchange and power generation, operating temperatures, pressures, flow rates and/or the thermodynamic cycle. This must be accomplished to meet the needs of the thermochemical cycle while still not exceeding materials capabilities, excessive parasitic loss limits, safety issues, and other practical design considerations that relate to maintainability and cost. Other considerations relevant to option selection follow.

7.4

Thermodynamic Limitations on Energy Conversion and Transport Options

In consideration of the details of various energy conversion and transport options for coupling nuclear heat sources to synthetic fuel production, it is easy to lose sight of fundamental thermodynamic limitations which limit system efficiency. The maximum efficiency of any water splitting process which utilizes a heat source at high temperature, T_h , and rejects heat to a sink at low temperature, T_L , is:

$$\eta = \frac{\Delta H}{\Delta G / \left[\frac{T_h - T_L}{T_h} \right]}$$

where

ΔH = standard enthalpy of formation of liquid water
= 286 KJ/g-mole

ΔG = standard free energy of formation of liquid water
= 237 KJ/g-mole

Assuming a sink temperature of 25°C, or 298°K, the following maximum possible efficiencies can be calculated corresponding to certain selected heat source temperatures of Figure 7.2.

<u>Heat Source Temperature, °K</u>	<u>Second Law Maximum Efficiency</u>
1255 - HTGR Core Outlet	.92
773 - HTGR Core Inlet	.74
1255 to 773 (linear relation between heat flow and temperature assumed) - HTGR average heat source temperature	.84 (Upper limit on the GA system)

The actual efficiency of the GA system, in terms of the thermal input of 608 KJ/.993 g-mole of H_2 is:

$$\eta = \frac{285}{452 + 48 + 108} = .47$$

where

452 = process heat

48 = enthalpy change in helium primary loop converter to work

108 = thermal inputs to power conversion equipment to generate additional 65 KJ of electricity required.

In the GA system, 113 KJ/.993 g-moles H_2 of work or electricity is required. Of this amount, 48 KJ are obtained utilizing a direct cycle gas turbine as a topping cycle to the thermochemical process. The balance, 65 KJ, is obtained by converting the remaining 108 KJ from the HTGR, plus 39 KJ of heat recovered from the process. This latter 65 KJ of work results from utilizing power conversion equipment which is highly integrated in a parallel and series manner with the thermal inputs that enter the thermochemical process. Since the 39 KJ is recovered heat, it does not enter into the denominator of the previous equation which includes only the net total thermal input into the process from the HTGR.

If we assume that the basic inputs to the GA process are fixed (i.e., 452 KJ/.993 g-moles H_2 of thermal energy input at the required process temperatures and 113 KJ/.993 g-moles H_2 of mechanical work, or its equivalent electrical energy)

then the overall thermal efficiency of the system can only be improved by increasing the efficiency at which the electricity is generated. On the other hand, if the thermal input is fixed, then reducing the electrical requirements will also increase the efficiency. Two hypothetical limits are of interest:

Maximum GA System
Efficiency at 100 % Elect.
Generation Efficiency

$$n = \frac{285}{452 + 113} = .50$$

Maximum GA System
Efficiency with No
Electricity Requirement

$$n = \frac{285}{452} = .63$$

Thus even with very limited information on the GA system, some conclusions can be drawn regarding the actual system in comparison to ideal limits. First, with the exception of converting the 113 KJ of work used for pumps and compressors into moderate to low-grade heat (at most 450°K), this input is totally an irreversible loss.

This alone does not, however, account for all the irreversibilities since if the need for this work input were completely eliminated, the efficiency rises to only .63, as shown above, compared to the theoretical maximum of .84. The additional losses occur in the thermochemical portions of the process due to required quenching of product streams to prevent recombination and transferring heat across finite temperature differences. Losses also occur when heat is recovered within the process, converted to work within the process (i.e., excluding transfers to Section 5, Figure 7.4), and subsequently utilized in the process to overcome pressure drops again resulting in nearly a totally irreversible loss. Since the GA

system is highly heat integrated and utilizes topping cycles to generate the required electricity or mechanical work equivalent, maintaining the system efficiency or attempting to improve it when other coolants, system arrangements, and lower heat source temperatures are considered, can be expected to be difficult. This is discussed in the following.

7.5 General Engineering Considerations in the Selection of the Energy Conversion and Transport System

Evaluation by this study team and reviews of other somewhat similar studies^(7.1-7.6) of many combinations of working and transport fluids, thermodynamic cycles, parallel and/or series arrangements of equipment, materials, etc., for high temperature process heat and power generation appear to lead to the selection of only four reasonable conversion and transport options for the following reasons.

Transport fluids appear to be limited to helium and alkali metals (sodium, potassium, and possibly cesium) based on chemical compatibility with piping, heat exchange, and electrical generation equipment and chemical stability at the temperatures of interest. Liquid alkali metals have low vapor pressures at the temperatures of concern, which reduces structural loads due to system pressures. Pumping power requirements are also low. Helium, on the other hand, must operate at high pressure (~1000 psia) in order to reduce pressure drops in transmission circuits and conversion and transport equipment. If long distances are involved, the pumping power could still be excessive. Helium, on the other hand, has obvious safety advantages over the alkali metals.

Na has too low of a vapor pressure at the temperatures of interest to be used as a working fluid in thermodynamic cycles (i.e., expansion turbines) to generate electricity. K appears to be acceptable as a working fluid for Rankine topping cycles and cesium appears even more desirable due to its higher vapor pressure. Cesium, on the other hand, is not as readily available as potassium. Helium can be utilized in the Brayton thermodynamic cycle to generate electrical power, but here again high pressures are required to reduce equipment size and parasitic losses.

Equipment arrangements, materials, safety issues, etc., have been addressed for electrical power generation at the temperatures of interest for: (1) helium as part of the HTGR development effort; and (2) the alkali metals as part of LMFBR development, space power systems development, and recently the development of high efficiency fossil fuel electricity generating plants. Similar assessments have been made for some process heat applications utilizing the HTGR with helium as the coolant. No assessments appear to have been made of alkali metals as thermal transport media for interfacing with very high temperature process heat applications.

In summary, in the area of converting thermal energy to electricity, there has been some development in the areas of system arrangements, sizing of equipment, materials selection, definition of the thermodynamic operating conditions, and assessment of losses and safety issues for both helium and the alkali metals. There has been little development in the process heat area which can be directly utilized for this application.

Therefore, the four resulting energy transport and conversion subsystem options described below are a combination of extrapolating previously developed options for power generation while still meeting the thermal input needs of this particular process. It is perhaps fortuitous that the maximum temperature requirements for the thermal input to the process are very similar to the temperature levels that have been investigated for generating electrical power using both helium and the alkali metals.

7.6 Thermal Transport and Energy Conversion Options

There are many different hypothetical options for transporting the required process heat from the blanket to the thermochemical cycle and for generating the required electricity or its equivalent, mechanical work. However, when the choice of working fluids (and/or thermal transport media), heat exchange equipment, thermodynamic cycles, and series and/or parallel arrangements of transport and conversion portions of this subsystem is limited to options commercially available, or options which have received some development, and are simple enough to potentially be economically and operationally feasible, the choices appear to reduce to four. These options are illustrated in Figures 7.5 through 7.9 and include:

- A. A single series Na thermal transport loop with process heat topping and electrical generation bottoming. Bottoming components would be based on LMFBR technology.
- B. A combined series/parallel subsystem utilizing helium as the thermal transport media and working fluid for generating electricity. This option has an HTGR

*Heat and Work Units - $\frac{\text{KJ}}{.993 \text{ g-moles H}_2}$

$$n = \frac{285}{766} = .37$$

MPa	mass time
°K	Fluid

FIGURE 7.5. Single Series Na Loop - Electric Power Generation Based on LMFBR Technology - Subsystem A.

FIGURE 7.6. Temperature Versus Heat Supply and Demand for the Single Series NA Loop - Subsystem A.

FIGURE 7.7. Combined Series/Parallel Subsystem Utilizing He as the Thermal Transport Media and Working Fluid for Generating Most of the Required Electricity - Subsystem B.

$$\eta = \frac{285}{568} = .48$$

FIGURE 7.8. Combined Series/Parallel Subsystem Utilizing K as the Working Fluid - Subsystem C.

FIGURE 7.9. Parallel K Subsystem with Separate Process Heating and Electricity Generation - Subsystem D.

technology base, and is similar to the GA system previously described; however, an attempt has been made to greatly simplify the system.

- C. A single series potassium vapor topping cycle for electrical generation with some process heat bottoming and steam electric bottoming at the potassium condenser.
- D. A parallel potassium (or potassium and sodium) arrangement with electrical generation via K vapor topping and steam cycle bottoming in the one branch and process heating in the other.

These options each have pros and cons and will now be briefly discussed. Limits to large deviations from these four options are also discussed.

7.7 Comparison of Four Candidate Energy Conversion and Transport Options

The He, Na, and K energy conversion and transport options illustrated in Figures 7.5 through 7.9 are discussed in more detail in the following. Each option has certain advantages and disadvantages and a final selection will require further performance, safety, and cost analysis. Selection of a system based on arrangement simplicity would lead to the single series sodium loop. However, it has reduced efficiency, compared to other options, and more safety concerns than the helium based option.

7.7.1. Single Series Sodium loop - Subsystem A

The single sodium series loop appears to provide the simplest means for meeting the process heat and electrical requirements of the GA thermochemical cycle. Process heat exchangers and a steam generator are arranged in series. Process heat is transferred from the high temperature portion of the loop, and a bottoming steam cycle operating at conditions typical of LMFBR technology would utilize the lower temperature energy

for generating electricity. The sodium loop would isolate the thermochemical cycle from activation products and tritium. The maximum sodium loop temperature would be 1159°K. The efficiency of converting blanket thermal energy to hydrogen chemical energy (higher heating value) would be 37%.

The void in the technology base centers around the blanket design (Li_2O particulates and Li/Na cauldron), the intermediate heat exchanger ($\text{Li}_2\text{O}/\text{Na}$ or Na/Na), the transport of sodium between the intermediate heat exchanger and the process, and subsequently the transport of heat from the sodium to the process streams.

The major safety issues center around the isolation of liquid sodium from H_2SO_4 and its decomposition products, and the isolation of sodium from the steam in the steam generator. The safety issues with the steam generator are apparently being adequately dealt with for the LMFBR program^(7.7-7.13) in terms of: (1) the definition of the design basis accident; (2) prediction of performance during an accident; (3) design specifications to minimize any potential for a catastrophic steam/sodium reaction; and (4) clean-up and refurbishment if an accident occurs.

As in the materials area, little if any in-depth work has been done to evaluate the safety aspects of decomposing sulfuric acid utilizing very high temperature sodium as a heat transport media. In addition, the materials selection and safety issues are highly related. For example, in the LMFBR steam generators Inconel 800, 2-1/4 Cr-1 Mo steel, and other similar materials are being utilized or considered for steam generator materials.

The design basis accident would consist of a small pin hole steam leak into the sodium. This, in turn, would lead eventually to the double open-ended guillotine break of the one tube and subsequently the failure of six surrounding tubes due to the localized sodium/steam reaction. The resulting assessments of the steam/sodium reaction, generation of hydrogen, increase in pressure in the generator shell, and dumping of the steam and sodium through accident control operations are all felt to be within safety requirements and preclude the possibility of a catastrophic accident.

A catastrophic accident appears possible if there were massive rupturing of the steam generator tubing, intimate mixing and reaction of large amounts of steam and sodium within the generator and no adequate means for relieving the pressure build up due to hydrogen generation. The materials used in LMFBR steam generator tube construction have both high strength and ductility at the temperatures of concern, thus precluding initiation of massive rupturing. Experimental tests suggest that the seven tube design basis accident criteria may be overly conservative since experiments have shown that the equivalence of one guillotine break is all that is likely to occur.

In the process heat exchangers, particularly the decomposer, the temperatures are much higher than in the steam generator. The chemical composition on the process side is also different, consisting of a small amount of water, H_2SO_4 , and its decomposition products. Materials with any ductility at these

temperatures have very low strength. Refractory metals have high strength but low ductility. Both of these types of materials could potentially lead to catastrophic failure.

In summary, the advantages of this option are: (1) a simple physical arrangement; and (2) minimum blanket and/or intermediate heat exchanger outlet temperatures. The areas of concern are: (1) low thermodynamic efficiency due to reduced maximum system temperature and low electric power generation efficiency; (2) safety and materials issues associated with the process heat exchangers; and (3) reduced safety associated with the generation of electric power in comparison to a helium-based system.

7.7.2 Combined Series/Parallel Helium Loop - Subsystem B

The combined series/parallel subsystem utilizing helium as the thermal transport media and working fluid for generating all or part of the electricity would be the most desirable system based on safety if the materials, pressure drop, and/or pumping power requirements could be met. This system consists of two loops. The first loop consists of a gas turbine expander and a gas turbine compressor connected in series. Located between the expander and the compressor are an H_2SO_4 boiler, and an $\text{H}_2\text{SO}_4/\text{H}_2\text{O}$ evaporator connected in series. These, in turn, are connected in parallel with a steam generator. The thermal process heat requirements of the boiler and evaporator can be met and with the direct cycle gas turbine and steam turbine simultaneously generate the 113 required KJ of electricity.

The second loop provides the high temperature heat required for decomposition of H_2SO_4 .

Increasing the pressure ratio of the first loop gas turbine and increasing the flow rate would allow the generation of more electricity from the gas turbine while still meeting the process heat requirements of the H_2SO_4 evaporator and boiler. However, there is an optimum pressure ratio where the maximum electricity will be produced for a fixed flow rate. This is not significantly different from the amount of electricity produced under the operating conditions shown in Figure 7.7. In addition, with an increase in pressure ratio, the discharge temperature from the expander is reduced. A point will also be reached where the discharge temperature of the expander is below the H_2SO_4 boiler discharge temperature, thus presenting another limit to increasing the pressure ratio.

In summary, the advantage of the helium-based system is its inherent inertness compared to liquid metal coolants. Its drawbacks are the high operating pressures required and the high pressure drop and pumping power requirements. The efficiency numbers given above do not account for parasitic pumping requirements to transport the helium to or through the process heat exchangers. Nor do they include the pumping power requirements of the intermediate heat exchanger (IHX) and piping between the IHX and the turbine and compressor. Given the current design size of the TMR and the length and complexity of the relevant manifolding, these pumping requirements could seriously erode the efficiency numbers. The lengths and structural requirements needed to accommodate high pressure/high temperature helium could also result in high costs.

7.7.3 Potassium (Liquid/Vapor) Based Energy Conversion and Transport Subsystems C and D

Generation of electric power using a topping turbine is not practical with sodium due to its low vapor pressure at the temperatures of interest. This limits the overall system efficiency of sodium-based systems. Potassium, on the other hand, does have a reasonable vapor pressure at the temperatures of interest and has been investigated for use in topping cycles--at the temperatures of interest. At least two fairly simple arrangements, designated C and D, are possible as shown in Figures 7.8 and 7.9. Figure 7.8 (Subsystem C) illustrates a combined series/parallel arrangement. The first loop utilizes a potassium vapor topping turbine with process heat bottoming at the boiler and evaporator, and also steam electric bottoming. Thermal energy to chemical energy conversion efficiency is 48%.

The second loop would utilize liquid potassium to transport thermal energy to the decomposer. Potassium vapor or liquid sodium might also be considered for the second loop.

Figure 7.9 (Subsystem D) illustrates a parallel arrangement where process heating and electric generation are done in parallel. The process heat exchangers are arranged in series. Electrical power generation is accomplished using a potassium vapor turbine and a steam bottoming turbine. The process heat loop could use liquid K or Na. Potassium vapor might also be considered. Thermal to chemical energy conversion efficiency is 43%.

Subsystem C has a definite advantage from an efficiency standpoint. Subsystem D has the advantage of separate electrical generation and process heat loops.

It should be noted that the performance of the potassium vapor turbines and related steam turbines is based on data taken from Reference 7.1. In addition, no losses in efficiency have been included for pumping the alkali metal coolants to and from, and through the process heat exchangers. In comparison to the Na and helium options previously discussed, options utilizing potassium vapor turbines should have equal or better efficiency. System pressure will be higher than for sodium, but much lower than for helium. The development of the technology for potassium would seem to lag both that for sodium and helium.

H₂ Recycle--A Method for Reducing the Materials/Temperature Requirements

In the preceding sections energy conversion and transport subsystems have been selected which meet the thermochemical process heat and temperature requirements and also generate electricity at high efficiency. Although fairly high efficiencies can be obtained, this approach appears to tax the state of technology, particularly in the materials area due to the high temperature requirements. By reviewing Figure 7.3, significant gains in the materials area could be made if the decomposer heat requirements could be met in some other way. The balance of the process heat needs (boiler and evaporator) and the electrical demands could be met using technology at temperatures more consistent with a fossil fuel or LMFBR base.

One possible way of achieving this end is illustrated in Figures 7.10 through 7.15. The approach involves recycling part of the hydrogen product to the decomposer where it is either oxidized in-situ, or combusted externally with the heat transferred through heat exchanger walls. Although this scheme could reduce the materials/temperature problems, it does result in a significant reduction in overall plant efficiency. The fraction of H₂ product that must be recycled to reduce the temperature requirements that must be met by the energy conversion and transport subsystem is illustrated in Figure 7.11. Also illustrated is the fraction of the total thermochemical plant process heat that must be met by hydrogen recycle above a given temperature. The overall system efficiency is also illustrated. These curves are based on modifying the Single Series Sodium Loop--Subsystem A.

Q_R = Total thermal energy from the fusion reactor blanket utilized for process heat and electric power generation.

H_2 = Total higher heating value of hydrogen from thermochemical cycle.

X = Fraction of hydrogen recycled as a high temperature heat source for H_2SO_4 decomposition.

FIGURE 7.10. Energy Flow Diagram for H_2 Recycle.

FIGURE 7.11. Hydrogen Recycle Interrelationships.

FIGURE 7.12. Single Series Na Loop Modified with Hydrogen Recycle.

FIGURE 7.13. Temperature Versus Heat Supply and Demand for Single Series Na Loop Modified with Hydrogen Recycle.

* Heat and Work Units - $\frac{KJ}{.993 \text{ g-moles } H_2}$

FIGURE 7.14. Parallel Sodium System with H_2 Recycle.

FIGURE 7.15. Temperature Versus Heat Supply and Demand for the Parallel Na System with Hydrogen Recycle.

A similar approach could not be utilized for the other conversion and transport options since electric power generation in these concepts is based on using high temperature working fluids.

Meeting process heat demands below 800°K does not appear desirable for three reasons. First, a significant amount of process heat is required in the boiler at a constant temperature of 713°K. Therefore, a large amount of heat is required before any significant reductions in temperature are obtained. Second, temperatures must be reduced to about 600°K to reach another technology base (i.e., the light water fission reactor technology base). Third, overall hydrogen production system efficiency becomes so low that the resulting production costs would be prohibitive.

If the temperature of the energy conversion and transport system is limited to 838°K, which is on the high side of most LMFBR designs, then we have two options. The first is the modified single series system shown in Figures 7.12 and 7.13. Restrictions on pinch point temperature differences in the decomposer and steam generator result in high sodium flow rates. If the high flow rate is not desirable, then a parallel system illustrated in Figures 7.14 and 7.15 offers an alternative with about 60% the flow rate but the same efficiency.

The overall efficiency and energy flows are compared in Figure 7.16 for the H₂ recycle and the no recycle versions. For temperatures no larger than LMFBR conditions on the Na side

No H₂ Recycle

$$N_{\text{sys}} = \frac{285}{766} = .37$$

$$* \frac{\text{KJ}}{.993 \text{ g-moles H}_2}$$

H₂ Recycle

$$N_{\text{sys}} = \frac{180}{661} = .27$$

For $T_{\text{BLANKET}} \leq 838^\circ\text{K}$ (i.e., LMFBR Tech.), 23% of thermochemical heat must come from hydrogen recycle. This requires a recycle of 37% of the H₂ output.

FIGURE 7.16. Comparison of Energy Flows for H₂ Recycle and Non-recycle.

of the energy conversion and transport system, 37% of the H_2 must be recycled. Overall system efficiency is 27% as compared to 37% for the non-recycle version.

A similar result could be accomplished by generating excess electricity and using it to provide the decomposer heat. Since the efficiency of generating hydrogen by the thermo-chemical cycle is about the same as the efficiency of generating electricity, the overall plant efficiency using electric heating or H_2 recycle would be about the same.

Although the decrease in efficiency would appear to have unacceptable negative effects on the production costs, recycle of products for high temperature processes is utilized economically in fossil-based synthetic fuels plants, since the loss in efficiency is more than compensated for with the cost reductions in the high temperature heat exchange equipment. (7.14,7.15)

7.9

Application of the Tandem Mirror to Drive A
Synfuel Plant--Role of the Direct Converter

For the pure thermochemical cycles, as the name suggests, we are interested, not in electrical power production, but in the production of process heat and in the use of available thermal energy out of the fusion reactor. Two regions of the Tandem Mirror Fusion Reactor provide this energy: (1) the blanket and (2) the direct converter. We are primarily interested in using any surplus electrical energy out of the direct converter over and above that which will be needed to satisfy circulating (driving) power requirements for the reactor itself. The energy balance with this in mind proceeds as follows: We establish the following quantities (see Fig. 7.17):

$$AM = \text{The blanket thermal energy} \quad (1)$$

$$B(1-\eta_{dc}) = \text{The direct converter thermal energy} \quad (2)$$

$$B\eta_{dc} = \text{The direct converter electrical energy} \quad (3)$$

$$C = \text{Thermal energy contribution from} \\ \text{injected neutrals} \quad (4)$$

where, for the D-T cycle:

$$A = 0.8 P_{inj} \eta_A^0 \quad (5) \\ \text{(The neutron energy fraction)}$$

$$B = 0.2 P_{inj} \eta_A^0 + P_{inj} \eta_A \quad (6) \\ \text{(The alpha or charged particle fraction)}$$

$$C = P_{inj} (1 - \eta_A) \quad (7) \\ \text{(The fraction of injected neutrals that} \\ \text{don't get ionized)}$$

POWER BALANCE-MIRROR REACTOR WITH DIRECT CONVERSION PROCESS HEAT PRODUCTION FOR SYNFUELS

Fig. 7.17

The definitions of terms needed for Figure 7.17 are:

- η_A = ionization efficiency
- M = blanket energy multiplication (from exothermic reactions)
- η_{inj} = injector efficiency
- η_{dc} = direct converter efficiency (converting charged particle kinetic energy to electrical)
- η_{TH1} = thermal converter efficiency (converting thermal energy to electrical)
- η_{TH2} = thermal converter efficiency (converting direct-converter thermal energy to electrical)
- P_{AUX} = power required for pumps, refrigeration, and other fusion plant needs
- Q = fusion power produced/power injected

The value of P_{aux} is taken as a fraction, f , of the thermal and DC energy before conversion

$$P_{aux} = f (B + AM + C) \quad (8)$$

By definition

$$P_{circ} = P_{inj}/\eta_{inj} \quad (9)$$

7.9.1 The Influence of Q

We use the injected power as reference and set $P_{inj} = 1.0$. To set up the power balance for the reactor as an energy source supplying process heat for synfuel (H_2) production, it will be stipulated that the dc electrical energy component $P_{dc} = B\eta_{dc}$ of the direct converter will be used, in the first place, to satisfy the circulating power and the auxiliary needs of the reactor itself. There will be a specific value of Q at which this demand is exactly satisfied. For higher Q values, there will then be a surplus of dc electrical energy that can

be used by the thermochemical plant. No electrical energy is sold. For lesser values of Q , some thermal energy from the reactor blanket or from the thermal part of the direct converter will have to be converted to electrical energy to help drive the reactor. Figure 7.18 is a plot of how this electrical surplus or deficiency, $P_{dc} - (P_{aux} + P_{circ})$, varies as a function of Q . The representative set of input parameters used to provide data for this plot are given in Table 7.2:

Table 7.2

REPRESENTATIVE QUANTITIES FOR THE
TANDEM-MIRROR ENERGY BALANCE

P_{inj}	injection power	= 1.0 Unit
η_A	ionization efficiency	= 0.95
M	blanket energy multi- plication	1.20
η_{TH}	thermal efficiency	= 0.4
η_{inj}	injector efficiency	= 0.6
f	fraction of thermal and DC energy before conversion	= 0.02
η_{dc}	direct converter efficiency	= 0.55

It may be seen from Figure 7.18 that when the Q value is about 14 or higher, there begins to be a dc electrical component left over from the reactor that can be used to drive such things as electrolysis cells, pumps, motors, etc., in the synfuel plant.

Relative to the total process energy that the reactor has created for synfuel purposes the fraction that is dc tends to a limit R , where R is defined as surplus dc expressed in units of

UNITS OF SURPLUS ELECTRICAL ENERGY
VS $\hat{Q}$. TANDEM MIRROR REACTOR.

$$P_{\text{SURPLUS}} = [P_{\text{DC}} - (P_{\text{AUX}} + P_{\text{CIRC}})]$$

$$P_{\text{INJ}} = 1.0$$

Fig. 7.18

equivalent thermal energy divided by the total energy available to the process chemistry in thermal units.

$$R = (1/\eta_{TH}) P_{NET} / [P_T + (1/\eta_{TH}) P_{NET}]$$

$$P_{NET} = P_{DC} - (P_{AUX} + P_{CIRC})$$

$$P_T = AM + B(1 - \eta_{dc})$$

This limit is indicated in Figure 7.19 where we see that as Q gets larger and larger, the dc percentage of the reactor's output available for process chemistry tends to a limit of about 13%.

7.9.2 Comments on the Tandem as an Energy Source

From Figures 7.18 and 7.19 the following conclusions can be reached.

- 1) At $Q \sim 14$ the TMR dc electrical output is just large enough to feedback and drive the reactor leaving the thermal fraction of the direct converter and the blanket thermal energy available for process chemistry.
- 2) As Q values exceed 14 there is some surplus dc electrical power available for process chemistry.
- 3) As Q increases the thermal equivalent of the direct current electrical energy that is available for process chemistry tends to a limit of about 13% of the plant useful thermal output, short of the 62% ($25\% \div 0.4$) needed for G.A.'s electrical power or the 52% for Ispra's.
- 4) The availability of this direct current electrical energy out of the TMR is an asset that other energy producing machines

Table 7.3
ENERGY BALANCE DATA AS A FUNCTION OF Q

Output			Input			Avail. to Process Chemistry			
1	2	3	4	5	6	7	8	9	10
Q	THERMAL ENERGY FROM BLANKET = AN	THERMAL ENERGY FROM DIRECT COIN = $B(1 - \eta_{dc})$	ELEC ENERGY FROM DIRECT CONVERTER = $P_{DC} = B(\eta_{dc})$	CIRCULATING POWER = P_{inj}/η_{inf}	AUXILIARY POWER = $P_{aux} = f(B+AN+C)$	SURPLUS $dc = (9 - (5) - (6))$ $P_{SURP} =$	THERMAL ENERGY = $P_T = (2) + (3)$	THERMAL EQUIVALENT OF 7 (ASSUMES $\eta_{th} = 40\%$)	$R \times 100$ $R = (9)/(10) + (9)$
5	4.56	0.86	1.05	1.67	.13	-0.75	5.42	-1.875	-52.8
10	9.12	1.28	1.57	1.67	.24	-0.34	10.40	-0.85	-8.90
20	18.24	2.14	2.61	1.67	.46	0.48	20.38	1.20	5.56
30	27.36	2.99	3.66	1.67	.68	1.31	30.35	3.27	9.74
40	36.48	3.85	4.70	1.67	.90	2.13	40.33	5.32	11.66
50	45.6	4.7	5.75	1.67	1.12	2.96	50.03	7.40	12.20

Fig. 7.19

do not have. The HTGR, the Tokamak, the LWR, the FBR--all of these machines must go through the thermal conversion step to produce this electricity at a penalty that is directly proportional to the thermal efficiency. The TMR avoids this thermal conversion step when values of Q exceed 14.

As discussed in Section 7.7, the primary use of the thermal power generated by the Tandem Mirror Reactor is for the synthesis of hydrogen gas. However, a portion of this power must be distributed to various plant facilities in the form of electricity. In Figures 7.20 and 7.21 we show power flow diagrams for the overall system consisting of the TMR, its blanket and direct converter, the power distribution system and the synfuel plant. In Figure 7.20 the power distribution system is taken to be the single series Na loop of Figure 7.5, and the TMR blanket has the characteristics of the baffled Li_2O blanket of Table 6.5. In 7.21 the power distribution system corresponds to the combined series-parallel system of Figure 7.8 with a potassium-vapor topping turbine whose thermoelectric efficiency is $\eta_T = 0.20$. The ratio of electrical and thermal powers of the synfuel plant (600 MWe/2641 MWt) is the same as the ratio of total work plus electricity and thermal input per g-mole H_2 (113 KJ/452 KJ).

In Fig. 7.20 and 7.21 are shown the fusion, thermal and electric processes corresponding to a synfuel plant output of 1671 MMH_2 (1.13×10^{10} liter/day). For the particular cases shown the gain $Q = P_{\text{Fus}}/P_{\text{in}}$ ($= 11.6$) has been adjusted so that the external recirculating power P_{RC} is zero.

Figure 7.22 shows P_{Fus} and P_{RC} as functions of Q for a fixed synfuel plant output of 1671 MMH_2 . For $Q = 11.6$, $P_{\text{Fus}} = 4600 \text{ MWf}$ and 3850 MWf for the bottoming and topping cases respectively. The topping case corresponds to the TMR parameters listed in Table 4.4.1. The overall thermal efficiency $\eta = P_{\text{H}_2}/P_{\text{Fus}}$ is 0.36 and 0.43 for the two cases. The efficiencies of converting blanket thermal energy to hydrogen chemical energy are 0.39 and 0.47, comparable to the values 0.37 and 0.48 given in Figures 7.5 and 7.8.

In Figure 7.22 the arrows on the right indicate the limiting values of P_{Fus} and P_{RC} as reactor gain Q (or length) becomes very large. For both the topping and bottoming cycles $(1/\eta_{TH}) P_{RC}(\infty)/P_{Fus}(\infty) = 0.27$. This is to be compared with the limit $(1/\eta_T) P_{NET}/[P_T + (1/\eta_T) P_{NET}] = 0.12$ shown in Figure 7.19.

Fig. 7.20 Power flow diagram for the TMR-Synfuel System with bottoming electric power generation (cf. Fig. 7.5). For the case shown $\phi = 11.6$.

Fig. 7.21 Power flow diagram for the TMR-Synfuel System with topping and bottoming electric power generation (cf. Fig. 3.14). For the case shown $Q = 11.6$.

Fig. 7.22 Gross fusion power and external recirculating power as functions of fusion reactor gain Q .

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SECTION 8

THERMOCHEMICAL PROCESS

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8.1. Basic Principles of Thermochemical Cycles

8.1.1 Introduction

A thermochemical cycle for hydrogen production is a process in which water is used as a feedstock along with a non-fossil high temperature heat source to produce H_2 and O_2 as product gases. The water splitting process is accomplished through a closed loop sequence of chemical reaction steps in which the chemical reagents are continuously recycled and reused in the process with essentially no loss of material. Practical thermochemical cycles, as currently envisioned, require input temperatures of ~ 1200 K, for the highest temperature chemical step, and operate at a thermal efficiency of $\sim 45\%$. Here, the thermal efficiency is defined as the higher heating value of the H_2 produced, $286 \text{ kJ/g mol } H_2$ (combustion enthalpy of the H_2 to give liquid water at 298.15 K), divided by the thermal heat delivered by the heat source.

Currently, there are some 30 thermochemical cycles world-wide that are under various stages of investigation and development. Energy sources that are being considered for these cycles fall into three categories: (1) high-temperature, gas-cooled reactions providing a maximum temperature of ~ 1200 K, (2) solar central receivers providing a maximum temperature somewhere in the $1200\text{--}2000$ K range, and (3) magnetic fusion reactors anticipated to provide a maximum temperature in the $1200\text{--}2000$ K range. Selection of an actual maximum operating temperature depends upon the process needs of the thermochemical cycle as well as on overall system design. Cycle efficiency, system economics, and materials needs are all important considerations. Electrical energy for process equipment is required in addition to high temperature heat for operation of thermochemical hydrogen plants.

Of the cycles under study, only three have thus far been developed to the stage where closed-loop, table-top models have been built and tested in the laboratory; these are the cycles that we are considering for magnetic fusion applications. They are illustrated in terms of their principal chemical steps and reaction temperatures in Figure 8.1. The main effort on the development of the Sulfur-Iodine Cycle is underway at the General Atomic Company,⁽¹⁻³⁾ for the Sulfur Cycle at the Westinghouse Electric Corporation,⁽⁴⁻⁶⁾ and for the Sulfur-Bromine Cycle at the Commission of European Communities Joint Research Centre-Ispra Establishment.⁽⁷⁻⁹⁾

Figure 8.1 Thermochemical cycles whose chemistry and closed loop operation have been verified in the laboratory

SULFUR-IODINE CYCLE

SULFUR CYCLE (PART ELECTROCHEMICAL)

SULFUR-BROMINE CYCLE (PART ELECTROCHEMICAL)

8.1.2 Basic Principles

The energetics of water decomposition are as follows:

$$\Delta H_{298}^0 = 285.9 \text{ kJ/mol}$$

$$\Delta G_{298}^0 = 237.2 \text{ kJ/mol}$$

Thus, the energy requirement for decomposing water to the elements is quite high; it can be further shown that if water were to be decomposed in a single step, temperatures of about 2500-3000 K would be required to obtain significant yields. Furthermore, there are a number of major problems associated with decomposing water in a single step, even if such high temperatures could be attained, i.e., back reaction of H_2 and O_2 during cooldown, serious materials problems, and the need for recycle.

If the decomposition of water is carried out in two or more chemical reaction steps, a number of advantages result. For example, the production of H_2 and O_2 can be separated into different reaction steps, thus avoiding the back reaction problem. Also, by picking a proper combination of reactions, the maximum reaction temperature can be lowered with resultant benefits in reaction yields and availability of suitable materials. In actual practice, it is found that two-step cycles show only minimal lowering of the temperature compared to the single step decomposition of water, unless one of the steps involves an electrochemical assist (e.g., see the Sulfur Cycle in Fig. 8.1). A cycle that includes an electrochemical step is called a "hybrid" cycle. With three or more steps, cycles based only on thermal input for the chemical reactions become possible (e.g., see the Sulfur-Iodine Cycle in Fig. 8.1), and many will work efficiently at maximum temperatures of 1200 K or even less.

In order to illustrate some of the important considerations in developing and evaluating thermochemical cycles, we can use a generalized two-step cycle represented as follows:

In reaction (1), a reducing agent R is used to reduce water to produce H_2 at temperature T_1 , forming the oxide RO in the process. Reactions such as this are usually carried out near room temperature or at slightly elevated temperatures. In order to achieve a near zero or negative ΔG^0 to make the reaction go, the enthalpy change is usually governing and H_1 is negative (an exothermic reaction). Heat produced by reaction (1) is usually of little value for reuse in the cycle because of the low temperatures involved. Reaction (2) is carried out at a high temperature, usually in the vicinity of 1200 K, and is the decomposition step that regenerates the reducing agent R and O_2 . Reaction (2) is highly endothermic (ΔH_0 is positive) and requires a large change in entropy (ΔS^0) to produce a near zero or negative ΔG^0 . This can be readily seen from the well-known second law expression:

$\Delta G^0 = \Delta H_0 - T\Delta S^0$. Hence, one of the requirements in selecting RO is that it have as large a ΔS^0 of decomposition as possible, to minimize the maximum temperature required in the thermochemical cycle. As an example, the gaseous molecule SO_3 ($R = SO_2$) meets the requirement for reaction (2), but SO_2 falls short of the requirement for reducing water to produce H_2 in reaction (1). The fix for this problem is to use an assist for reaction (1) by either (a) forming a hydrogen compound of intermediate stability, such as HI or HBr, which can be subsequently decomposed to obtain the desired H_2 product, or (b) using an electrolysis assist at a voltage substantially less than for direct water electrolysis. In

the case of HBr decomposition, an electrolysis assist is also used, since the yield is low for thermal decomposition of HBR.

Two types of cycle efficiencies are used in evaluating thermochemical cycles. The work efficiency, i.e., the efficiency of converting heat into work, $E(w)$, is given by

$$E(w) = \frac{237.2}{\Sigma \Delta H_0} \leq \frac{T_D - 298}{T_D} \quad (3)$$

The thermal efficiency, or the efficiency of heat utilization, $E(H)$, is given by

$$E(H) = \frac{285.9}{\Sigma \Delta H_0} \leq 1.21 \frac{T_D - 298}{T_D} \quad (4)$$

In expression (3), the number 237.2 (in kJ/mol) represents the maximum work (ΔG^0) available from the hydrogen produced, (see initial expression in Section 8.1.2), while $\Sigma \Delta H_0$ represents the total external heat requirements for the cycle, i.e., the heat provided by the non-fossil fuel heat source. For electricity required in the cycle, the $\Sigma \Delta H_0$ term includes the equivalent heat required to produce the electricity. We see that $E(w)$ is limited by a Carnot expression for the maximum efficiency. The thermal efficiency, $E(H)$, compares the combustion energy of the hydrogen produced to form liquid water as the final product, with $\Sigma \Delta H_0$. The thermal efficiency could conceivably exceed the Carnot efficiency by the ratio of $285.9/237.2 = 1.21$.

8.1.3 Some Important Criteria

A listing of the major reaction steps (such as in Fig. 8.1) gives only an indication of the complexity of a thermochemical cycle system. Choosing the best cycle and optimizing it in terms of efficiency and economics are challenging research goals. The criteria that must be considered include:

- Favorable thermodynamics.
- Fast reactions.
- High reaction yields.
- Simple separations.
- Process design factors, such as efficient use of heat, avoidance of scarce or hazardous reagents, and simplicity of construction.

Favorable thermodynamics implies both a negative and a very small free-energy change for each reaction step. If the free-energy change were positive, the equilibrium constant of the reaction would be unfavorable, and much energy would be wasted in separating or concentrating the reaction products. If the free energy change were too negative, on the other hand, one or more of the reaction products would be too stable and hard to regenerate into the original reagents.

Fast reactions are desirable, especially for reactions involving gaseous reagents, because they enable us to use smaller and less expensive reaction vessels. The requirement that the free-energy change be nearly zero makes it harder than usual to achieve fast reactions, however, because it allows very little driving force for the reactions. Reactions in which the energy barriers are low enough permit a very small free-energy change to drive them rapidly.

High reaction yields are important to minimize recycling of unreacted reagents or diversion of material into undesirable by-products.

By using simple separations, we can avoid another area in which hidden costs can be substantial. Operations such as boiling, compression, and filtering can involve expensive process equipment and energy losses, and they need to be minimized.

Finally, careful process design can do much to provide the optimum tradeoff between efficient heat-energy use and economy of construction and operation. Its contributions include new and innovative process equipment, plant layout, and siting.

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8.2 General Atomic Cycle

8.2.1 Chemical Description

The Sulfur-Iodine Cycle being developed by the General Atomic Company^(1,2,3) is a pure thermochemical cycle operating in an all liquid-gaseous environment and is described by the following major reaction steps:

All reactions in this system have been verified in the laboratory and total recycle has been illustrated in a small closed-loop cycle experiment. Major parts of the process are associated with separation and purification of the reaction products. A critical aspect for the successful operation of the process is the separation of the aqueous reaction products in reaction (1) above. Workers at the General Atomic Company have solved this problem by using an excess of I_2 in reaction (1), which leads to separation of the products into a lower density phase, containing H_2SO_4 and H_2O , and a higher density phase, containing HI , I_2 and H_2O .

Reaction (2) shows the catalytic decomposition of HI , which is in the purified liquid form (50 atm). Laboratory decompositions are around 30% per pass and, therefore, use a recycle step. The unreacted HI is condensed out of the H_2 and I_2 products. Pure H_2 is obtained by scrubbing out I_2 with H_2O .

The equilibrium for reaction (3) lies to the right at temperatures above 100 K, but catalysts are needed to attain sufficiently rapid decomposition rates. Catalysts are available for this process, but careful consideration needs to be given to cost versus effectiveness. The SO_3 decomposition reactor is a challenging unit to design, owing to the high temperatures and corrosive products involved. All of the H_2SO_4 based cycles need such an SO_3 decomposer. Each of the three laboratories involved has approached this problem from a different standpoint. General Atomic has not released a design for their SO_3 decomposer.

8.2.2 Process Schematic Flow Diagram

Figure 8.2.1 is a simplified schematic block flow diagram of the Sulfur-Iodine Cycle conceptually showing product flows and the recycle streams. The full process engineering flowsheet expanding the features shown in the conceptual block diagram in Fig. 8.2.1 is available to us from the General Atomic Company. However, for the purposes of our initial scoping study, this detail is not required; we have included in the Supplement some General Atomic^(1,2,3) schematic and abbreviated flowsheets as follows:

- Flowsheet Section I, H_2SO_4 -HI Production and Separation.
- Flowsheet Section II, H_2SO_4 Concentration and SO_3 Decomposition.
- Flowsheet Section III, HI Separation.
- Flowsheet Section IV, HI Decomposition and H_2 Purification.
- Flowsheet Section V: Energy Distribution and HTGR Coupling.

Fig. 8.2.1. Block Diagram for General Atomic Cycle (1,2,3)

8.2.3 Energy Balance

The dominant (i.e., 74%) thermal energy requirement for this process is the H_2SO_4 concentration, boiling, and decomposition steps. The heat vs. temperature requirements for this H_2SO_4 concentration and decomposition are shown in Fig. 8.2.2. The abscissa is scaled in units of kJ/mole H_2 . Of the 452 kJ/mole H_2 requirement, about one-third is for H_2SO_4 concentration, one-third H_2SO_4 boiling, and one-third H_2SO_4 vapor decomposition. If we were to do our own process flowsheet development optimizer for the TMR, we would want to approximate this curve as closely as possible with as large a number of heat exchangers, economizers, and heat recovery units as necessary to get a high process efficiency, yet limiting equipment to be consistent with good economics.

General Atomic drives their process with helium from a High Temperature Gas Reactor (HTGR) through many turbines and heat exchangers, and the details of their approach will not apply directly to our TMR-driven system. The thermal history of the primary helium from the HTGR and secondary helium driving the process is shown for the three, first-stage heat exchangers in Fig. 8.2.3. For more detailed energy interfacing, see Section 7. In order to do a more detailed job in developing the interface heat exchanger design for the TMR/thermochemical process, a redesign and reoptimization of the G.A.-Sulfur Cycle would be required. This is beyond the scope of our present project, but is planned for the future.

Fig. 8.2.2. Heat Demand Curve for H_2SO_4 (1,2,3)

Fig. 8.2.3. Distribution of G.A. Process Heat (1,2,3)

8.3 Ispra Cycle

8.3.1 Chemical Description

The process that we have considered for this study is not Ispra's latest one, which is being evolved around their new concept for a hot-air driven direct contact H_2SO_4 decomposition reactor, but their previous version, Mark 13-V2. This process has sufficient documentation and detailed calculations for energy and material balances.

This cycle is a hybrid cycle (contains an electrolysis step), and is described^(4,5) by the following three reactions:

The block diagram is shown in Fig. 8.3.1.

At present, a significant research effort is being directed toward the working conditions of the electrolytic cell. In the latest evaluation, the cell voltage is 0.8 V, the temperature is 403 K, and the pressure is 30 atm.

The azeotrope of hydrobromic acid, containing the bromine liberated at the anode, leaves the cell and goes to a distillation column where bromine, separated from the acid solution, goes to a Fischer Reactor. The azeotrope then returns to the cell. Sulfur dioxide and water, coming from various stages of the oxygen separation zone, also go to the Fischer reactor, which is envisaged as a counter-current liquid-gas contactor. Vapors of hydrobromic acid produced therein go to the electrolytic cell; while sulfuric acid, having a concentration of 75-85%, leaves as liquid effluent.

Fig. 8.3.1. IspraMark 13-V2 Cycle Flow Schematic

In order to increase the cycle efficiency and reduce the load of the evaporator, the concentration of the acid leaving the reactor should be as high as possible. Following the Fischer Reactor, the sulfuric acid is concentrated in an adiabatic tower, then evaporated to form water and sulfur trioxide; the latter is then partially decomposed following the procedure described for Mark 11.

8.3.2 Process Schematic Flow Diagram

A simplified flow schematic is shown in Fig. 8.3.1, with the more detailed flow diagram given in the supplement. For this discussion the process has been reduced to the following principal sections:

1. The Acids Formation Section
2. The H_2SO_4 Concentrator Section
3. The H_2SO_4 Decomposition Section
4. The SO_2/O_2 Separation Section
5. The Bromine Distillation/HBr Absorption Section
6. HBr Electrolysis Section

The electrolysis of hydrobromic acid is carried out at moderately high pressures and temperatures, thus leading to a decrease in the cell overvoltages due to the reduction in the bubble size and an increase in electrolyte activity. Furthermore, higher pressures reduce the amount of hydrogen bromide leaving with the wet hydrogen gas; by operating at sufficiently high temperatures, waste heat may be re-used in the process. Although the material problems may be avoided by working at lower temperatures, the heat then available is only of low quality.

The $\text{SO}_2/\text{Br}_2/\text{H}_2\text{O}$ reactor is also a heat source, so its operating temperature should be as high as possible; while the

bromine stripping tower, requiring heat, should be run at a lower temperature. Ispra has found that varying the $\text{SO}_2/\text{Br}_2/\text{H}_2\text{O}$ reactor conditions from 10 to 15 atm and 423 to 438 K had little effect on the process, but changes in the distillation tower operating conditions has a marked influence upon process efficiency. If the tower temperature is made lower than that in the electrolyzer so waste heat from the electrolysis can be used in the reboiler, higher process efficiencies are possible.

The concentration of the hydrobromic acid leaving the distillation column is also important. The closer it is to the azeotrope, the higher is the HBr partial pressure. Therefore, a value closer to 40 w/o was chosen by Ispra.

The SO_3 Decomposition Reactor has been the subject of much analysis and discussion at Ispra, and they believe that the lowest hydrogen cost has been found by operating at an SO_3 pressure of ~ 12.5 atm.

Once having established the general plant operating conditions, it is then necessary to vary the heat exchange network slightly. Normally, if a stream's temperature is different from that of the apparatus it enters, it can be cooled or heated as needed, or it can enter with no such preconditioning. In the case of streams having scrubbers, these are all initially at 30 K and go to the $\text{SO}_2/\text{Br}_2/\text{H}_2\text{O}$ reactor, which operates at 165 K. Streams can either be heated before letting them enter the reactor, or the excess reactor heat can be used to bring their temperature up to the correct level. Ispra's analysis indicated that the first method is better, in that the process efficiency increases; however, the capital cost of heat exchangers also increases. Overall, there is no change in hydrogen production cost; so one must decide upon the practicality of using a system with many heat exchangers.

An important point in the heat exchange network is the manner by which the relatively weak sulfuric acid fed to the acid concentrator is heated. Again it could be heated in the tower itself or pre-conditioned. By heating this flow in the apparatus itself, a substantial quantity of high quality heat is used; this is not the case when this stream is preconditioned. In the latter case, lower quality heat may be used, thus leaving higher temperature heat sources for electricity production or more appropriate process requirements.

8.3.3 Energy Balance

The Ispra process energy flow analysis is given in the Supplement.

The Ispra process in its present form is not highly integrated or internally heat coupled. The thermochemical efficiency is reported to be 45.7%.

8.4 Westinghouse Cycle

8.4.1 Chemical Description

The Westinghouse cycle is also a hybrid cycle, and has been described by Westinghouse^(6,7) as a process driven by their very high temperature reactor (VHTR). The process, in its most general form, consists of two chemical reactions--one for producing hydrogen and the other for producing oxygen. The production of oxygen occurs via the thermal reduction of sulfur trioxide obtained from sulfuric acid.

The overall reaction (1), occurring electrochemically, is comprised of the individual reactions:

The process is completed by using the sulfur dioxide from the thermal reduction step to depolarize the anode of a water electrolyzer.

Reaction (2) is identical to that in the other cycles and was discussed in Section 8.2. The net result of reactions (1) and (2) is the decomposition of water into hydrogen and oxygen.

Although electrical power is required in the electrolyzer, much smaller quantities than those necessary in conventional electrolysis are needed. The theoretical voltage to decompose water is 1.23 V, with many commercial electrolyzers requiring over 2.0 V. The voltage requirement for reaction (1) is about 0.7 V in the current stage of development, and the power requirements are thus only about one-third of that required in conventional electrolysis.

8.4.2 Process Schematic Flow Diagram

The process is shown schematically in Fig. 8.4.1. Hydrogen is generated electrolytically in an electrolysis cell, which anodically oxidizes sulfurous acid to sulfuric acid while simultaneously generating hydrogen at the cathode. Sulfuric acid formed in the electrolyzer is then vaporized, using thermal energy from a high temperature heat source. The vaporized sulfuric acid (sulfur trioxide-steam mixture) flows to an indirectly heated reduction reactor where sulfur dioxide and oxygen are formed. Wet sulfur dioxide and oxygen flow to a separation system, where oxygen is produced as a process co-product and the sulfur dioxide is recycled to the electrolyzer.

Fig. 8.4.1. Westinghouse Cycle Schematic Diagram^(6,7)

8.4.3 Energy Balance

The key thermal and electrical requirements are given in Table 8.4.1. The electrolyzer cell voltage has been indicated as a parameter, although Westinghouse has only recently demonstrated 0.7 V at 1 atm. With a commercial electrolyzer design based on 20 atm, SO_2 pressure, we feel that the reaction (1) driving force becomes sufficiently more favorable and that a cell voltage level of 0.6 V should be reasonable for our cycle-comparison design study.

The electrical requirements of the process are large, as shown in Table 8.4.1, and are for the most part (i.e., 90%) for driving the electrolyzer. We do not find any statements about thermodynamic efficiency at which their electrical power would be generated. Since the VHTR is producing both thermal energy to drive the thermochemical process and electric power, we cannot check whether their thermodynamic efficiencies are around the 39% to be expected for their steam generators (SG-2). The fractions don't total 100% in Table 8.4.1, owing to an internal generator and poor heat balance closure.

If their thermodynamic efficiencies are reasonable and we choose 0.6 V as an attainable cell voltage, then our Tandem Mirror Reactor blanket should produce 45% of the heat at 1283 K and 53% at 1108 K. The thermal output totals 98%, and the missing 2% results in errors in the mass balance closure.

TABLE 8.4.1

PRINCIPAL THERMAL AND ELECTRICAL REQUIREMENTS. BASIS 10.76 MSCMD*

		Goal for Cell Voltage		
		400 mv	500 mv	800 mv
Hi Temp Units	% Therm. Output	51	45	39
	Temp., K	1283	1283	1283
	Press., atm	67	67	67
Med. Temp Units	% Therm. Output	47	53	46
	Temp., K	1108	1108	1108
	Press., atm	45	45	45
Elect. Process Power Req'd GW_e		0.48	0.69	0.90
VHTR Rating GW_t		2.8	3.2	3.7
H_2 Thermal Efficiency %		53.1	46.8	40.5

* MSCMD - Million Standard Cubic Meters of Hydrogen Gas Produced per Day

8.5 Comparisons of Cycles

8.5.1 Intercomparison of the Three Candidate Cycles

8.5.1.1 Electrical vs. Thermal Requirements

Intercomparing the three candidate cycles relative to the electrical and thermal requirements on a comparable basis is difficult because each of the cycle designs uses different processes and bases for the scale of the operation. We have selected a method of breaking down the percentage energy requirements for each cycle at the upper and lower process temperatures and for electrical process requirements (or possibly shaft work in G.A.'s case) as shown in Table 8.5.

It can be seen that, on the basis of either the thermal efficiency for hydrogen production or the thermodynamic efficiency for electricity production, the three candidate processes appear very comparable indeed. However, on the basis of high temperature heat demand and electrical demand, they are very different. Since the G.A. Cycle is strictly thermochemical and does not use any electrolytic steps, it will have the least electrical demand. The Westinghouse and Ispra cycles are both "hybrid" cycles, since they involve an electrolytic step; however, of the two, the Ispra cycle is considerably less demanding in the form of high temperature process heat requirements and electricity.

8.5.1.2 Differing Technology Requirements

There are two technology requirements for the thermochemical cycles that appears to be critical to the ultimate viability of a fusion reactor-driver H_2 plant: (1) thermal demands are set by both the high temperature limits of the process and the distribution of heat as a function of temperature. As discussed

TABLE 8.5

ELECTRICAL AND THERMAL REQUIREMENTS FOR
VARIOUS CYCLES BASED ON HTGR HEAT SOURCES

	H ₂ Scale of Plant	Thermal* Efficiency	HTGR Output Heat		Thermal Energy Used to Generate Electri- city or Shaft Work	
			Higher Temp	Lower Temp	Elec- trolytic Demand	Process Shaft Work
General Atomic Sulfur-Iodine Cycle	---	47%	24% at 1250 K	51% at 843 K	0	25% [†]
Westinghouse Sulfur Cycle	448,000 std. m ³ /h	47%	23% at 1280 K	20% at 1108 K	57%	0
Ispra Mark-13	100,000 std. m ³ /h	46%	27% at 1083 K	52% below 773 K	21%	0

* Defined as the higher heating value of H₂, 285.9 kJ/mole, divided by the net external thermal energy demand on the reactor

[†] This shaft work was produced from the upper temperature portions of the heat exchangers designated as E501 and E502 in Fig. 8.2.3

earlier (in Section 8 and summarized in Table 8.5), each of the three candidate cycles use about 25% of the heat at the highest temperature. The Ispra process highest temperature needs are at 1083 K, while the G.A. process is at 1250 K, and the Westinghouse process is at approximately 1280 K. Ispra has spent a substantial effort in reducing the high temperature requirements, so that their Mark 13-V2 process can be driven by a more conventional HTGR. Westinghouse has shaped their process around their VHTR (very high temperature reactor), which they believe is a unique advantage to Westinghouse. G.A. has similarly aimed their Sulfur-Iodine Cycle toward their helium cooled HTGR, which employs a number of G.A. proprietary contributions.

Both the G.A. and Westinghouse heat sources are at the limit of high temperature, advanced alloys. We feel, that for our present task of using a fusion driver instead of HTGR, the technology requirements for the thermochemical process should not be extreme, since the fusion driver itself is pushing the limits of our present technology.

In the G.A. cycle, the high temperature derives from the use of a secondary helium loop designed to provide radiation safety isolation between the HTGR and the thermochemical process. In a TMR-driven system, we believe fusion's reduced radiation hazard potential may allow us to eliminate the secondary helium loop.

The Reactor Heat Exchanger Types

In the overall energy balance (the combined reactor and the chemical plant) applicable to the General Atomic sulphur-iodine thermochemical process, there will be both appropriate energy distributions and temperature range possibilities that will allow matching the reactor thermal output to the thermochemical plant input. In the reactor, we are able to provide three energy/temperature regimes from three reactor regions, basically three heat exchanger energy sources.

1) A high temperature section, ~1200 K:

The source of this energy is either the Condensing Vapor Heat Exchanger unit (CVHX) used with the LiNa Cauldron design or the high temperature heat exchanger of the flowing Li_2O microsphere design.

2) An intermediate temperature section, ~950 K:

The energy source is the direct convertor thermal energy. There is a constraint on this heat exchanger design. The delivered energy is available only in the form of flowing high temperature gas or water vapor and not as a liquid metal because the coolant must flow through high voltage collectors held at dc potential; therefore, the coolant cannot be an electrical conductor. This energy source forms the Direct Convertor Heat Exchanger (DCHX).

3) A low temperature section, ~675-750 K:

In either the Li-Na Cauldron blanket concept or the flowing Li_2O microsphere concept, it will be necessary to have an integrally cooled structural container and first wall. This cooled first wall heat exchanger is the source of the low temperature energy.

8.5.2 The Blanket/Thermochemical Cycle Interface

8.5.2.1 The Li-Na Cauldron

The details of the Li-Na Cauldron blanket module were covered in Section 5. It is the purpose of this section to develop methods of transporting the heat from the blanket to the thermochemical cycle.

This Cauldron blanket module offers some unique chemical engineering advantages to the coupling with the thermochemical cycle, which can help us achieve a simple and reliable design. First, the module completely encapsulates the liquid metal Li-Na mixture and avoids the problem of pumping large quantities of liquid metals into and out of the fusion blanket areas through neutron fluxes or magnetic fields. This means that we can obtain one safety-isolation barrier at the condensing vapor heat exchanger installed in the vapor dome, as shown in Fig. 8.5.2.1, or with heat pipes (shown in the Supplement). The module itself, as discussed in Section 5 and by R. Werner⁽⁸⁾, offers a number of features which may lead to high reliability and safety: a low pressure (~ 1 -5 atm), a relatively cool (700 K) first wall, a modular design for easy maintenance and high total plant reliability, a low activity titanium-vanadium structure to reduce "hot" maintenance handling problems and possible release of activation products, and a simple structure that can be manufactured using today's technology.

These unique design features of the Cauldron blanket can be exploited in developing interface design concepts for driving the thermochemical process, such as the low pressure coolant that can feed directly the SO_2 decomposer reactor operating around 1050 to 1080 K. We have discussed the temperature requirements of the G.A. cycle in Section 8.2. We now need to discuss the implications for the various chemical engineering design concepts for the TMR/Cycle linkage.

The interface configuration shown in Fig. 8.5.2.1 as Concept #1, offers a fairly close match between thermal energy output of the TMR vapor dome, the first wall, and the direct converter with the H_2SO_4 concentrator, H_2SO_4 vaporizer, and the SO_2 decomposer. The lower temperature first wall heat

exchanger can provide the lower grade heat, around 5% of the total heat requirement, at about 700 K via sodium, helium, or an organic polyphenyl coolant. The direct converter, operating at high voltage, requires a helium gas coolant to remove a residual 5% heat load at 900 K. Hence, these two heat exchangers combined can provide 10% of the heat requirements as process steam. Because of these different heat supply characteristics of the TMR, the heat exchangers used by G.A. for the HTGR-driven cycle must be reconfigured. The power distribution is approximate. See Section 7 for a specific case.

In linking the TMR to the G.A. Cycle, the Cauldron condensing vapor heat exchange loop is the critical one. We show in Fig. 8.5.2.1 the possibilities of using either sodium, helium, or potassium. Concept #2 would involve introducing a heat pipe between the Cauldron and this loop; the engineering tradeoffs for this concept are discussed in the Supplement. Concept #3 would involve introducing an additional, secondary helium loop to interface with the SO_3 decomposer to provide additional isolation between liquid metals and the SO_3 , SO_2 , O_2 , H_2O -containing process stream.

Let us now examine the design constraints with the temperature conditions shown in Section 5 for the Cauldron (first wall at 700 K and Li-Na mixture at around 1200 K) and as shown in Concept #1 in Fig. 8.5.2.1, with an all-sodium loop and condensing vapor heat exchanger fabricated from TZM. There are four thermal resistances important to our design work:

1. ΔT across the condensing Na vapor,
2. ΔT in the liquid sodium flowing through the inside of the heat exchanger tubes within the Cauldron,
3. ΔT in the liquid sodium at the SO_3 decomposer, and
4. ΔT within the hot SO_3 , SO_2 , O_2 and H_2O stream within the SO_3 decomposer.

Fig. 8.5.2.1. Concept #1: Coupling the Three Reactor Heat Sources to the Process

The detailed analysis of the two heat transfer resistances 1 and 2 is discussed in Section 5.1.3. Resistances 3 and 4 and the economics of pumping power will be reported in this section.

These calculated temperature loss constraints, consequently, lead to a temperature range for the blanket operation of 1200 and 1250 K for SO_3 decomposer temperatures of 1050 and 1080 K. Our goal for the blanket is around 1200 K. Temperatures higher than 1200 K offer serious materials problems.⁽⁹⁾

The SO_3 decomposer operation is the next item impacting our task of developing an interface; the details are presented in Section 9. We only summarize the important decomposer constraints relative to the interface here. The SO_3 kinetics and equilibrium,⁽¹⁰⁻¹¹⁾ as measured recently by the General Atomic Company, show this critical, high temperature SO_3 decomposition reactor to be surface kinetics controlled (heterogeneous) at temperatures below 1050 K, and to become homogeneous controlled at higher temperatures, about 1180 K. For noncatalytic surfaces (i.e., heat exchanger tubing), the conversion from SO_3 to SO_2 is about 0.3 at 1180 K and 0.2 at 1080 K for a 0.3 to 1 second residence time and at around 1.5 atm total pressure. This 20-30% conversion leads to large (i.e., three to five times) recycle of H_2SO_4 flows and thus much larger and more expensive equipment. Increased residence time, although improving the kinetics, increases the size of the equipment; increased total pressure decreases equipment size, but unfavorably shifts the equilibrium; decreased conversion again increases equipment size. Catalytically-enhanced kinetics⁽¹⁰⁻¹¹⁾ greatly improve the conversion to the area of 65-80% for our conditions, but complicate the SO_3 decomposer far beyond a single Incoloy-800H heat exchanger. This complicated topic and SO_3 decomposer design requirements are presented in Section 9--where we develop the fluidized bed

concept that uses 1516 kg/s sodium in seven beds, with a sodium drop temperature of 100 K; then we contrast this with the helium heat transfer fluid. This above discussion lays out the engineering tradeoff issues arising from the chemical engineering of SO_3 decomposition, so that we can develop the interface in a compatible manner.

We have also shown, in Fig. 8.5.2.1, three regions separating the coupling: the nuclear island where the reactor-heated coolants are handled; the intermediate building where the process fluids or steam are brought in proximity with the heated coolant; and the process region where, at some large distance from the reactor, steam is heat exchanged with process fluids or used to drive pumps or compressors. These different regions were selected for safety isolation reasons. For example, we want to avoid such process streams containing O_2 , H_2O , etc. from leaking into the sodium loop and causing a fire that would engulf the Li-Na-containing modules. We also want to avoid running the sodium loop a long distance out into the thermochemical plant for safety and economic reasons.

The intermediate building is conceived as an isolated heavy concrete structure capable of withstanding sodium fires. It is also separated in order to isolate these reactor coolant-process heat exchangers from any possible accident that might occur within the reactor hall or out in the thermochemical plant. In this configuration, we estimate the distances from the blanket to the SO_3 decomposer to be around 100 m.

Calvin Brooks⁽¹²⁾ has calculated the pressure drops for sodium flowing in 100-m long heat transport pipes, varying in diameter from 0.25 m to 2.0 m; the best pipe size appears to be 0.75 m with an associated ΔP of around 0.1 atm. Heat losses

of below 1 K were typical with standard 5-cm thick, high temperature insulation. So sodium interfacing is technically straight-forward, but offers some safety concerns, as discussed above.⁽¹³⁾ Potassium appears to be nearly identical to sodium.

Helium, on the other hand, is technically quite challenging,⁽¹³⁾ but offers some very strong safety advantages. Pumping the 100 m or more out into the plant can be very costly and is sensitively dependent on pipeline velocity and pipe diameter. Working with Calvin Brooks,⁽¹²⁾ we have made an attempt to identify the near optimum pipeline configuration to supply the heat to the decomposer at a minimum cost.

Starting with a parametric study of 1100 K helium flowing in one to seven pipes from 0.75 to 2.0 m in diameter, at pressures from 10 to 60 atm, we first constrained the pipeline velocity under Mach number 0.25. We then obtained approximate capital costs for these pipes, constructed of Incoloy-800H, and pumping power operating costs with unit electric marginal costs of from 0.75 to 3.0¢/kWh. Standard economic practices were used based on a 20-year plant life, 50% utility equity, 12% bond interest rate, and constant July 1980 dollars.

The results are shown in Figs. 8.5.2.2 and 8.5.2.3, where total cost in present value dollars is shown versus pipeline diameter and pressure for four- and seven-unit delivery pipe systems (eight and fourteen round trip), coupling the blanket and the SO₃ decomposer 100 m apart. Optimal pipeline diameters and pressures are listed in Table 8.5.2.1. Since the SO₃ decomposer appears to be the cost critical process unit, we have assumed that helium-driven steam generators can be similarly interfaced at 100 m from the blanket and do not change our conclusions below.

Fig. 8.5.2.3. Optimal Helium Pipe Conditions for a 7 Pipe System

TABLE 8.5.2.1

OPTIMAL HE PIPELINE DIAMETER AND PRESSURES

8-40	Pipes	Power Cost 75¢/kWh			1.5¢/kWh			3.0¢/kWh		
		Cost M\$	Diam, m	Press, atm	Cost M\$	Diam, m	Press, atm	Cost M\$	Diam, m	Press, atm
	4	46	2.0	20	58	2.0	30	66	2.0	35
	7	38	2.0	10	49	2.0	13	54	2.0	15

It appears that the best choice, simply from the pipeline standpoint, is the seven 2.0-m pipe system at 10 atm. If we add optimal criteria for the SO_2 decomposer into this, by seeking a combined minimum, the result is nearly identical, since the cost of a large number of heat exchanger tubes in the decomposer is minimized as the helium pressure approaches a 5-7 atm process pressure. The constraints from the condensing vapor heat exchanger (see Section 5) are to shift the optimum pipe sizes to around 1.5 m and pressure to values of 30 atm or above. Since the curves in Fig. 8.5.2.2 are relatively flat for pressures about the pipeline optimum, such a shift to slightly higher pressures of around 30 atm does not add significantly to the helium pipeline costs.

8.5.2.2 The Li_2O Flowing Microsphere Design

Although the flowing Li_2O microsphere blanket design presented in Section 6 appears very different from the Li-Na Cauldron, the heat transport conditions are similar to the liquid sodium case discussed previously. In this blanket concept, the sodium is heated by the hot Li_2O microspheres. ΔT values of around 50 to 100 K appear typical; therefore, the heat-flow characteristics are similar to the previous case.

8.6

Process Chemistry Materials

The critical materials problems associated with interfacing the Cauldron blanket design with the General Atomic Sulfur-Iodine Cycle involve the following design elements:

- Transport piping
- Heat exchanger - SO_3 decomposer
- Heat exchanger - H_2SO_4 boiler

The transport piping deals with transport of heat from the Cauldron to the thermochemical process--i.e., mainly to the SO_3 decomposer and the H_2SO_4 boiler sections. The heat exchangers for the SO_3 decomposer and H_2SO_4 present the most serious design and materials problems in the thermochemical plant, and so we have chosen to emphasize them in our initial study here. Although the materials problems are discussed in terms of the General Atomic Cycle, the problems are common to the Ispra and Westinghouse Cycles as well, because of the commonality of the H_2SO_4 handling steps. Each of the design elements indicated above will be described in turn in the sections below.

8.6.1 Transport Piping

The main materials considerations for the transport piping are corrosion resistance, good creep strength, low cost, and ease of fabrication. We envision the transport piping to be surrounded by a porous insulating blanket and a jacket, with a sweep gas passing through the insulating layer to remove any residual tritium. The heat transport fluid in the main pipe would be either liquid Na, liquid K, or high pressure He at temperatures of about 1100-1150 K.

Corrosion considerations are similar for using liquid Na or K as the heat transport fluid; taking into account the materials

considerations above, we find that In-800 (or In-800H) provides the best piping material. As mentioned earlier in Section 5.6.4, corrosion rates for In-800 in liquid Na are very low at 1100 K; we can expect similar behavior in liquid K.

With high pressure helium, corrosion is no longer a problem; from a consideration of creep strength, cost, and fabrication issues, In-617, In-625, and In-800 (or In-800H) appear to provide the best alternatives. Stress rupture and creep properties for In-625 and In-800 are comparable.⁽¹⁴⁾ Although creep strength data are not available for In-617, it shows 1000-hour stress rupture strengths that are three times higher⁽¹⁴⁾ than for the other two alloys. Thus, the data for In-625 and In-800 suggest a creep strength for 1% creep in 100,000 hours of $2000 \pm 50\%$ psi (1.4×10^7 Pa) at 1100 K and $1200 \pm 30\%$ psi (8.2×10^6 Pa) at 1150 K. We estimate the corresponding values for In-617 to be $6000 \pm 50\%$ psi (4.1×10^7 Pa) at 1100 K and $3600 \pm 30\%$ psi (2.4×10^7 Pa) at 1150 K based on the argument that the creep strengths scale as a fraction of the long term creep rupture data similar to In-625 and In-800. Materials problems and creep strength at these temperatures remain of critical importance.

8.6.2 Heat Exchanger - SO₂ Decomposer

The highest temperature heat requirement in the General Atomic Sulfur-Iodine Cycle is for the SO₂ decomposer, which operates at a temperature of ~1100 K. Process heat would be delivered either by liquid sodium, liquid potassium, or high pressure helium to heat exchanger tubes ~2 cm in diameter and 2-3 mm wall thickness. Materials considerations on the heat delivery side of the heat exchanger are similar to those for the transport piping; while on the chemical process side, corrosion by the decomposing SO₂ gas is of major concern.

Corrosion tests on a number of heat exchanger alloys for the H_2SO_4 vapor decomposer have been carried out at Ispra and at the General Atomic Company at temperatures in the 773-1173 K range. Their latest results⁽¹⁵⁾ indicate that as bare uncoated alloys, In-800H and In-625 provide the best materials with anticipated lifetimes of a few years. A substantial gain in lifetime can be achieved by using an aluminide-coated In-800H on the surface exposed to the decomposing SO_3 gas. This gives an unusually stable coating-substrate combination that stabilizes in coating thickness and weight change after an exposure of 200-300 hours to the gaseous H_2SO_4 products.⁽¹⁹⁾ Corrosion protection is apparently afforded by an Al_2O_3 film, and imperfections in the coating are self-healing. The useful life of aluminide-coated In-800H for this application is believed to be of the order of 20 years.

In view of the experience with aluminide-coated In-800H in the presence of the decomposing SO_3 gas and in view of data by Nevzorov⁽¹⁶⁾ that indicates good corrosion resistance to liquid Na for an alloy similar in composition to In-800H (see also Section 5.6.4), we select In-800H as the material of choice for the heat exchanger tubes. Unfortunately, the allowance in wall thickness due to corrosion by liquid Na or K cannot be established with certainty for a 20-y life. Based on the 300-h test data of Nevzorov, using a linear corrosion rate with time gives 1.6 mm of corrosion in 20 y. For a parabolic corrosion rate with time (a solid-state diffusion controlled process), the corrosion amounts to 7 μm in 20 y. We believe that the diffusion controlled process is probably the one that prevails, i.e., the diffusion of Ni (the most soluble constituent) into the liquid Na stream. Use of a 0.5-mm thick steel clad applied to the heat exchanger walls that are exposed to liquid Na or K provides us with an alternative approach to

avoid corrosive attack. A clad with an Fe-2.25 Cr-1Mo alloy should give the required corrosion resistance as well as avoid hydrogen embrittlement problems due to traces of hydrogen in the liquid alkalis.

Use of helium as the heat transport fluid avoids the corrosion problems on the heat input side of the heat exchanger, but we are still committed to the use of In-800H with an aluminized coating on the SO_3 side, since corrosion protection there remains a requirement.

8.6.3 Heat Exchanger - H_2SO_4 Boiler

The most critical corrosion problem in the General Atomic Sulfur-Iodine Cycle is corrosion of the heat exchanger used to boil a concentrated azeotropic solution of H_2SO_4 . This boiling step is carried out at ~ 673 K and ~ 10 atm pressure, using the azeotropic composition of H_2O - H_2SO_4 that occurs at 97 wt% H_2SO_4 . The high temperature and pressure are required to minimize equipment size and gas pumping power. Under these boiling conditions, the liquid phase H_2SO_4 is highly corrosive to all known metallic heat exchanger materials (except for the Pt group metals and Au, which are too expensive for consideration).

A number of materials have been tested in hot concentrated H_2SO_4 at 633-693 K in recent years at the Lawrence Livermore National Laboratory^(17,18) and at Westinghouse⁽¹⁹⁾ to identify materials that can be used for the H_2SO_4 boiler. The top candidate materials from the standpoint of corrosion resistance and heat exchanger design are the following:

- Siliconized SiC
- " CrSi_2 " coated In-800
- Durichlor-51 (Fe-14% Si-4% Cr)

All of these materials depend upon the development of an SiO_2 scale on the surface to provide protection against corrosion by H_2SO_4 .

SiC currently presents the best prospect as a heat exchanger material. Siliconized SiC (a two-phase composition consisting of a mixture of SiC and Si) is produced at the Norton⁽²⁰⁾ and Carborundum⁽²¹⁾ companies and is especially suited for this type of application. This type of material, which contains about a 10-15 wt% excess of silicon metal, is impervious to gases, has a high thermal conductivity, high strength, good thermal shock resistance, and can be fabricated in complex shapes and bonded together to form heat exchanger assemblies.⁽²⁰⁾ Corrosion testing of SiC for 1121 h and Si for 592 h in 97 wt% H_2SO_4 at 673 K at the Lawrence Livermore National Laboratory showed no evidence of corrosion,⁽¹⁷⁾ thus confirming the corrosion resistance of both SiC and Si for this application.

Specimens of In-800 coated with chromium silicide of nominal composition CrSi_2 were tested for corrosion for 240 h at 673 K in 97 wt% H_2SO_4 at the Lawrence Livermore National Laboratory⁽¹⁸⁾ and found to show a very low rate of corrosion (extrapolates to a weight loss rate of $15 \text{ mg/cm}^2\text{-y}$). The coatings were about $120 \text{ }\mu\text{m}$ thick, uniform in thickness, well-bonded to the substrate, and without evidence of fractures. The coatings were prepared by Dr. Charles M. Packer of Lockheed Missiles and Space Company of Palo Alto, California, using a slurry coat and rapid melt technique. Although the above results are preliminary in nature, they are encouraging in that a conventional heat exchanger material such as In-800 can be protected against corrosion by a coating process.

Durichlor-51 is a commercial high silicon cast iron material that is yet another candidate as a heat exchanger or container material. It is basically an intermetallic compound with a composition of approximately Fe_3Si and, as such, presents the disadvantages common to most intermetallics of poor ductility, low tensile strength, and poor machining and fabrication characteristics. Also, being a cast material, the control of internal porosities and non-uniformities present production problems. Nonetheless, with proper quality control and with good engineering design and practices, these problems can be controlled. From a corrosion standpoint, tests of up to 524 h on Durichlor-51⁽¹⁷⁾ have shown only a moderate amount of attack ($5\text{ }\mu\text{m}$ surface penetration); but very importantly, corrosion appears to proceed very uniformly over the surface of this material. It therefore seems reasonable to extrapolate to a lifetime of the order of 5 y for this application. In contrast to Durichlor-51, another high silicon cast iron, Duriron, shows a rather irregular corrosion behavior with grain pullouts, corrosion pits, and spallation in similar H_2SO_4 corrosion tests at 673 K.⁽¹⁷⁾

In view of the developments thus far, our choice of material for the H_2SO_4 heat exchanger is siliconized SiC.

Technology Issues

A more complete enumeration of the technology issues associated with a TMR-driven thermochemical H_2 plant will be possible only at the completion of the entire scoping study. However, even at this early date in the program, the following technology issues stand out as worthy of mention:

1. Only in the Ispra Mark 13-V2 process, has a rather thorough attempt been made to design and optimize the process for a high thermochemical cycle efficiency and good economics simultaneously.
2. The version of the G.A. process developed as of circa 1978 was configured to maximize the thermochemical cycle efficiency (without considering capital cost) and, as a result, is highly integrated and more complex than its competitive Ispra and Westinghouse processes.
3. Significant process chemistry advances at G.A. that have been made recently need to be incorporated into their process, a new flowsheet developed, mass and energy balances made, and an optimized process design completed in future work.
4. Coupling a TMR to the SO_3 decomposer and steam generators via a liquid sodium loop is a complex issue due to corrosion and safety issues.
5. Helium coolant, linking the TMR to the H_2 plant, provides excellent safety isolation and freedom from liquid metal fires. However, it raises the operating pressures (i.e., ~35 atm) on the tube-side of the condensing vapor-heat exchangers in the blanket and on the tube-side of the SO_3 decomposer. Both of these heat exchangers have

inherently low shell side pressures (1.5 to 8 atm). This deviation from near pressure-balanced conditions (possible in sodium coolants) significantly increases stresses and creep at high temperatures and forces one to advanced alloys and thicker tubewalls.

6. Although they add to the cost, heat-pipes offer attractive advantages as additions to the condensing vapor heat exchanger, in that they can provide a tritium pumping feature, excellent safety isolation, deamplification of the heat flux for the helium-side, direct coupling to the SO_3 decomposer⁽¹³⁾ etc., with 1.5 to 2 atm sodium or potassium filled heat pipes feeding 35 atm helium coolants. Problems of compressive buckling must be considered.
7. The SO_3 decomposer is a technologically difficult and cost-critical process unit. Capital and operating cost considerations force one to go to higher temperatures and lower pressures. At lower temperatures and lower conversions, large increases in H_2SO_4 recycle increase the size and capital costs of the H_2SO_4 multi-effect evaporator train and boiler.

Conclusions

1. All thermochemical cycles have high temperature limits set by alloy strength and corrosion limitations.
2. The G.A. Cycle lends itself to what may be termed conventional process design, since it uses chemical engineering unit operations of mixing, settling, heating, cooling, boiling, scrubbing, etc. and it does not involve any electrolytic steps.
3. The G.A. Cycle schematic that we examined is of circa 1978 vintage. A potential exists for improvement through the addition of recent General Atomic process simplifications that reduce both capital cost and energy demand.
4. In coupling the TMR to the G.A. Cycle, a reduction in capital cost may be possible by the elimination of a secondary isolation loop, because of the reduced radiation levels in the present design as compared with HTGR sources.
5. The SO_3 decomposition chemical reactor dominates the high temperature heat load requirements in interfacing with the TMR.
6. The use of helium as a heat transport fluid has the following advantages over sodium:
 - a. Low corrosion
 - b. Better safety

Although we have only initially examined helium pumping power and piping costs, solutions appear to be possible. Further work in this area is indicated.

7. There is an adequate selection of materials available for the transport piping and heat exchangers. Our preliminary choices are as follows:

- a. Transport piping: Incoloy 800 (or 800H)
- b. SO_3 decomposer heat exchanger:
Incoloy 800H aluminized on the SO_3 side
- c. H_2SO_4 boiler: siliconized SiC

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SECTION 9
CHEMICAL REACTORS AND OTHER KEY
PROCESS UNIT DESIGNS

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9.0 CHEMICAL REACTORS AND OTHER KEY PROCESS UNIT DESIGNS

9.1 Motivation

The General Atomic Company has not gone beyond the flowsheet stage toward a process design of any individual process units. Since process chemistry improvements are still being incorporated into our prescoping study this year, we could only examine cost-critical items. We selected the SO_3 decomposer as one of the most important items, since it dominates the highest temperature blanket heat requirements and process interfacing details.

9.2 Fluidized Bed SO_3 Decomposer

This section describes a summary of our first cut at a point design for the G.A./TMR SO_3 Decomposer Chemical Reactor, covering kinetics, mechanical design, heat transfer, and costs.

The flow concept for coupling the TMR to the G.A. Cycle involves either liquid sodium or helium from the pool boiler blanket vapor dome condensing vapor heat exchanger to the SO_3 decomposer heat exchanger, discussed earlier⁽¹⁾ as Concept #1, Fig. 9.1. We have picked ~5 atm and 1050 K for the SO_3 decomposer chemistry with the aim of alleviating problems with catalysts and reducing materials' problems in the decomposer and in the TMR blanket.

The background chemistry in the SO_3 decomposition reaction includes work at Westinghouse⁽²⁾, the Nuclear Research Centre in Julich, Germany⁽³⁾, General Atomic in San Diego^(4,5,6), and the Joint Research Centre at Ispra, Italy⁽⁷⁾. The theoretical effect of temperature and pressure on the SO_3/SO_2 equilibrium in the presence of water vapor⁽⁶⁾ is shown in Fig. 9.2. At around 5.2 atm (reactor exit total

Fig. 9.1. Concept #1 Interface Coupling the G.A. Cycle to the TMR

Fig. 2. Simplified Flowsheet for Section II (Courtesy G.A.)

pressure) and 1050 K, the fraction SO_3 converted to SO_2 is around 55%. This compares to G.A.'s 74% conversion at 1144 K.

The key premise as the basis for our concept lies in the small increase in the vapor load on the multi-effect evaporators, due to the near three-fold increase in H_2SO_4 recycle. In Fig. 9.3 is shown the section of the flowsheet around the decomposer, E-214; the material balance is given in Table 9.1, contrasting the original G.A. design with our TMR modification. To avoid a complete modification of all the G.A. flowsheets, we have required that the flowsheet and mass balance outside of the recycle stream 235 exactly match the original G.A. version, i.e., SO_2 flow = 0.9990 gmol SO_2 /gmol H_2 , etc., in stream 232. Under our conditions of lower SO_3 conversion rate, we conducted standard chemical stoichiometric mass balances⁽⁷⁾ based on H_2SO_4 decomposition stoichiometry and SO_2 - H_2O - SO_3 equilibria⁽⁸⁾. We found within the recycle there are substantial changes in flows; for example, the recycle (stream 235) increases in H_2SO_4 flow from 0.3620 to 0.8210 gmol H_2SO_4 /gmol H_2 , nearly threefold, and in stream 231 SO_3 increases from 1.164 to 1.561 gmol SO_3 /gmol H_2 . However, the water load on the multi-effect evaporators remains constant at 4.86 gmol H_2O /gmol H_2 , and the H_2SO_4 increases 34% from 1.36 to 1.82 gmol H_2O /gmol H_2 . Thus, the volumetric vapor load only increases 7.4%, which has a very small impact on the size of the expensive multi-effect evaporator train.

9.3

Chemical Kinetics

Now that we have established the flowsheet and material balance, we need to turn to the chemical kinetics of the SO_3 decomposition. In comparing the current experimental and theoretical understanding, we will show that there is a substantial agreement between the findings of G.A.,^(4,5)

Fig. 9.3. Simplified Flowsheet for Section II (Courtesy G.A.)

TABLE 9.1

**EFFECT OF TEMPERATURE ON RECYCLE
MASS FLOW RATES FOR SECTION II**

Note the increases in mass flows in streams 235, 231, and 232 as the result of decreasing temperature and increasing recycle.

gmole per gmole H₂

General Atomic 1144 K Decomposer

Component	H ₂ SO ₄ Feed 201	Recycle 235	Evap. Vapor 258	Boiler Vapor 231	Decomp. Prod. 232	Coiled Prod. 237
H ₂ SO ₄	0.9990	0.3620	0	0.1961	0.0005	0
SO ₃	0	0	0	1.1644	0.3615	0
SO ₂	0.0038	0	0	0	0.9990	1.0052
O ₂	0	0	0	0	0.4995	0.4965
H ₂ O	4.0935	0.7726	4.7114	1.3191	1.5152	0.3895
Temperature (K)	393	418	425	800	1144	368
Pressure (atm)	2.0	2.0	4.9	7.8	5.2	2.0

TMR Driven 1050 K Decomposer

H ₂ SO ₄	0.9990	0.821	0	0.264	0.0008	0
SO ₃	0	0	0	1.561	0.820	0
SO ₂	0.0038	0	0	0	0.9990	1.0052
O ₂	0	0	0	0	0.508	0.4965
H ₂ O	4.0935	0.773	4.7114	1.769	2.032	0.3895
T, (K)	393	418	425	800	1050	368
P, (atm)	2.0	2.0	4.9	7.8	5.2	2.0

TMR Driven 950 K Decomposer

H ₂ SO ₄	0.9990	2.431	0	0.496	0.0020	0
SO ₃	0	0	0	2.938	2.4330	0
SO ₂	0.0038	0	0	0	0.9990	1.0052
O ₂	0	0	0	0	0.4995	0.4965
H ₂ O	4.0935	0.7726	4.7114	3.0887	3.5827	0.3895
T, (K)	393	418	425	800	950	368
P, (atm)	2.0	2.0	4.9	7.8	5.2	2.0

Ispra,⁽⁹⁾ and Westinghouse⁽²⁾ for catalytic bed residence times.

Perhaps the most detailed studies of SO_3 reaction kinetics with varying residence time were done on the Fe_2O_3 catalyzed system by the Commission of the European Communities, JRC-Ispra Establishment, Italy, and reported recently.⁽⁹⁾ Their results are reproduced in Fig. 9.4. They show percent conversion versus residence time defined as the catalyst volume divided by the volumetric flow at 273 K and 1 atm.

For economic reasons, we want to select a low residence time, but consistent with high conversion. Ispra selected around 1.0 s at 64% conversion. Although residence times down to around 0.7 s appear feasible from Fig. 9.4, the steepness of the curve causes the reactor to be unstable. Unfortunately, in their work they only examined Fe_2O_3 in this detail and not the more active platinum-based catalysts. Much of their catalyst work remains IEA restricted.

We therefore turn to the work of G.A., where a variety of catalysts has been studied^(4,5) but in less detail. G.A. carries out their studies using a different definition of residence time, with the volumetric flow based on process temperature and pressure instead of STP, i.e.:

$$\tau = \frac{\text{vol. cat. bed voids, m}^3}{\text{vol. flow at T,P, m}^3/\text{s}} \quad (1)$$

For comparison, a residence time using Ispra's definition would be comparable to a 20% lower residence time defined as in equation (1) above (at 1050 K and 6.5 atm). In G.A.'s studies,^(4,5) the residence time was varied from 0.2 seconds on up; catalysts tested were Fe_2O_3 , CuO , and Pt . All

Fig. 9.4. Hydrogen Production by the Decomposition of 96W% H₂SO₄.
The G.A. Fe₂O₃ Catalyst is Far More Effective

appeared to be attractive for our proposed conditions of ~5 atm and 1050 K without showing any decline in performance down to 0.2 s. In other words, their conversion performance remained high on a flat portion of the conversion-residence time plot down to 0.2 s, as is shown in Fig. 9.4. We conclude that the G.A. Fe_2O_3 catalyst is far better than Ispra's Fe_2O_3 catalyst.

For the purpose of this design, we have selected 0.5 sec as the residence time for the SO_3 decomposer as being safely on the high, flat portion of the conversion-residence time plot, based on G.A.'s work^(4,5) on the Fe_2O_3 , CuO , and Pt catalysts. Without the use of catalysts, G.A. has shown⁽⁴⁾ that the conversion of SO_3 to SO_2 would be only 3% at 1050 K and that another 200 K would be required to achieve 95% at 1250 K. Thus, a complete elimination of the catalyst would be possible at 1250 K. We have reviewed the G.A. work^(4,5) at 1050 K and found that we could eliminate the need for the expensive platinum catalysts by substituting the much cheaper CuO (Cu-0803T) or Fe_2O_3 (Fe-0301T) catalysts available from Harshaw Chemical. These catalysts perform nearly identically. However, they are both deactivated by a sulfation reaction involving the substrate (e.g., Al_2O_3) on which the active metal oxide catalyst is placed. We have selected CuO as slightly preferable on the basis of a deactivation temperature around 950 K⁽⁵⁾, as compared to Fe_2O_3 at 1000 K⁽⁵⁾. There still remains some risk of catalyst deactivation due to operating plant temperature excursions. Operating at 1050 K provides a safety margin of 50 K for Fe_2O_3 and 100 K for CuO . If a catalyst is needed for temperatures below 1000 K, Pt provides the best option. Also, if low Pt content catalysts could be developed, Pt would be an alternate choice for the entire temperature regime.

We have reviewed the experimental apparatus of Norman, et al., at G.A.⁽¹⁰⁾ and found the laboratory reactor to be a basic

plug flow unit with the catalyst bed contained in a quartz tube, 1.8 cm i.d. and 5 cm long. Catalyst pellets 3.18 mm in diameter and length, as short cylinders, were used. We estimate from early packing studies of pellets⁽¹¹⁾ in their Fig. 8-13, that for an effective spherical diameter of 4.01 mm these pellets will pack into a 1.8 cm i.d. cylindrical vessel with a void fraction of 0.39, including the vessel wall void defect. For larger commercial vessels with less of a wall effect, void fractions of ~ 0.35 are to be expected.

This packing information and the definition of the residence time, τ , used by G.A.^(4,5), as in equation (1) above, can be used to estimate the catalyst requirements for a commercial-size plant. The decomposer maximum total volumetric flow (including H_2O , SO_3 & recycle) that would be possible for our plant driven by a 5,040 MW_t TMR with a process heat demand of 608.85 kJ/gmol H_2 ^(12,13) can therefore be calculated as 437 m³/s at 1030 K and 6.5 atm. The latter pressure was selected as an average of the SO_3 decomposer inlet and outlet conditions. Now from equation (1) we can estimate the catalyst bed at 560 m³, assuming the same catalyst geometry as used in the experimental work of G.A.

In our estimates for the catalyst volume of 560 m³, we assumed that the volume fraction of the catalyst substrate which is active is the same for the G.A. pellet catalyst and our smaller spherical catalysts used in the fluidized bed. We know⁽¹⁵⁾ this is to be quite conservative as shown in Fig. 9.5. The catalyst is really active only to a depth of 150 μm and has no activity beyond 250 μm .⁽¹⁵⁾ If we allow for this reduction in the inert substrate volume at the center of the catalyst volume in scaling from the G.A. pellet to a smaller diameter sphere, the catalyst volume requirement would be markedly reduced. A larger reduction is obtained if the entire catalyst sphere is considered active right down to the center.

Fig. 9.5. Catalyst Geometry and Contrast Between Packed Bed and Fluidized Bed Catalysts

With activity falling to zero around a radius depth of 0.25 mm (250 μm); a small 0.5 mm diameter catalyst would be ideal.

We examined the consequence of a 0.5 mm diameter catalyst and found a 2.5- to 7-fold reduction in catalyst volume requirements and a reduction in the decomposer volume by a factor of 2 or 3; however, the temperature gradients all increased so the overall ΔT increased from 22 K to around 60 K for a sodium-driven unit and 30 K to 70 K for a helium driven unit.

This could be countered by doubling or tripling the number of heat exchanger tubes, with an unavoidable increase in cost associated with higher complexity. So the benefits of a catalyst requirement reduction are difficult to capitalize on without attendant increases in ΔT or cost. But this is a tradeoff that should be kept in mind as we make the estimates of the overall plant economics.

9.4

SO₃ Decomposer Design

The design of chemical reactors with fast kinetics and large associated heat effects is one of the toughest problems in the chemical process industries.⁽¹⁶⁾ We have spent a substantial effort examining different types of chemical reactors to try to establish a design of least cost and greatest simplicity. The choice is made more difficult since the detailed SO₃ decomposition kinetic rate law is not known. Experimental and analytical difficulties for this very fast reaction have greatly hindered its determination.^(4,5) Therefore, we cannot determine such choices as the advantages of recycle, back mixing, residence time distribution, secondary addition, etc. More detailed kinetics studies should be planned in any future work.

The analysis for the first obvious choice, a plug-flow (flat velocity profile), packed bed reactor, shows this choice to be not feasible because of extremely large temperature gradients (i.e., 68 K) between the internal heat exchanger fluid supplying the heat to this highly endothermic reaction and the packed bed of catalysts. Furthermore, radial temperature gradients appear within the bed between the internal heat exchanger tube elements.

This problem of supplying the requisite heat into the decomposer has forced us to fluidized bed designs.⁽¹⁷⁾ Using a catalyst particle size of 0.5 mm and a catalyst density⁽¹⁴⁾ of 0.96 Mg/m³ and gas viscosity⁽¹¹⁾ of 0.04 g/m-s, the minimum fluidization velocity, U_{mf} , can be estimated⁽¹⁶⁾ from:

$$U_{mf} = \frac{\epsilon_0^3}{1 - \epsilon_0} \frac{(\rho_s - \rho_g)g D_p^2}{150\mu} \quad (2)$$

where ϵ_0 is the initial void fraction of the unfluidized bed, ρ_g is the gas density, ρ_s the density of the solid catalyst, g the gravitational acceleration, D_p the catalyst particle diameter, and μ the gas phase viscosity. From equation (2) we estimate $U_{mf} = 0.0428$ m/s.

We also need to estimate the entrainment velocity, U_t , at which the catalyst particles are suspended in the gas and carried out of the vessel.⁽¹⁷⁾

$$U_t = \frac{(\rho_s - \rho_g)g D_p^2}{18\mu} \quad (3)$$

where the remaining symbols are identical with those in equation (2). Equation (3) predicts $U_t = 3.67$ m/s. We must

operate the fluidized catalytic decomposer between these limits. We have selected the velocity to be $U_f = 1$ m/s.

At this velocity we can calculate the bed expansion⁽¹⁷⁾ from the following:

$$\frac{150}{g(\rho_s - \rho_g)} \frac{1}{D_p^2} (U_f - U_{mf}) = \frac{\epsilon^3}{1-\epsilon} - \frac{\epsilon_0^3}{1-\epsilon_0} \quad (4)$$

where the symbols are the same as before. Equation (r) predicts the expanded, fluidized bed void fraction to be $\epsilon = 0.8$. Consequently, the new bed volume will be 1627 m³ total, which can be broken up into seven beds of reasonable but large size: 8 m high by 6 m in diameter.

For such decomposer vessels, we find the particle Reynolds number will range from 34 to 82 and the modified Reynolds number:

$$Re^1 = \frac{U_f D_p}{(\epsilon - \epsilon_b)} \quad (5)$$

is estimated to range from 217 to 527, where ϵ_b is the correction to the geometric void-fraction due to flow patterns and is taken as 0.05,⁽¹⁸⁾ and the remaining symbols are the same as before. Under these conditions the dimensionless pressure drop group⁽¹¹⁾ is estimated at:

$$\frac{(-\Delta P) D_p \epsilon^3}{L_f \rho U_f^2 (1-\epsilon)} = 6.0 \quad (6)$$

where L_f is the height of the fluidized bed and the average inlet and outlet gas density with a value of 4.3 kg/m³. This predicts $\Delta P \approx -1.66$ atm. We have also allowed about 1 atm pressure drop across the distributor plate; thus $-\Delta P = 2.7$ total.

The pumping work can now be estimated for fluidizing this bed of catalyst. For seven beds of 6 m diameter, the volumetric flow rate is $198 \text{ m}^3/\text{s}$ and the work needed at this ΔP would be 51.4 MW_e . We indicate this as electrical work, although it is nearly equivalent to shaft work. This work requirement is for a basis $5,000 \text{ MW}_t$ reactor rating. For a $3,000 \text{ MW}_t$ basis, four catalytic decomposers would be used, and for $2,000 \text{ MW}_t$, three decomposers, and for $1,000 \text{ MW}_t$, one decomposer. A better way of expressing the pumping work is per unit H_2 production; this is 6.3 kJ/gmol H_2 , which is independent of scale. We consider this to an attractively small value.

9.5

Heat Transfer Aspects of the SO_3 Decomposer

We indicated earlier that the problem of supplying the endothermic heat requirements is a serious one; consequently, we now make a better estimate of the significant thermal gradients within the decomposer.

We first examine the catalyst surface to assess the ΔT there. We estimate⁽¹⁸⁾ the fluidized bed parameters as $\epsilon = 0.8$, $\epsilon_b = 0.05$, and turbulence level $Z_t = 0.25$. The Frossling group⁽¹⁸⁾ is found to be $Fs = 0.65$, and the Nusselt number ranges from 11.6 to 16.9, which corresponds to a heat transfer coefficient, h , ranging from $h = 1183$ to $1724 \text{ kcal/h}\cdot\text{m}^2$ of catalyst. The total catalyst external surface area for seven beds is $3.87 \times 10^6 \text{ m}^2$. With an endothermic reaction $\Delta H_R = +23.59 \text{ kcal/gmol SO}_3$ and 1.561 gmol H_2 in the decomposer feed, we calculate: $\Delta T = 0.24 \text{ K}$.

The available experimental literature⁽¹⁷⁾ indicates that in fluidized beds when the gas-solid heat transfer is good, as in our case with $\Delta T = 0.24 \text{ K}$, the particles and the gas are at thermal equilibrium and there are never significant ΔT 's within the fluidized bed itself. Instead, the heat transfer from the

fluidized bed to the heat exchanger surface is always controlling.⁽¹⁷⁾ And so it is in our case.

The heat transfer resistance occurs across a thin film between the fluidized bed and the surface of the heat exchanger vertical tubes. For this case, the Wender and Cooper correlation applies.⁽¹⁷⁾

$$\frac{h_w D_p}{K_g} = 0.01844 C_R (1-\epsilon) \frac{C_{pg} \rho_g}{K_g}^{0.43} \frac{D_p \rho_g U_f}{\mu}^{0.73} \frac{C_{ps}}{C_{pg}}^{0.8} \frac{\rho_s}{\rho_g}^{0.66} \quad (7)$$

where $\frac{D_p \rho_g U_f}{\mu} = 10^{-2}$ to 10^3 , h_w is the wall coefficient, C_R

is a function of the location off the center axis of the bed, K_g is the gas phase thermal conductivity, C_{ps} and C_{pg} the heat capacity of the solid and gas respectively, and all other symbols are as before. For our decomposer

$$\frac{D_p \rho_g U_f}{\mu} = \frac{C_{pg} \rho_g}{K_g} = 10033, C_R = 1.6, \frac{C_{ps}}{C_{pg}} = 0.79, \text{ and all}$$

other parameters are as before. Equation (7) predicts a wall Nusselt number of 24.23 and thus a heat transfer coefficient, h_w , of $h_w = 673 \text{ cal/s-m}^2\text{-K}$.

We set the maximum number of tubes in the decomposer by limiting their volume fraction to 10% and their tube diameter to 2 cm. Since the expanded bed volume is 1583 m^3 , the tube volume cannot exceed 158.3 m^3 . This corresponds to a total of 63,000 tubes distributed between the 7 decomposer units, or 9,000 tubes per decomposer placed on a hexagonal array with a pitch of 6 cm. These tubes must provide $4.29 \times 10^4 \text{ kcal/s}$ for each decomposer unit. Combining this heat requirement, the tube area of 4523 m^2 , and h , we obtain for this film $T = 14 \text{ K}$. This is high, but acceptable for fluidized beds.

Next we examine the sodium heat transfer fluid within these tubes. First we require that the sodium not decrease in temperature more than 100 K over its course through these tubes. For liquid sodium with a $C_p = 0.32 \text{ cal/g-K}$, this is equivalent to specifying the mass flow at $1.516 \times 10^3 \text{ kg/s}$ for each bed. We can use the Martinelli correlation:⁽¹⁹⁾

$$Nu = 7 + 0.025 (RePr)^{0.8} \quad (8)$$

which is specifically applicable to liquid metals. For sodium around 1050 K, with a thermal conductivity, $k = 19 \text{ cal/s-m-K}$, a viscosity of $\mu = 0.2 \text{ g/m-s}$, and a density of 0.8 Mg/m^3 , we estimate the Reynolds number at $Re = 50,000$ and Prandtl number at $Pr = 0.00337$. Thus, the $\Delta T = 0.2 \text{ K}$, which is very low and quite acceptable.

We can next estimate the sodium pumping work through 10 m long tubes from the friction factor of $f = 0.02$ ⁽⁹⁾, $\Delta P = 0.02 \text{ atm}$, and volumetric flow of $13 \text{ m}^3/\text{s}$ for all 7 beds, or 0.02 MW_e for a $5,000 \text{ MW}_t$ reactor or 2.02 J/gmol H_2 for any size reactor.

We have also estimated the ΔT across the 3 mm tube wall to be 6 K for Incoloy-800H to complete the decomposer vessel heat transfer analysis. We have combined all of these ΔT s in a schematic for the final decomposer configuration shown in Fig. 9.6. The overall ΔT between liquid sodium and the catalyst center is about 22 K. The liquid sodium feed temperature desirable would be around 1082 K at 7 atm. We selected the SO_3 range in temperature in Fig. 9.6 from 1003 to 1103 K so that the average will remain at 1050 K. The yield is taken at 55% as if an isothermal condition of 1050 K was maintained. Without more detailed kinetic studies on SO_3 decomposition little more is possible.

Fig. 9.6. Fluidized Bed Catalytic SO_3 Decomposer

We looked at the possibility of placing a helium gap of 0.6 mm in a duplex tube design to provide additional safety isolation between the sodium and the O_2 -containing process stream. We found that the ΔT across this gap would be 78 K--unacceptably increasing the blanket temperature. This problem was a further motivation for examining helium as the heat transfer fluid later in this paper.

We have also estimated the mass of these tubes (3 mm wall) and vessels (2 cm wall) to be 952 Mg and 211 Mg, respectively and find for Incoloy-800H the costs are acceptable. CuO and platinum catalysts would add negligibly for metal decomposition labor and for inventory at 0.05% Pt. No cost estimates for the $SO_3/SO_2/H_2O$ - protective aluminide coating are available, but we believe this coating will not be a significant cost.

A parametric study was done on varying the decomposer temperature. The mass balances for 100 K higher and 100 K lower are shown in Table 9.1; and the flows, vessel sizes and relative costs, and pumping power are shown in Table 9.2. There is a clear tradeoff between lower temperature and increased evaporator and decomposer vessel size, pumping power, and costs.

We also did a parametric study in varying the sodium flow rate through the tubes. Increasing the flow from 1516 kg/s per bed to 13,400 kg/s per bed dropped the sodium ΔT across the SO_3 decomposer from 100 K to 10 K. Under these conditions in order to keep the catalyst at 1050 K, the sodium feed temperature could be dropped from 1125 K as in Fig. 9.6 down to 1082 K. This is a drop of 43 K in the temperature requirements that must be provided by the fusion blanket, thus easing the alloy and safety problems in the blanket design. This is traded off with the added size of the transport piping and condensing vapor heat exchanger within the Cauldron blanket module. The

TABLE 9.2

A PARAMETRIC STUDY ON VARYING DECOMPOSER TEMPERATURE

	Decomposer	Temperature	
	<u>1144 K</u>	<u>1050 K</u>	<u>950 K</u>
SO ₃ Conversion, SO ₃ / SO ₂	0.735	0.55	0.29
Total Molar Flow in Evaporators, gmol/s	27,530	35,560	61,292
Volumetric Flow in Evaporators, m ³ /s	397	437	735
Volume Decomposer Catalyst, m ³	466	560	942
Catalyst Cost, Pt-Labor, M\$	4.44	5.33	8.9
Pt-Inventory (0.05% Pt), M\$	3.70	4.27	7.4
Number of Catalytic Decomposers in Use	6	7	12
Fluidization Pumping Power, kJ/gmol H ₂	5.5	6.3	10.9
Liquid Sodium Pumping Power, J/gmol H ₂	2.5	2.9	5.0
Relative Decomposer Vessel Costs, Incoloy 800H	100%	120%	200%

choice of material, described in Section 8.6, for a sodium-driven SO_3 decomposer would involve on the sodium side a 0.5 mm steel clad (Fe, 2-1/4% Cr, 1% Mo) on the Incoloy-800H internal heat exchanger tubes. On the SO_3 process side the Incoloy 800H would be coated with an aluminum layer, 100 μm or so thick.

9.6 Helium as an Increased Safety Option

We repeated the above design procedure while replacing the liquid sodium by helium as the heat transfer medium to carry the blanket heat out to the H_2 thermochemical plant. We illustrate with an example, which we believe to be a reasonable case, and we expect to do more detailed optimization on the combined system next year. We used a $\Delta T = 50$ K across the SO_3 decomposer and operated helium at 30 atm total pressure in order to minimize the stresses on the SO_3 decomposer internal tubes. The helium mass flow required was 1198 kg/s per bed or 120 m/s velocity for a decomposer with 18,000 tubes per bed (twice the number and twice the cost as in the sodium unit). The film ΔT for helium to the inside of these tubes was 18 K, thus raising the overall ΔT to about 30 K, very little above the 22 K for the sodium unit. The pressure drop was 1.0 atm across the decomposer, creating a pumping power of 110 MW_t (40 MW_e) for all 7 beds. Thus, from the decomposer standpoint, helium is a viable candidate and achieves greatly improved safety isolation between the O_2 -containing process stream and the blanket Li-Na pool. High helium pressures (i.e., 60 atm) will allow us to reduce the tubes back to 8,000, and will reduce pumping power to 60 MW_t ; but the added pressure will force us to double the thickness from 3 mm to about 6 mm. The cost would nearly double. The materials we

have selected for the He-driven SO_3 decomposer would involve Incoloy-800H at 2 mm thick wall, 20 mm diameter o.d., under our conditions of 30 atm, the stress level would be around 7.15 MPa (1100 psi), offering a safety factor of 2 over the 1% creep in 10^5h value of 13 MPa (2000 psi) $\pm$ 50% quoted in Section 8.6.

9.7

Tritium Management

In both blanket concepts: the Cauldron boiler or the flowing Li_2O system, we have developed concepts for controlling tritium releases whether the coolant coupling the blanket to the thermochemical process is either liquid sodium or helium. These sodium and helium loop processor systems were outlined in Section 5.7, and they apply to both blankets concepts since once the tritium enters the sodium or helium coolant the details of the blanket source become irrelevant. More details can be found in the Supplement.

The reference case we considered in Section 5.7 involved the 5 Ci/d limit of tritium losses into the H_2 product and an SO_3 decomposer operating at 1050 K and using 63,000, 10 m long, 20 mm o.d. Incoloy 800H tubes with 1 mm thick walls. When helium was used, twice as many tubes (i.e., 128,000) were required with the wall thickness increased to 2 mm. Thus, the tritium permeation remained the same.

9.8

Conclusions

1. The lower temperature range for operating the SO_3 decomposer is around 950-1050 K, allowing the TMR blanket to run around 1100 to 1200 K.
2. At 1050 K these are several viable catalyst options, such as CuO , Fe_2O_3 , and Pt, that will allow conversion in excess of 95% of equilibrium.

3. A fluidized bed SO_3 decomposer provides us with the best possible design, especially compared to packed bed designs for this critical highest temperature unit in the thermochemical cycle. The reasons for this are:
 - a. Low catalyst to process gas ΔT compared to packed bed.
 - b. Catalyst volume and hence reactor volume are reduced due to a smaller catalyst particle size and the elimination of inert catalyst material required in the packed bed.
 - c. The temperature profile between the heat exchanger tubes and the catalyst particles is much better in the fluidized bed, compared to the packed bed.
 - d. The thermal recovery of the catalyst in the fluidized bed is more rapid than in the packed bed.
 - e. The use of the smaller size catalyst further enhances both the heat and mass transfer coefficient.
4. Rough estimates of the capital and operating costs of the fluidized SO_3 decomposer appear attractive.

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