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BLANKET COMPARISON AND SELECTION STUDY

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FUSION POWER PROGRAM

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VIII. SOLID BREEDER TRITIUM RECOVERY

VIII.1 Introduction

In the development of a solid ceramic breeder blanket for a fusion reactor, a critical element is the selection of the blanket material. Many lithium compounds are being considered and we are just now identifying those prime candidates that fulfill requisite criteria tritium solubility, diffusivity, thermal stability, compatibility, and irradiation behavior. D-T type fusion reactors depend on having an adequate supply of tritium for operation. Current reactor designs include a tritium breeding blanket wherein bred tritium is recovered for use in the plasma. This chapter focuses on technical features that govern tritium release and recovery. Topics discussed include operating temperature limits, tritium solubility, tritium transport, irradiation effects, fabrication, and mechanical properties.

VIII.2 Temperature Limits

The tritium inventory in a candidate breeder material, which is characterized by transport (diffusion) and solubility (thermodynamic), is predicted to possess a strong temperature dependence and to be sensitive to diffusion path length. To ensure high tritium recovery (i.e., low tritium inventory), it may be necessary to operate at relatively high temperatures ($\sim 800^{\circ}\text{C}$) where the partial pressure for tritium or tritium-containing species above the solid is high and in-situ tritium diffusion is fast. However, at high temperatures, competitive processes such as grain growth and sintering may be enhanced, thereby reducing diffusion. It is thought that for optimum tritium recovery the solid breeder material must maintain interconnected porosity and small grain size throughout irradiation. Thus, any change in microstructure of the breeder material (such as grain growth and sintering) could affect the ease of tritium recovery.

In most reactor designs using solid breeder materials⁽¹⁾ the blanket is fixed and tritium recovery is achieved by sweeping a purge gas through

channels in a bed of the solid. The operating temperature of the breeder blanket must be high enough that acceptable tritium transport characteristics are achieved yet low enough that sintering, vaporization, etc., do not lessen blanket performance. We are recommending operating temperature limits based upon an assessment of materials properties that may affect either the upper or lower temperature bound ($T_{\max}$ or $T_{\min}$).

Bulk diffusion is considered to be a significant contributor to the tritium inventory in a blanket material. Using the mean residence time defined as the time required to extract 87.4% of the tritium, post-irradiation anneal experiments can be used to obtain diffusive inventory for solid breeders. Figure VIII.2-1 is a plot of the temperature dependence of diffusive inventories (for 20 μm grains) calculated using the equation $[I = 3.3 \times \tau(h)]$ and assuming a tritium generation rate per GWth of 1.87×10^{-3} g/s based on an assumed breeding ratio of 1.2 and 20 MeV/fusion. Based on this model, limitations can be defined where bulk diffusion is the rate-limiting step. A minimum temperature ($T_{\min}$) is defined as the temperature at which the tritium inventory exceeds 0.1 kg/GWth. While the selection of an inventory limit is somewhat arbitrary, it is clear that candidate materials exhibiting low inventory are certainly more desirable. In this analysis the minimum temperature limit assumes that no more than 5% of the blanket can be at temperatures below $T_{\min}$ and none of the blanket can be more than 50° below $T_{\min}$. Based on the above criteria, the minimum-temperature given in Table VIII.2-1 is established from diffusion controlled processes.

Except for Li_2O , the upper temperature limit for solid breeders (700-1400°C, Table VIII.2-1) is based on experimental determinations of the temperature at which closed porosity is apparent; this condition occurs at approximately 90% of theoretical density. In general, the thermal sintering characteristics of stable ceramic oxides are quite similar. Temperatures in excess of 0.8 T_m (T_m = the absolute melting point) are required before significant thermal sintering occurs. However, for some ceramic oxides, neutron irradiation is known to decrease the temperature at which sintering occurs. The effects of radiation may, therefore, close porosity at temperatures lower than 0.8 T_m , suggesting that lower values of $T_{\max}$ would be needed.

Figure VIII.2-1. Predicted tritium inventory in candidate solid breeder materials as a function of temperature after low fluence irradiation.

Table VIII.2-1. PROPERTY VALUES FOR CANDIDATE SOLID BREEDER MATERIALS^a

PROPERTIES	RECOMMENDED TEMPERATURE LIMITS					INFORMATION ONLY				
	MP, °C	ρ_{Li} , g/cm ³	κ^b , W/mK	T_{min} , °C	T_{max} , °C	ΔT , °C	$K \cdot \Delta T$	$\rho_{Li} \cdot K \cdot \Delta T$	Tritium Disso- lution	Breeding ⁱ Capability
Li ₂ O	1433	0.93	3.4	410 ^d	800 ^{c,h}	390	1325	1230	Low	Good
γ -LiAlO ₂	1610	0.28	2.2	300 ^d	1200 ^f	900	1980	554	Moderate	Poor
Li ₅ AlO ₄	1047	0.61	2.3	350 ^d	780 ^g	430	989	603	High	Fair
Li ₂ SiO ₃	1200	0.36	1.5	410 ^d	1000 ^f	590	885	319	High	Poor
Li ₄ SiO ₄	1250	0.54	1.5	320 ^d	950 ^f	630	945	510	Moderate	Fair
Li ₂ ZrO ₃	1616	0.33	1.3	400 ^e	1400 ^f	1000	1300	429	Moderate	Fair
Li ₈ ZrO ₆	1295	0.68	1.5	350 ^e	980 ^g	630	945	643	Moderate	Good
Li ₂ TiO ₃	1550	0.33	2.0	400 ^e	1185 ^g	785	1570	518	Moderate	Fair

^aSeparate effects data not available, therefore effects of radiation on temperature limits is unknown and not reflected in this table.

^bEstimated for 85% smeared density and temperature of 1000 K.

^cEstablished from chemical considerations, i.e., reaction with moisture to form LiOH. $\log P(LiOH, g)(atm) =$

$$\frac{-8635}{T} + \frac{1}{2} \log P(H_2O, g) + 4.57$$

^dEstablished from diffusion/inventory considerations.

^eEstimated assuming similar properties.

^fEstablished from onset of closed porosity, experimentally.

^gEstimated assuming $T_{max} = 0.8 T_m$, K.

^h1000°C for design approaches with helium purge gas flow directed only to the "cold" region of the breeder.

ⁱOnly Li₂O and Li₈ZrO₆ are considered capable of operating without the aid of a neutron multiplier.

Knowledge of the thermal conductivity of lithium ceramics is also important. There is concern that too low a thermal conductivity for the solid breeder would lead to excessive temperatures or require extra cooling to keep the system under control. At low temperature ($\sim 400^\circ\text{C}$) the thermal conductivity of Li_2O is almost twice that of LiAlO_2 ; however, at higher temperatures ($>700^\circ\text{C}$) the thermal conductivity of Li_2O approaches that of LiAlO_2 . The thermal conductivity for Li_4SiO_4 is similar to that of LiAlO_2 throughout the range of temperatures measured. The thermal conductivity for Li_2ZrO_3 appears to be lowest for all candidate materials.

There are important conclusions to be drawn from the thermochemical studies on Li_2O . It is evident that the solubility of LiOH in Li_2O is very low and the phases are nearly immiscible. Consideration of this fact is important because the blanket must not operate in a regime where LiOH is the stable phase. The pressure of T_2O in a blanket is likely to be highest in the coldest regions. Since the breeder must be at a temperature high enough to decompose LiOT this is another way of setting the minimum operating temperature for Li_2O at 410°C . Lower temperatures or higher T_2O pressures would cause a separate phase of LiOT to form, and reduction in tritium release is likely to occur.

At high temperatures ($>800^\circ\text{C}$), LiOT vaporization could present some design challenges in managing potential Li_2O transport. However, high temperatures are very attractive from a thermal-hydraulic design point of view. Thus, designs which take advantage of a high T_{max} must have purge gas flow directed only to the cold region ($<700^\circ\text{C}$) of the breeder.

In considering burnup effects and their limitations for solid breeder materials, the effects of compositional and structural changes predominate. For Li_2O the limitation is determined by radiation damage to the crystal, not by compositional change. For $\gamma\text{-LiAlO}_2$ there is a slightly reduced solidus as the composition shifts to form LiAl_5O_8 . For Li_2SiO_3 and Li_4SiO_4 , the reduced solidus is more dramatic, approaching a eutectic near 1030°C that may lead to enhanced sintering. By limiting the enrichment of ^6Li to 50 atom percent (which is necessary for maximum breeding ratio), substantial burnup can be

sustained as the ${}^7\text{Li}$ component of the candidate material can buffer effects of phase change or sintering. Thus, flexibility in setting the enrichment concentration allows higher burnups, as compositional changes in the breeder material can be accommodated by having the bulk of the material being only minimally affected by irradiation.

Table VIII.2-1 lists the melting point, MP, lithium density, ρ_{Li} , operating temperature range, ΔT , the thermal conductivity, K, and the product $\Delta T \cdot K$ for each candidate solid breeder material. Also included is the minimum temperature, which is based upon tritium diffusion, and the maximum temperature, which, except for Li_2O , is based upon thermal sintering and/or restructuring effects ($0.8 T_m$). The $\rho \cdot \Delta T \cdot K$ product represents a relative ranking of candidate materials, that is, the higher the numerical value the more favored is the candidate. Accordingly, Li_2O appears most favorable because of its high thermal conductivity, and Li_8ZrO_6 and Li_5AlO_4 appear almost as good because of their broad ΔT or high thermal conductivity. The compounds Li_2ZrO_3 , Li_2SiO_3 , and Li_4SiO_4 appear less favorable due to their lower thermal conductivity or narrower operating temperature ranges.

VIII.3 Tritium Solubility

VIII.3.1 Solubility Limits

VIII.3.1.1 Single Phase Systems

Experimental measurements on the solubility of water vapor with solid Li_2O at temperatures of 973 to 1273 K have been completed.⁽³⁾ Least squares fitting of the resulting data yielded the following analytical expression for the concentration of dissolved LiOH (expressed as mol fraction, x) as a function of temperature (in K) and partial pressure of water vapor (in atm.):

$$\begin{aligned} \log(x_{\text{LiOH}}) = & (0.427 + 1.700 \times 10^{-4} T) \log(p_{\text{H}_2\text{O}}) \\ & + (-17.667 + 2.502 \times 10^{-2} T - 9.62 \times 10^{-6} T^2) \quad [\text{VIII.3-1}] \end{aligned}$$

Assuming there are no isotope effects, this expression may be used to compute the solubility of LiOT in Li₂O. Figure VIII.3-1 is a plot of the relationship between concentration of LiOX (X = H or T) as a solute in solid solution in Li₂O and the partial pressure of X₂O. These curves indicate that the solubility of LiOH increases with temperature at constant partial pressure of water. Calculations using an ideal solution model with JANAF thermochemical data indicated that the solubility should decline with temperature, contrary to observations. This discrepancy has been resolved with the realization that the LiOH-Li₂O system is thermodynamically non-ideal. Calculations utilizing activity coefficients derived from the solubility data yield solubilities that follow the observed behavior. Generally similar results were obtained in the experimental study of Norman and Hightower.⁽⁴⁾ At this time comparison between the two studies is easiest in terms of activity coefficients; further discussion of the activity coefficients appears below.

VIII.3.1.2 Two Phase Systems

Experimental work has been completed to determine the partial pressure of water vapor required to precipitate LiOH from Li₂O in the temperature region (573 to 894 K) above and below the melting point of LiOH (744 K). The current data are in good agreement with earlier work on this system below the melting point of LiOH⁽⁵⁾ (723 K). Two equations represent the Tetenbaum and Johnson data. Below the melting point of LiOH, the minimum water vapor pressure required to form a two phase system is given by:

$$\log p(\text{H}_2\text{O}, \text{g}, \text{atm}) = -6718/T + 6.45 \quad [\text{VIII.3-2}]$$

For example, at 700 K, $p(\text{H}_2\text{O}) = 7.1 \times 10^{-4}$ atm. Above the melting point of LiOH the critical water vapor pressure is given by:

$$\log p(\text{H}_2\text{O}, \text{g}, \text{atm}) = -4406/T + 3.27 \quad [\text{VIII.3-3}]$$

At 800 K, $p(\text{H}_2\text{O}) = 5.8 \times 10^{-3}$ atm and at 900 K, $p(\text{H}_2\text{O}) = 2.4 \times 10^{-2}$ atm. Thus, the maximum allowable partial pressure of H₂O to avoid second phase

Figure VIII.3-1. Tritium solubility in Li_2O as a function of temperature and partial pressure of T_2O .

formation, is 710 vppm at 700 K. Less stringent conditions prevail at higher temperature.

There is a gap of about 80 degrees between the upper end of the temperature range of these data and the lower end of the temperature range for the single phase solubility data mentioned earlier. It is probably more reliable to extrapolate the two phase data to higher temperatures to provide an estimate of the water vapor pressure required to saturate the Li_2O with LiOH than to extrapolate the single phase solubility and activity coefficient data to lower temperature.

VIII.3.2 Thermodynamics of the Li-O-H System

VIII.3.2.1 Activity Coefficients

Activity coefficients (γ) for LiOH as a solute in Li_2O were derived from a combination of the Tetenbaum et al.⁽⁶⁾ data on the concentration of LiOH as a function of composition and temperature, and thermochemical data for LiOH(l) , LiOH(g) , $\text{H}_2\text{O(g)}$, and $\text{Li}_2\text{O(s)}$ from the JANAF tables. The resulting expression is:

$$\begin{aligned} \log(\gamma) = & (21.517 - 3.0263 \times 10^{-2} T + 1.1392 \times 10^{-5} T^2) \\ & + (3.1703 \times 10^{-2} - 7.3830 \times 10^{-5} T \log(\text{pH}_2\text{O}) \end{aligned} \quad \text{[VIII.3-4]}$$

The range of partial pressures in the experimental work was 10^{-5} to 10^{-3} atm. The rather large activity coefficient values ($\sim 10^3$) at lower temperatures are noteworthy. Over the concentration regime studied, the activity coefficients are greater than unity, indicative of positive deviation from ideality. Such deviations from ideality are interpreted to result from repulsive interactions between LiOH and Li_2O , which is suggestive of low solubility of LiOH in Li_2O (i.e., lower than would be predicted by an ideal solution calculation).

The Norman and Hightower study⁽⁴⁾ (see also Fig. VIII.3-2) has yielded the following activity coefficient expression, using the Gregory and Mohr⁽⁵⁾ data as reference values for the activity of pure LiOH:

$$\log(\gamma) = 4867/T - 0.808 + 1.27 \times 10^{-3} T - 1.051 \log T \quad [\text{VIII.3-5}]$$

This equation resulted from application of a regular solution theory expression to the data and is distinguished from Eq. VIII.3-4 by being independent of the partial pressure of water vapor. However, at temperatures above 700°C the two sets of data overlap for experimental errors considered at the two sigma level.

VIII.3.2.2 Thermodynamic Calculations

Some implications of the non-ideality of the system have been explored by using the SOLGAS computer program for chemical equilibrium calculations. The temperature range covered was from 700 to 1300 K. The calculation was for 1 mole of Li₂O, the tritium activity was fixed at 10⁻⁹, and the oxygen activity was fixed at 10⁻²⁰. The gas phase volume is 15% of the system volume and the total system pressure is set at 1 atm to model the effect of a sweep gas at that pressure. Non-ideality of LiOH (and of LiOT) in Li₂O in the condensed phase is accounted for by means of the derived activity coefficients. Gas phase behavior is taken to be ideal.

Two calculations were done: one for a protium-free system, and one having a protium activity fixed at 10⁻⁶ (equivalent to doping the carrier gas with 1 vppm protium). Protium-containing systems require consideration because all real systems will probably be contaminated to some degree by protium (mainly water) or they may be flushed deliberately with protium-containing sweep gas in an effort to enhance tritium recovery. Increased complexity in the species relationships is the result.

An important question for a protium-doped system is the degree of enhanced tritium recovery that results when compared to a protium-free system.

Figure VIII.3-2. Solubility of LiOH in Li_2O as a function of temperature and water pressure.

A comparison is made in terms of the change in the tritium content enhancement ratio with temperature for the gas phase. The tritium content enhancement ratio is the ratio of the sum of g. at. T in the gaseous tritium-containing species for the protium-containing system compared to the corresponding sum for the protium-free system. Thus, the species T_2 and T_2O are involved in the protium-free case, and T_2 , HT, T_2O , and HTO are considered in the protium-doped case. While the total number of moles of T in the gas phase is greater at lower temperatures for either case, the enhancement ratio is greater at a higher temperature. Numerically, the enhancement factor increases by a factor of about 3 from 800 to 1300 K; however, the gas phase tritium content at the lower temperature is about 3 orders of magnitude greater than at the high temperature. Thus, the gain in enhancement at higher temperature is probably not attractive enough to operate a tritium recovery system at higher temperatures just for that reason.

For the protium free (tritium only) case we shall indicate some general relationships among the various gaseous species and condensed LiOT. At low temperatures, T_2O is by far the dominant gaseous species. However, at about 1050 K T_2 , T_2O , and LiOT are of equal importance. Above 1050 K, LiOT gradually gains dominance over T_2 while T_2O declines. Interestingly, Li_2O gains in importance and at 1300 K it is the most important gaseous species, exceeding even LiOT in concentration.

The dimeric form of gaseous LiOT is relatively insignificant, but its concentration approaches that of LiOT at lower temperatures. This suggests that the activity of LiOH in the condensed phase might not be most accurately represented in terms of a single species (which essentially bears on the non-ideality of the system). At present, no detailed information is available on the atomistic nature of the solute species in Li_2O ; the nearest approximation so far seems to be the hypothesized "lithium vacancy plus T ion" model being discussed in Section VIII.4.

Calculations show that most of the tritium is retained as LiOT in solid solution in Li_2O . The most favorable situation from the standpoint of tritium recovery from the condensed phase is at 700 K where about 80% of the tritium

inventory is in the condensed phase. At higher temperatures, however, smaller fractions of the tritium content appears in the gas phase; yet, the situation should be viewed positively. If transport processes are kinetically fast, and this equilibrium calculation be taken as representing a steady-state condition, then the condensed phase LiOT represents the "held" inventory and it is only necessary that the carrier gas remove the gaseous species to keep up with the production rate. If we assume, (1) a 15 volume percent gas phase, (2) an average temperature of 1000 K, and (3) the gas phase is swept out once per minute, then, 21 moles of gas would pass over the 1 mole of Li_2O per year. At 700 K, this reduces to about 15 moles of gas. At 700 K the T_2O mole fraction is 10^{-3} and the amount of T_2O removed per year would be 0.015 mole, which suggests that a burnup rate of 1.5% per year can be easily handled (assuming thermodynamic equilibrium conditions prevail).

At 1300 K, Li_2O is the most prominent gaseous species at 10^{-8} mole fraction so that about 3×10^{-7} mole Li_2O would be transported in a year. Thus for a low oxygen activity, vapor transport of breeder material should not be significant. An assessment of vapor transport is a design-specific issue involving other factors in addition to thermodynamic ones. This is because the amount of material transported depends not only on its vapor pressure but also on the flow rate of the carrier gas, which can be different from the one assumed in this calculation. Further, the distribution of the condensate depends on temperature gradients and volumes of space available to receive condensate.

VIII.4 Tritium Transport/Mass Transfer

The rate and method of tritium transport in a ceramic breeder material are important considerations for determining tritium inventory. Unfortunately the literature⁽⁷⁻²²⁾ reporting diffusion-coefficient data have, in most cases, neither characterized the grain size nor identified the migrating species type. The modeling of tritium transport and release from solid breeder materials must recognize the relative importance of mass transport in the gas phase as well as solid state diffusion. Experimental investigations on oxygen

containing compounds have produced considerable uncertainty in the absolute value of the tritium diffusion and minimal data in characterizing the transfer of tritium from the gas-solid interface into the gas stream. The most extensive and well-characterized work has been performed on Li_2O . Figure VIII.4-1 shows the temperature dependence of the diffusion coefficient for tritium in Li_2O as well as for other relevant species, i.e., oxygen, lithium, hydrogen, and deuterium. A model for tritium diffusion in Li_2O has been suggested consistent with existing diffusion data.⁽²³⁾ Careful analysis of the data suggests that there exists a systematic pattern that can be interpreted in terms of the following diffusion mechanism.

Neutron irradiation converts the Li^6 components in Li_2O into tritium and helium creating at the same time (among other defects) a Li vacancy (V_{Li}). The origin of the V_{Li} can arise from several sources:

1. the extrinsic impurity-induced defects that control Li diffusion below 1000°C ,
2. the ${}^6\text{Li}(n,\alpha)\text{T}$ reaction that generates many defects in transforming Li into ${}^3\text{H}$ and ${}^4\text{He}$ atoms, and
3. defects generated via displacement damage, i.e., recoil of the energetic ${}^3\text{H}$ and ${}^4\text{He}$ atoms.

The following defect reaction takes place subsequently:

The notation V_{Li}/T denotes a stable defect complex comprising a V_{Li} and a tritium atom bound together in a certain geometrical configuration. This concept could be useful when discussing the chemistry of tritium release from Li_2O . However, the migrating component of this unit is the complex V_{Li}/T . In this defect complex one expects $E_{V_{\text{Li}}/\text{T}} \simeq E_{V_{\text{Li}}}$ since interstitial tritium should have a lower migration energy than V_{Li} . This interpretation is consistent with the tritium diffusion data (see Fig. VIII.4-1). Below 400°C the

Figure VIII.4-1. Temperature dependence of diffusion coefficients of tritium, oxygen, lithium, hydrogen, and deuterium in Li_2O

slope of $\ln D_T$ vs. $1/T$ is almost the same as that of the slope of $\ln D_{Li}$ vs. $1/T$.

On the other hand, this V_{Li}/T complex has only a finite free energy of binding (ΔG_b). If the temperature is raised above 400°C such that $|\Delta G_b| \lesssim k_B T$, then substantial dissociation into V_{Li} and tritium takes place. Thereafter, the tritium migrates mainly in a form that is not associated with Li vacancies, which should have a lower migration energy (i.e., lower slope) than the low-temperature regime. This hypothesis is again consistent with the data. The temperature above which substantial break-up of the V_{Li}/T complex takes place appears to be 500°C . According to this model, there should be a temperature regime (between 400 and 500°C) where both V_{Li}/T and interstitial tritium make a substantial contribution to tritium release from Li_2O . There should then be a non-Arrhenius behavior in that regime. This prediction agrees very well with the qualitative trend indicated by the Nasu dataset which is the only one that spans the critical temperature regime.

Diffusion data for the other lithium-containing solids of interest to fusion reactor design are either missing or incomplete and thus do not allow for mechanistic interpretation. The existing temperature dependence of the diffusion coefficients for LiAlO_2 , $\beta\text{-Li}_5\text{AlO}_4$, Li_2SiO_3 , and Li_4SiO_4 are presented in Figs. VIII.4-2 through VIII.4-5, respectively.

VIII.5 Irradiation Effects

Recent in-reactor tests have shown that the candidate breeder materials perform well although their current irradiation times are measured in months rather than years. Available data indicate that tritium retention is low and pellet integrity is reasonably good. Vaporization and swelling introduce potential $T_{\max}$ and lifetime limiting problems for Li_2O , whereas other materials do not appear to be as limited.

For current designs, Li_2O has a recommended $T_{\max}$ of 800°C or higher (Table VIII.2-1), depending upon the design approach selected. This upper limit is now established by vaporization mass transport as contrasted to an

Figure VIII.4-2. Temperature dependence of diffusion coefficient of tritium in $LiAlO_2$.

Figure VIII.4-3. Temperature dependence of diffusion coefficient of tritium in $\beta-Li_5AlO_4$.

Figure VIII.4-4. Temperature dependence of diffusion coefficient of tritium in Li_2SiO_3 .

Figure VIII.4-5. Temperature dependence of diffusion coefficient of tritium in Li_2SiO_4 .

earlier limit associated with tritium release mechanisms, i.e., sintering, grain growth, etc. Irradiation data show the occurrence of grain growth in Li_2O at temperatures above 700°C , but the data also imply that the dependence of tritium release on grain growth is not as strong as earlier suggested.

The tritium inventory limit of a blanket (see Table VIII.5-1) is not represented here as a single quantitative value, since other factors such as lifetime and tritium production rates directly affect the inventory. Instead, the tritium inventory is considered on a percentage basis. From this viewpoint, a 2% inventory at the end-of-life represents a 2% burden on the breeding ratio requirements of a specific blanket design. For a commercial size reactor producing 500 g tritium per day and operating for 3 years at 75% availability, a 2% inventory represents 8.2 kg of tritium. Hence, the tritium retention data for Li_2O (Table VIII.5-1) indicate that under worst-case conditions (TULIP, i.e., large grains, moderate temperatures and static-plena) tritium inventories of 9% were accommodated. Under more favorable conditions (FUBR-1A: finer grain size and higher temperatures) much lower tritium burden was found. The optimum condition occurred in the TRIO experiment wherein only 0.003% retention was found.

Table VIII.5-1. IRRADIATION EXPERIMENTAL DATA ON SOLID CERAMIC BREEDER MATERIALS

Experiment/Reactor	Materials	Temperature	Maximum Burnup (10^{20} captures/ cm^3)	Tritium Retained %
OG-5/ORR(24)	LiAlO_2	850°C	10.3	0.003
	Li_2O	600°C	0.3	0.5
	Li_4SiO_4	600°C	0.3	0.5
TULIP/EBR-II(25)	Li_2O	600°C	3	<8
	Li_2O	600°C	11	<4
FUBR/EBR-II(26)	Li_2O	500°C	3	<9
	Li_2O	700°C	3	<3
	Li_2O	900°C	3	<1
	Li_2O	900°C	3	<1
TRIO-1	LiAlO_2	650°C	2.2	<0.003

Tritium recovery from the removed blanket material at end-of-life would further reduce the impact of retention.

The integrity of monolithic ceramics bodies featured in some design concepts take on importance when tight temperature limits are imposed for materials like Li_2O . Pellet cracking and material rearrangement have resulted in uncertainties in the temperature distribution within the blanket. During the irradiation experiments completed to date, pellet integrity appeared good for all the materials considered, but internal crack generation has been observed; however, large thermal gradients did not develop in the pellets tested. The cracks observed were found to be generally parallel to the direction of heat transfer, so they would have a minimal impact on operating temperature.

Vaporization and redeposition on cladding walls of approximately 3% of the Li_2O was observed in the FUBR-1A experiment at an estimated temperature of 900°C and a burnup of 3×10^{20} captures/ cm^3 . The transport was most probably in the form of LiOT vapor. Consequently, solid breeder designs having flow channels located in regions with temperatures above 800°C must accommodate some solid breeder material redistribution. Purge gas containing the vaporized LiOT species can transport material to cooler regions downstream, where condensation/deposition of LiOT could result in flow blockage. It is not clear what level of transport is permissible in this scenario. On the other hand, if designs locate flow channels in only the lower temperature regions of the blanket ($T < 700^\circ\text{C}$), then LiOT transport distances are reduced and potential problems considerably reduced. In a design having low temperature flow channels, transport would be radially along the temperature gradient within the solid breeder, and deposition would be on other Li_2O grains that were operating at lower temperatures ($T < 700^\circ\text{C}$). This deposition could restrict gas permeability somewhat, but is not likely to produce the blockages considered possible for the high temperature flow channels.

Swelling data on lithium ceramics in the FUBR-1A experiments indicate that swelling may be the most undesirable property of Li_2O in comparison to other ceramics. In that experiment, Li_2O pellets, did not densify through

sintering but swelled in an inhomogeneous manner to approximately 2% diametrical. Since swelling is thought to be caused by defect and helium production, it must be assumed that swelling will be proportional to burnup and will increase at higher burnup levels.

Material swelling during irradiation has provided lifetime limiting criteria for some materials in fission reactor applications, i.e., graphite, stainless steel, etc. In other situations, however, swelling does not limit the lifetime because it can be restrained by surrounding components, the most notable example of this situation is the restraint of mixed oxide fuel by stainless steel cladding. If Li_2O can plastically deform sufficiently at blanket operating temperatures, swelling could be restrained and perhaps not constitute a life limiting problem. If it cannot be restrained, designs must either accommodate this property or accept lower lifetimes. Other ceramics, such as LiAlO_2 , Li_4SiO_4 , and Li_2ZrO_3 , do not appear to swell and thus are not limited in the same way.

VIII.6 Blanket Fabrication/Configuration

VIII.6.1 Introduction

There are two possible blanket configurations for solid breeders: pressed and sintered pellets and spherical particle configuration (sphere-pac). The pressed and sintered technique has been used successfully for preparing Li_2O , Li_4SiO_4 , $\gamma\text{-LiAlO}_2$, and Li_2ZrO_3 pellets. Experience was gained in preparing large quantities of sintered Li_2O pellets for the TFTR lithium blanket module (LBM) program.⁽²⁷⁾ Sphere-pac technology has been developed for LWR and FTR fuels and is considered attractive for solid breeders. Compared to the pressed and sintered technique, the sphere-pac configuration could potentially allow higher thermal conduction and minimize contact resistance in breeder blanket designs.

VIII.6.2 Fabrication of Li₂O Pellets

Cold-pressing and sintering techniques were developed for Li₂O, Li₄SiO₄, and γ -LiAlO₂ samples irradiated in ORR.⁽²⁸⁾ For the EBRII irradiation experiment, techniques were developed by ANL and MDAC to control impurities, structures, and grain size of Li₂O, Li₄SiO₄, γ -LiAlO₂, and Li₂ZrO₃ powders and by HEDL for fabricating pellets from these powders by hot pressing.⁽²⁹⁾ Li₂O pellets of 70% to 93% theoretical density were prepared using cold pressing and sintering by Takahashi and Kikuchi.⁽³⁰⁾ Dense pellets up to 99% theoretical density were prepared by these same authors using hot isostatic pressing and hot pressing methods. Current efforts suggest that grain size can be best preserved using hot pressing methods.

VIII.6.2.1 Lithium Oxide Pellet Fabrication Procedure

Lithium oxide pellets, cylinders 1 in. in diameter by 1 in. in height, have been prepared for the LBM experiment. From the point of view of lithium atom density in the blanket, these pellets should be near theoretical density. However, to facilitate the release of the bred tritium open porosity must be maintained. Takahashi and Kikuchi⁽³¹⁾ showed that as the density of lithium oxide pellets is increased above 86% theoretical density, the fraction of closed porosity also increases rapidly, while for densities less than 86%, the fraction of closed porosity remains essentially constant at 3% to 4% of the pellet volume. Preliminary trials with lithium oxide powder showed that the fabrication of large lithium oxide pellets by cold pressing and sintering techniques was feasible. More detailed studies established the conditions for obtaining pellets of about 80% theoretical density.

Cold pressing was carried out in a steel die of 1.0452 in. inside diameter and 6 in. height. Stearic acid (in the form of 2 wt % solution in absolute ethyl alcohol) was used as die lubricant. Sintering was carried out in vacuum in a resistance furnace using a tungsten mesh heater, with the cold pressed lithium oxide pellet contained in a platinum crucible. The dimensions and the weight of the pellet were measured, both before and after the

sintering operation, and the green density of the pellet were calculated. The fabrication parameters studied are summarized below and illustrated in Fig. VIII.6-1(a), (b), and (c).

1. Lithium oxide pellets free from lamination defects can be obtained from minus 100 mesh powder if the cold pressing pressure is kept below 30,000 psi.
2. The green density of the cold pressed pellet increased with the cold pressing pressure.
3. Lithium oxide pellets prepared from minus 100 mesh powder at a pressure of 30,000 psi and sintered at 1150°C in vacuum for 4 h yielded densities of 86% of the theoretical value.
4. Reducing the pressure from 30,000 psi to 15,000 psi but keeping the same sintering temperature (1150°C) and time (4 h) does not reduce the pellet density significantly. However, if the sintering temperature is reduced while maintaining the sintering time at 4 h, the sintered pellet density decreases.
5. If the pressure is reduced from 15,000 to 10,000 psi, the sintered pellet density decreases further. For the same sintering temperature (in the range of 1010-1110°C) and time (4 h), such a drop in cold pressing pressure leads to a 2.5% decrease in density.
6. Longer sintering time raises the pellet density. For a cold pressing pressure of 10,000 psi and a sintering temperature of 1110°C, an increase in sintering time from 4 to 6 hours raised the sintered pellet density from 82 to 83.5% of the theoretical value.

On the basis of these observations, the following procedures are recommended for the fabrication of lithium oxide pellets by the cold pressing and sintering technique. The walls of the cavity of the steel die and the surfaces of the steel plungers should be brushed with a solution of 2 wt %

Figure VIII.6-1. Study of fabrication of Li_2O pellet by cold pressing and sintering.

stearic acid in absolute ethyl alcohol. A known amount of lithium oxide powder of minus 100 mesh size should be added to the cavity of the steel die and pressed at a pressure of 10,000 or 15,000 psi, then sintered in vacuum at 1050°C (for a cold pressing pressure of 10,000 psi) or 950°C (for a cold pressing pressure of 15,000 psi).

The reproducibility of the pressed and sintered lithium oxide pellets was assessed. Nine pellets were placed on a nickel tray inside a 2.5 in. diameter Inconel-601 tube and the tube was situated horizontally inside a Globar furnace that provides a constant temperature zone ($\pm 10^\circ\text{C}$) of 10 in. length and evacuated to 10^{-4} Torr during the sintering operation. The pellets were cold pressed at 10,000 psi and sintered at 1050°C for 4 h.

The results on pellet density and diameter are shown in Table VIII.6-1. The average density of the 27 sintered pellets is 80.6% of the theoretical value, with a standard deviation of $\pm 0.6\%$. The average diameter is 0.9851 in., with a standard deviation of ± 0.0032 in.

In addition to density and diameter, the microstructure of one sample from each of the five runs were examined at the edge, the center, and midway between the edge and center. No significant difference was observed among the samples or for the same sample at different locations. Typical examples are shown in Fig. VIII.6-2(a), (b), and (c). The samples for metallographic examination were vacuum-impregnated with epoxy resin before polishing. The light color network in these figures represents porosity accessible to the resin, i.e., open porosity. It appears that the porosity is "open", which should facilitate the release of the bred tritium.

VIII.6.3 Fabrication of Sphere-Pac Breeder Materials

The sphere-pac route for fabrication of fissile fuel is an alternative to the conventional pellet method. With the sphere-pac process, high density ceramic spheres of controlled sizes are produced, and these are subsequently loaded, with the assistance of low-energy vibration, into the cladding.

TABLE VIII.6-1. DENSITIES AND DIAMETERS OF Li_2O PELLETS FABRICATED FOR REPRODUCIBILITY STUDY (COLD-PRESSED AT 10,000 PSI, SINTERED IN VACUUM AT 1050°C FOR 4 H).

Run No.	Pellet No.	Green Pellet Density (% TD)	Sintered Pellet Density (% TD)	Diameter of Sintered Pellet ^a (in.)	
I	1	68.8	80.3	0.9844	
	2	69.3	81.0	0.9849	
	3	69.3	80.0	0.9887	
II	1	69.8	81.4	0.9820	
	2	69.5	81.2	0.9804	
	3	69.5	79.9	0.9874	
III	1	69.7	80.7	0.9846	
	2	69.6	80.9	0.9824	
	3	69.7	79.5	0.9910	
IV	1	69.9	80.5	0.9839	
	2	70.4	80.6	0.9836	
	3	69.7	80.8	0.9829	
	4	69.8	81.4	0.9805	
	5	69.9	79.7	0.9894	
	6	69.8	79.9	0.9885	
	7	70.0	81.5	0.9811	
	8	69.8	81.1	0.9844	
	9	69.9	80.1	0.9888	
V	1	69.8	80.1	0.9897	
	2	69.8	81.0	0.9866	
	3	69.9	80.5	0.9838	
	4	69.9	81.3	0.9808	
	5	70.0	81.5	0.9868	
	6	69.8	80.0	0.9888	
	7	69.5	81.3	0.9805	
	8	69.7	80.4	0.9829	
	9	69.8	79.8	0.9897	
		Average Density	80.6%	Average Diameter	0.9851 in.
		Standard Deviation	±0.6%	Standard Deviation	±0.0032 in.

^aAverage value of six measurements made at the two ends and the middle of the pellet for two diametrical positions 90° to each other.

(a) Midway between edge
and center of pellet

(b) Edge of pellet

(c) Center of pellet

Figure VIII.6-2. Microstructures of sintered lithium oxide pellet.

VIII.6.3.1 Gel-Sphere-Pac Process

For fission fuels the gel-sphere-pac preparation is based upon three major steps: (1) preparation of a sol or special solution ("broth"), (2) gelation of droplets of sol or broth to give semi-rigid spheres, and (3) drying and sintering these spheres to a high density. Gelation (gel) may be accomplished by two methods: water extraction (suitable only for sols), or ammonia gelation (suitable for both sols and broths, but used almost exclusively with broths). Ammonia gelation can be accomplished either externally, via ammonia gas and ammonium hydroxide, or internally via hexamethylenetetramine which generates ammonia when thermally decomposed; internal gelation is followed by treatment with ammonium hydroxide to complete the reaction.

Sphere-pac requires three sizes of spheres to achieve about 88% smear density. These sizes have diametral ratios of about 40:10:1. The actual diameters currently favored are 1200, 300, and 30 μm . If a lower density is acceptable, two sizes will yield about 85% smear density.⁽³²⁻³⁵⁾ The loading of sphere-pac fuel involves vibratory packing of spheres of the proper size ratio. Considerable technology has been developed regarding the identification of proper sizes, size ratios, blending ratios, and loading sequences to fabricate maximum smear densities at minimum loading times. Simultaneous loading of all three size fractions shows promise of overcoming the problem of excessive loading times for long rods. Much of the particle dispensing and blending technology developed for HTGR fuels is applicable. Sphere-pac loading of commercial-length fuel rods has been demonstrated.

It is clear that an adequate technology base has been developed for the gel-sphere-pac process which appears applicable to the solid breeder materials. Attention must be paid to the hygroscopic nature of Li_2O and the formation of LiOH . For fission reactors, sphere-pac fuel can be loaded into cladding of relatively simply cylindrical geometries by low-energy vibrations. However, the BOT blanket concept may require additional development for the packing of breeder material in irregular geometries.

A method for producing porous lithium oxide spheres was patented by Research Dynamics, Inc.⁽³⁶⁾ Molten lithium hydroxide is passed through a small bore vertical tube to form a liquid jet which disintegrates into molten microspheres. These spheres are cooled below their melting point in an inert atmosphere, subjected to reduced pressure not exceeding 2 microns of mercury, and subsequently heated to a temperature between 150°C and 200°C. A similar method may be applicable for producing high density solid breeder microspheres.

VIII.6.4 Observations on Conductivity and Contact Resistance Sphere-Pac Fuel

A theoretical model for calculating the thermal conductivity of sphere-pac fuel consisting of a ternary mixture of fuel microspheres has been developed for fission fuel.⁽³⁷⁾ The calculated results show:

1. An increase in thermal conduction with restructuring due to an increase in the contact area between adjacent fuel microspheres.
2. A reduction in thermal conduction with fission gas release as the thermal conductivity of fill gas is degraded.
3. An increase in thermal conductivity as fuel rod internal gas pressure is increased owing to the Knudsen domain effect in the gas gap between fuel microspheres.

For fusion reactors in which no fission gas will be released, the first and the third observations will still be applicable.

At 1000°C and a helium fill gas pressure of 2 MPa (20 atm), results⁽³⁷⁾ show that the effective conductivity of a mixture of three sizes of spheres of UO_2 (1.8 W/m-K) is much higher than a two size mixture (0.9 W/m-K). The conductivity of 85% TD UO_2 fuel at 1000°C is 1.85 W.m-K. Before the sphere-pac configuration is used for fusion reactor blanket modules, the effective thermal conductivity of the selected material will need to be determined.

Contact resistance between the solid breeder and the fuel cladding is very important in determining the temperature distribution of the fuel. For the $\text{U}_{0.80}\text{Pu}_{0.20}\text{O}_{1.99}$ fuel, gap conductance has been measured for sphere-pac fuel and pellet fuel of identical chemical composition.⁽³⁸⁾ These measurements show a gap conductance of $7.3 \text{ kW/m}^2\text{°C}$ for pellet fuel and $19.3 \text{ kW/m}^2\text{°C}$ for sphere-pac fuel. Similar improvements, assuming that they can be obtained for fusion reactor solid breeders, are important in defining the performance of the blanket concept.

VIII.6.5 Conclusions

The major advantage of the sphere-pac fuel over pressed and sintered pellets is the potential reduction of contact resistance between the breeder material and its containment. It is recognized that this advantage may be design dependent. The quantification of the contact resistance and the effective thermal conductivity for a particular solid breeder in sphere-pac form needs to be measured by experiment. The fabrication of microspheres for the selected breeder needs to be investigated and demonstrated. The technology base that exists for fabrication of sphere-pac fuel for fission reactors should be applied to fabrication of candidate breeder blanket materials.

VIII.7 Mechanical Properties

Experimental data on the mechanical properties of solid breeder materials are nonexistent at the present time. Baseline information is essential to design of solid breeder blankets and is necessary to understand the irradiation behavior of solid breeders. Elastic properties are needed to determine the thermoelastic mechanical behavior of materials, i.e., Young's modulus (E) and Poisson's ratio (ν). Fracture properties are needed to evaluate cracking under tensile stress. Estimation of these properties for Li_2O is included.

VIII.7.1 Elastic Properties

Young's modulus (E) and Poisson's ratio (ν) are the elastic properties required to determine the thermoelastic mechanical behavior of materials. Thermal stresses in the solid breeder are directly proportional to E and are affected by ν , depending on the solid breeder configuration, i.e., whether the configuration approaches a plane-strain or plane-stress condition. Since thermal stresses typically vary from tensile to compressive in the solid breeder under a temperature gradient or during temperature cycling, the possibility of tensile-stress-induced cracking must be considered. These cracks can represent sources for localized stress and strain concentrations in the containment material. In the event of solid breeder swelling and/or ratchetting during blanket operation, the localized stress and strain concentration is a potential failure cause for the containment materials.

Recently, Krikorian⁽³⁹⁾ presented an expression for Young's modulus of Li_2O . By correlating the melting temperature versus Young's modulus for a number of cubic oxides, he estimated the Young's modulus of Li_2O at room temperature and zero porosity to be 140 GPa. The porosity dependence of E is represented by an empirical relationship.

$$E = E_0 \exp(-b p) \quad [\text{VIII.7-1}]$$

where $E_0 = 140$ GPa, p is the fractional porosity and b is an empirical constant taken to be 4.0 based on Al_2O_3 data. The temperature dependence of E is represented by

$$E = E_0 [1 - T/T_m \exp(1 - T_m/T)] \quad [\text{VIII.7-2}]$$

where T_m is the melting temperature of Li_2O . The overall expression of E is obtained by combining Eqs. [VIII.7-1 and VIII.7-2], i.e.,

$$E = 140 \exp(-4 p) [1 - T/T_m \exp(1 - T_m/T)] \text{ GPa} \quad [\text{VIII.7-3}]$$

For the reference 85%-dense ($p = 0.15$) Li_2O , the calculated Young's moduli are 70 GPa at 410°C and 50 GPa at 800°C (based on $T_m = 1433^\circ\text{C}$). The estimated uncertainty of these values is a factor of 2 to 3.

The Poisson's ratio of Li_2O was also estimated by Krikorian.⁽³⁹⁾ A mean value of 0.25 was chosen and ν was assumed to be independent of porosity, temperature, and chemical purity. The estimated uncertainty for this value could be as much a factor of 2.

VIII.7.2 Fracture Strength

Ceramic materials are known to be brittle and are often observed to fracture at a critical uniaxial tensile stress. This maximum tensile stress (σ_f) can be used as a fracture criterion to determine whether or not cracking will occur in candidate materials. High σ_f value is desirable since it means a material can experience a large thermal gradient without cracking. In a thermoelastic stress analysis performed for the DEMO blanket,⁽⁴⁰⁾ it was concluded that the cracking propensity for Li_2O is high based on a reported value of $\sigma_f = 48.3$ MPa. However, as this value is for B-Spodumene which differs from Li_2O it cannot be legitimately used.

Based on Li_2O microhardness data and the roughly linear relationship between microhardness and tensile strength for several ceramic oxides, Krikorian estimated a tensile strength of 20.3 MPa for Li_2O of 30 μm grain size and 3% porosity. This estimate offers a basis for comparison with the calculated thermal stresses. The variation of strength with grain size and porosity is represented by an empirical expression

$$\sigma_f = \sigma_0 d^{-n} \exp(-b p) \quad [\text{VIII.7-4}]$$

where d is the grain diameter, p is the fractional porosity and σ_0 , n , and b are empirical constants. Using values of $n = 0.4$, $b = 10$, and ensuring that Eq. [VIII.7-4] yields $\sigma_f = 20.3$ MPa for $d = 30 \mu\text{m}$ and $p = 0.03$, Krikorian obtained a σ_0 of 108 MPa.

The temperature dependence of σ_f is represented by

$$\sigma_f = \sigma_0 [1 - 44 \exp (-7000/T)] \quad [\text{VIII.7-5}]$$

where T is in degree K. The overall expression of σ_f is obtained by combining Eqs. [VIII.7-4 and VIII.7-5], i.e.,

$$\sigma_f = 108 d^{-0.4} \exp (-10 p) [1 - 44 \exp (-700/T)] \text{ MPa} \quad [\text{VIII.7-6}]$$

with an estimated uncertainty of a factor of 2 to 3. Equation [VIII.7-6] depends strongly on porosity and grain size, but shows little temperature dependence over the range of interest (410-800°C). For the reference case of 85%-dense ($p = 0.15$) Li_2O with an average grain diameter of 1 μm , the calculated σ_f values are 24.1 and 22.5 MPa at $T = 410$ and 800°C , respectively. For the DEMO blanket employing a 70%-dense Li_2O , the calculated σ_f value would be even lower. However, it is still not low enough so that the previous conclusion regarding the Li_2O cracking propensity remains unchanged.

The estimated elastic properties and fracture strength can be used in designing the Li_2O solid breeder blanket to avoid cracking. However, the uncertainties in these properties may render the approach risky. Furthermore, it will not be sufficient to consider only the thermal stresses generated by the steady-state temperature gradients. Thermal stresses incurred during temperature cycling are generally believed to be the cause for the Li_2O cracking observed to date.⁽⁴¹⁻⁴³⁾ The latter type of analysis has yet to be performed.

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IX. SOLID BREEDER BLANKET DESIGNS

IX.1 Introduction

Interest within the fusion community in solid lithium compounds as tritium breeders in power reactor blankets has grown considerably since the late 1970's. The solid breeders offer potentially attractive alternatives to liquid metal and liquid alloy breeders. The liquid breeders are perceived to have potentially strong disadvantages in MHD-induced pressure drop and associated pumping power losses as breeder/coolants, and in safety because of incompatibility with materials within the blanket or in nearby systems. The solid breeders avoid MHD problems, and in general have minimal or no chemical reactivity with candidate blanket coolants and other materials in reactor nuclear systems. However, the potential effects of radiation on tritium release characteristics and thermophysical properties of solid breeders are almost entirely unknown at this time. With the probable exception of Li_2O and Li_8ZrO_6 , all candidate solid breeders will require the use of a neutron multiplier to achieve net tritium breeding.

The present study has benefitted from a number of recent studies involving solid breeder blankets, of which References 1 through 8 are representative. This chapter begins with a discussion of performance/lifetime analyses of the solid breeders (Sec. IX.2), which focuses on predictions of lithium oxide (Li_2O) physical integrity and irradiation behavior, and on thermal conductance between solid breeder and structural material. (Chapter VIII deals with other issues related to solid breeder tritium release.) The next three sections describe the development of the study's three initial "mainline" solid breeder blanket concepts:

$\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept	Sec. IX.3
$\text{Li}_2\text{O}/\text{He}$ concept	Sec. IX.4
Ternary Oxide/ $\text{H}_2\text{O}/\text{Pb}$ concept	Sec. IX.5

The first two concepts use pressurized water and helium, respectively, as coolants for Li_2O , presently the leading candidate solid breeder. The third concept explores the potential of lead, in liquid or solid form, as an alternative to beryllium for use as a neutron multiplier in blankets with ternary oxide (e.g., LiAlO_2) solid breeder.

The approach taken in selection of the basic design and development of design details varied to some extent among the blanket concepts, which were the responsibility of three separate organizations (see Chapter I). This variation arises principally from (1) the characteristics of the concepts themselves, (2) the initial state of designs (if any) from previous studies, and (3) differences in perceptions of the degrees of emphasis necessary for each of the blanket evaluation criteria (in the absence of quantitative indices early in the study) in order to develop the "most attractive" design for each blanket concept.

Several studies had recently been performed for water cooled solid breeder blankets similar to the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept, but the anticipated problems of breeder cracking and breeder/structure interface heat conductance uncertainties led to an in-depth reevaluation of the approach to blanket arrangement and breeder fabrication. The $\text{Li}_2\text{O}/\text{He}$ concept likewise had a background of recent studies of similar concepts. Here, the primary emphases were to further develop existing designs to make the best use of helium's safety advantages, and to maximize tritium breeding and first wall cooling through the use of a bellows-type first wall designed to accommodate radiation-induced swelling without relying on radiation creep to relieve stresses. The $\text{TO}/\text{H}_2\text{O}/\text{Pb}$ concept does not have the advantage of previous design studies upon which to build. The challenges presented by those characteristics of the materials that were most unfavorable to blanket design--lead's low melting point and corrosion problems, the ternary oxides' low breeding potential, water's high pressure--resulted in primary emphasis being placed on tritium breeding and safety in tube rupture accidents.

IX.2 Performance/Lifetime Analysis

Several issues are of generic importance to the performance/lifetime of the solid breeder blankets. These issues include solid breeder physical integrity, thermal conductance between solid breeder and structural material, and the irradiation behavior (i.e., sintering, grain growth, creep and swelling) of solid breeders. In this section, analytical results are given for physical integrity (IX.2.1) and thermal conductance (IX.2.2). Solid breeder configuration/confinement is discussed in IX.2.3 which provides a basis for selecting the reference and backup designs for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ system.

IX.2.1 Solid Breeder Physical Integrity

Previous thermal stress analysis⁽¹⁾ showed that for DEMO design with a monolithic Li_2O solid breeder block, the temperature-gradient induced thermal stresses can reach very high magnitudes, well beyond the estimated solid-breeder fracture strength. Previous analysis⁽¹⁾ also showed that for thin disc geometry (instead of long cylinder) thermal stresses can be reduced somewhat, but the propensity for cracking is still very high. Since cracking and the resulting cracked fragment relocation create problems in controlling the gap between solid breeder and coolant tube, design solutions have been proposed that entail breeder segmentation both axially (planes normal to the direction of coolant flow) and radially (planes extending radially from the tube), and also the use of metallurgical bonding between breeder and coolant tube. Depending on the degree of segmentation, solid-breeder cracking, in principle, can be avoided.

Fabrication, especially the assembly of such segmented pie-shaped configuration, however, may represent a difficult task. Alternative solid-breeder geometries are being considered; either plate-type or beam-type geometry has been proposed in what is called a coolant panel design concept (IX.3).

One of the tasks for the coolant panel concept is to develop a one-dimensional, first order method for thermal stress analysis of solid breeders in plate or beam geometry. This method is given below, followed by an examination of the thickness requirements for a plate- or beam-type Li_2O solid breeder. The method is applicable to other solid breeders, but requires basic materials properties inputs which are unavailable at the present time.

IX.2.1.1 Analysis Formulation

The general plate and beam geometries of solid breeder are shown schematically in Fig. IX.2-1. The planar form of plate (Fig. IX.2-1a) is assumed arbitrary with a length L and a thickness $2h$. The beam (Fig. IX.2-1b) is assumed to have the same length and thickness as the plate, with a width b in the Y direction. By definition of plate, the thickness $2h$ should be much less than L , whereas for beam, $2h \ll L$ and $b \ll L$.

The layout of plate- or beam-type solid breeder in a blanket module is assumed such that significant heat flow occurs only in the thickness direction Z . For analytical simplicity, it is also assumed that a single plate or beam is situated between two adjacent coolant panels, and that the coolant flow conditions produce a symmetrical temperature profile $T(z)$ with respect to the X axis. With these assumptions and with a uniformly distributed volumetric heat source Q (from bulk nuclear heating), the steady-state temperature distribution in the solid breeder is

$$T(z) = T_0 + \frac{Q}{2k} (h^2 - z^2), \quad [\text{IX.2-1}]$$

where T_0 is the temperature at the plate or beam surface, k is the solid-breeder thermal conductivity (assumed constant), and h is the half-thickness of the plate or beam. Considering the solid-breeder temperature window ΔT , Eq. [IX.2-1] can be written as

$$\Delta T = T_{\max} - T_{\min} = \frac{Q}{2k} h^2, \quad [\text{IX.2-2}]$$

or alternatively,

$$h^2 \leq \frac{2k}{Q} \Delta T. \quad [\text{IX.2-3}]$$

Equation [IX.2-3] readily gives a criterion for sizing the plate or beam thickness based on the temperature window (ΔT) constraint. For example, if one takes 410 and 800°C as the minimum and maximum temperature allowables for Li_2O , the plate or beam thickness can be calculated as a function of Q and k .

(a)

(b)

Figure IX.2-1. Schematics of plate (a) and beam (b) type solid breeders.

For thermal stress analysis of plates or beams with temperature variation through thickness only, the formulations are similar and the plate case will be considered first. Surface tractions in both cases are assumed to be zero.

Plate

The thinness of the plate, i.e., $2h/L \ll 1$, and the nature of the temperature distribution, $T = T(z)$, result in the following stress components:

$$\sigma_{xx} = \sigma_{yy} = f(z); \sigma_{zz} = \sigma_{xy} = \sigma_{yz} = \sigma_{zx} = 0. \quad [\text{IX.2-4}]$$

Direct substitution shows that the equilibrium equations (with zero body forces) are identically satisfied for stress components given in Eq. [IX.2-4]. Of the six compatibility equations, three are identically satisfied by Eq. [IX.2-4], and the remaining three are satisfied provided that

$$\sigma_{xx} = \sigma_{yy} = f(z) = -\frac{\alpha E}{1-\nu} T + C_1 + C_2 z, \quad [\text{IX.2-5}]$$

where α , E , ν are the thermal expansion coefficient, Young's modulus and Poisson's ratio, respectively, and C_1 , C_2 are constants to be determined from boundary conditions of zero tractions on the edges of the plate. It is clear from Eq. [IX.2-5] however, that tractions cannot be made zero throughout the thickness except in the special case in which the temperature varies linearly with z . It is possible, on the other hand, to choose C_1 and C_2 such that for any $T(z)$ the resultant force and moment (per unit length) produced by σ_{xx} and σ_{yy} are zero over the edges of the plate, i.e.,

$$\int_{-h}^h \sigma_{xx} dz = \int_{-h}^h \sigma_{xx} z dz = 0, \text{ and} \quad [\text{IX.2-6}]$$

$$\int_{-h}^h \sigma_{xx} dz = \int_{-h}^h \sigma_{yy} z dz = 0.$$

The explicit forms of stress components with C_1 and C_2 determined from Eq. [IX.2-6] are then

$$\sigma_{xx} = \sigma_{yy} = \frac{1}{1-\nu} \left\{ -\alpha E T + \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T \right\}, \quad [\text{IX.2-7}]$$

$$\sigma_{zz} = \sigma_{xy} = \sigma_{yz} = \sigma_{zx} = 0,$$

where N_T , M_T are the resultant force and moment per unit length, respectively, given by

$$N_T = \alpha E \int_{-h}^h T dz; \quad M_T = \alpha E \int_{-h}^h T z dz. \quad [\text{IX.2-8}]$$

According to Saint-Venant's principle, Eq. [IX.2-7] is an accurate solution for stresses at distances about one-plate thickness away from the edges. It is also the unique solution of the stated thermoelastic boundary-value problem.

The strain and displacement components corresponding to the stresses given in Eq. [IX.2-7] can be obtained from the Hooke's law and the strain-displacement relationship, viz.,

Strain Components

$$\begin{aligned} \epsilon_{xx} = \epsilon_{yy} &= \frac{1}{E} \left\{ \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T \right\}, \\ \epsilon_{zz} &= -\frac{2\nu}{(1-\nu)E} \left\{ \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T \right\} + \left(\frac{1+\nu}{1-\nu} \right) \alpha T, \\ \epsilon_{xy} = \epsilon_{yz} = \epsilon_{zx} &= 0. \end{aligned} \quad [\text{IX.2-9}]$$

Displacement Components

$$\begin{aligned} u &= \frac{x}{E} \left\{ \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T \right\}, \\ v &= \frac{y}{E} \left\{ \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T \right\}, \\ w &= -\frac{3M_T}{4h^3 E} (x^2 + y^2) + \frac{1}{(1-\nu)E} \left\{ (1+\nu)\alpha E \int_0^z T dz - \frac{\nu z}{h} N_T - \frac{3\nu z^2}{2h^3} M_T \right\}. \end{aligned} \quad [\text{IX.2-10}]$$

Beam

Since the thickness ($2h$) and the width (b) of a beam are both much less than its length, plane stress condition prevails and the stress components are

$$\sigma_{xx} = \sigma_{xx}(z); \sigma_{yy} = \sigma_{zz} = \sigma_{xy} = \sigma_{yz} = \sigma_{zx} = 0. \quad [\text{IX.2-11}]$$

Equation [IX.2-11] satisfies equilibrium conditions identically, and the compatibility conditions are satisfied provided that

$$\sigma_{xx} = -\alpha E T + C_1 + C_2 z. \quad [\text{IX.2-12}]$$

All bounding surfaces are thus free of traction with the exception of end faces at $x = \pm L/2$. As in the plate case, this problem is circumvented by letting the resultant force and moment per unit length be zero, i.e.,

$$\int_{-h}^h \sigma_{xx} dz = \int_{-h}^h \sigma_{xx} z dz = 0. \quad [\text{IX.2-13}]$$

The only nonzero stress component thus obtained is then

$$\sigma_{xx} = -\alpha E T + \frac{1}{2h} N_T + \frac{3z}{2h^3} M_T. \quad [\text{IX.2-14}]$$

Comparing Eq. [IX.2-14] for beam and σ_{xx} in Eq. [IX.2-7] for plate, one sees immediately the familiar relationship between the plane-strain and the plane-stress results, i.e., the stresses differ in plate and beam only by a numerical factor, $1/(1-\nu)$, with stresses in the beam being lower than those in the plate.

The corresponding strain and displacement components in a beam are obtained similarly as in the plate case and are:

Strain Components

$$\begin{aligned}\epsilon_{xx} &= \frac{1}{E} \left\{ \frac{bN_T}{A} + \frac{z}{I} (bM_T) \right\}, \\ \epsilon_{zz} &= -\frac{\nu}{E} \left\{ \frac{(bN_T)}{A} + \frac{z}{I} (bM_T) \right\} + (1+\nu)\alpha T, \\ \epsilon_{yy} &= \epsilon_{xy} = \epsilon_{yz} = \epsilon_{zx} = 0.\end{aligned}\tag{IX.2-15}$$

Displacement Components

$$\begin{aligned}u &= \frac{x}{E} \left\{ \frac{(bN_T)}{A} + \frac{z}{I} (bM_T) \right\}, \\ v &= 0, \\ w &= -\frac{(bM_T)}{2EI} x^2 - \frac{\nu}{E} \left\{ \frac{(bN_T)}{A} z + \frac{z^2}{2I} (bM_T) \right\} + \alpha(1+\nu) \int_0^z T dz.\end{aligned}\tag{IX.2-16}$$

In Eqs. [IX.2-15] - [IX.2-16], the area A and the moment of inertia I of the beam cross section are

$$A = 2hb; I = 2h^3b/3.\tag{IX.2-17}$$

The general first-order formulation for thermal stress analysis of plate or beam with temperature variation through thickness only is given by Eqs. [IX.2-4] to [(X.2-17)]. In the following, a specific temperature distribution $T(z)$ given by Eq. [IX.2-1] is used to derive stress components in explicit forms. A second criterion for sizing the plate or beam thickness can then be established based on the solid-breeder fracture strength.

IX.2.1.2 Criteria for Determining Solid Breeder Plate or Beam Thickness

Substitution of Eq. [IX.2-1] into Eq. [IX.2-8] obtains

$$N_T = \alpha E \left\{ 2h T_o + \frac{2Q}{3k} h^3 \right\}; M_T = 0.\tag{IX.2-18}$$

Substitution of Eqs. [IX.2-18] and [IX.2-1] into Eqs. [IX.2-7] and [IX.2-14] obtains for the plate

$$\sigma_{xx} = \sigma_{yy} = \frac{\alpha E}{1-\nu} \frac{Q}{k} \frac{1}{6} [3z^2 - h^2] , \quad [\text{IX.2-19}]$$

and for the beam,

$$\sigma_{xx} = \alpha E \frac{Q}{k} \frac{1}{6} [3z^2 - h^2] . \quad [\text{IX.2-20}]$$

Equations [IX.2-19] and [IX.2-20] show clearly the nature of thermal stress distribution in the plate and beam respectively. For both plate and beam, maximum compressive stress occurs at $z = 0$, whereas maximum tensile stress occurs at $z = \pm h$ (i.e., the breeder surface). The transition from tensile into compressive stress regions occurs at $z = \pm 1/\sqrt{3} h$. The absolute magnitudes of the maximum compressive stresses are exactly one-half of the maximum tensile stresses.

For ceramic solid-breeder materials at temperatures below which plastic flow is significant, the materials response under stress load is essentially elastic and brittle. Under those conditions, the fracture strength (σ_f) is a useful criterion to determine whether solid breeders will crack or not. To apply this criterion, one needs to compare the maximum tensile principal stress to σ_f . This then provides the second criterion for sizing the solid-breeder plate or beam thickness. Writing the maximum tensile stresses from Eqs. [IX.2-19] and [IX.2-20] at $z = \pm h$, i.e.,

$$(\sigma_{xx} = \sigma_{yy})_{\max}^+ = \frac{\alpha E}{1-\nu} \frac{Q}{k} \frac{h^2}{3} \quad [\text{IX.2-21}]$$

for plate, and

$$(\sigma_{xx})_{\max}^+ = \alpha E \frac{Q}{k} \frac{h^2}{3} \quad [\text{IX.2-22}]$$

for beam, and from $(\sigma_{xx} = \sigma_{yy})_{\max}^+ < \sigma_f$ and $(\sigma_{xx})_{\max}^+ < \sigma_f$, one obtains for plate

$$h^2 < \frac{3(1-\nu)k}{\alpha Q} \left(\frac{\sigma_f}{E} \right) , \quad [\text{IX.2-23}]$$

and for beam

$$h^2 < \frac{3k}{\alpha Q} \left(\frac{\sigma_f}{E} \right) . \quad [\text{IX.2-24}]$$

Equations [IX.2-23] - [IX.2-24] and Eq. [IX.2-3] thus provide the two criteria necessary for sizing the solid-breeder plate or beam thickness.

IX.2.1.3 Plate- and Beam-type Solid Breeder Thickness Requirements

Detailed neutronic, thermal-hydraulic and heat transfer analyses of the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ coolant panel concept are still in progress. For purpose of illustration, the preliminary design values are used and the Li_2O (85%-dense) is assumed to have the following properties:

Thermal conductivity, $k = 4 \text{ W/m}^\circ\text{C}$

Thermal expansion coefficient, $\alpha = 2.66 \times 10^{-5}/^\circ\text{C}$

Poisson's ratio, $\nu = 0.25$

Young's modulus, $E = 0.62 \times 10^5 \text{ MPa}$

Fracture strength, $\sigma_f = 23.7 \text{ MPa}$

Three average nuclear heating rates, each representative of a separate region in the blanket are considered. Region 1 has the highest nuclear heating rate ($Q = 59 \text{ W/cc}$) and is taken near the first wall. Region 2 is taken within the blanket where nuclear heating is 25% of that in Region 1. Region 3 is taken near the shield region where the nuclear heating rate is 4% of the maximum value.

Table IX.2-1 gives the Li_2O solid-breeder plate and beam thickness requirements established from the temperature window (ΔT) and fracture strength (σ_f) constraints, Eqs. [IX.2-3], [IX.2-23], and [IX.2-24]. The temperature window is taken to be $\Delta T = 390^\circ\text{C}$ in all three regions.

TABLE IX.2-1. PLATE- AND BEAM-TYPE SOLID BREEDER HALF-THICKNESS (h) REQUIREMENTS

	Half-thickness, h (cm)		
	1	2	3
ΔT criterion	0.73	1.45	3.64
σ_f criterion (plate)	0.15	0.30	0.74
σ_f criterion (beam)	0.17	0.34	0.85

The salient features in the above table can be summarized as follows:

- a. Between the two criteria for sizing the solid breeder plate or beam thickness, the fracture strength (σ_f) criterion always results in smaller half-thickness (h) than that from the temperature window (ΔT) criterion. To avoid cracking, the plate or beam thickness should be determined from the σ_f criterion, which, if satisfied, automatically satisfies the temperature-window constraint.
- b. Based on the σ_f criterion, beam-type solid breeder can be made slightly thicker than the plate-type solid breeder. The former therefore should have higher tritium breeding ratio due to the increased volume content of solid breeder versus those of the structural materials and coolant.

It should be emphasized that the thickness requirements given in Table IX.2-1 are only preliminary estimates. The assumed properties values will of course influence the numerical results. Except for the thermal conductivity and thermal expansion coefficient which are based on the Li_2O data, the other materials properties are all estimates subject to substantial uncertainties (see Chapter VIII.6). Experimental investigation of solid breeder elastic and fracture properties is required to provide the basic information needs.

IX.2.2 Thermal Conductance

Thermal conductance (h_I) across the solid breeder/structural materials interface has been recognized as a key issue in the development of fusion solid breeder blanket technology. Previous analyses⁽¹⁾ showed that the sensitivities of the solid-breeder temperatures to variations in h_I and other parameters (e.g., solid-breeder thermal conductivity, neutron wall load) are comparable, but the uncertainty range of h_I may be larger.

The interface between solid breeder and structural materials (e.g., coolant tube, first wall panel, or module wall) can be classified into two broad categories: (1) a gap which may be filled with helium gas, and (2) a solid material which may be metallurgically bonded to the solid breeder and structural materials. For the gap interface, the heat transfer takes place by gas-phase conduction, radiation transport, and conduction through the solid-solid contact asperities on both surfaces. In the case of an open gap, the

last process is inactive. For the solid interface, the heat transfer takes place by conduction through the solid interface material, and the process is similar to that in solid breeder and in structural materials.

In the following, a general approach to the overall conductance problem for a gap interface is given based on theoretical considerations. Considerable detail and analysis are presented for the gas-phase conductance only; those for radiation transport and solid-solid contact will be treated in the future.

IX.2.2.1 Heat Transfer Across Interface

As mentioned earlier, there are three independent processes of heat transfer across a gap interface. Denoting the component conductances due to gas-phase conduction, radiation transport, and solid-solid contact by h_g , h_r , and h_s , respectively, one obtains for the overall interface conductance h_I ,

$$h_I = h_g + h_r + h_s \quad . \quad [IX.2-25]$$

The problem then becomes the determination of h_g , h_r , and h_s .

Gas Phase Conductance, h_g

It can be shown that for two flat plates 1 and 2 separated by a distance d ,

$$h_g = \frac{k_g}{d + g_1 + g_2} \quad , \quad [IX.2-26]$$

and for two coaxial cylinders of radii $r_1 < r_2$,

$$h_g = \frac{k_g}{r_1 \left(\log \frac{r_2}{r_1} + \frac{g_1}{r_1} + \frac{g_2}{r_2} \right)} \quad , \quad [IX.2-27]$$

for the inner cylinder, where k_g is the thermal conductivity of the gas in the gap, g_1 and g_2 are the temperature jump distances of surfaces 1 and 2 respectively, to be defined later.

Equations [IX.2-26] - [IX.2-27] show that h_g is proportional to k_g and inversely proportional to the sum of the separation distance between, and the

temperature jump distances of, the two surfaces. Each of these parameters then requires assessment from which h_g can be determined.

Gas Thermal Conductivity, k_g

In the kinetic theory of heat conduction by molecular action of gas molecules, Kennard⁽²⁾ showed that

$$k_g = \frac{1}{3} n \bar{v} L \frac{d\bar{\epsilon}}{dT} , \quad [\text{IX.2-28}]$$

n being the number of gas molecules per unit volume, $\bar{v}$ their mean speed, L their mean free path, $\bar{\epsilon}$ the average heat energy of a molecule, and $d\bar{\epsilon}/dT$ can be related to the specific heat of the gas, $n d\bar{\epsilon}/dT = \rho c_v$, where ρ is the density and c_v is the specific heat at constant volume. Equation [IX.2-28] applies to rarefied gas which ignores force actions between molecules. It is also assumed that the overall heat energy can be expressed as the sum of heat energies of separate molecules or as $n\bar{\epsilon}$.

Substitutions of the equation for gas viscosity, $\eta = \frac{1}{3} \rho \bar{v} L$, into Eq. [IX.2-28] obtains

$$k_g = \eta c_v . \quad [\text{IX.2-29}]$$

Equation [IX.2-29] has been tested against experimental values of k_g for a number of gases, and the calculated k_g are generally lower. Further refinements by Kennard and by Chapman resulted in

$$k_g = \frac{5}{2} \eta c_v , \quad [\text{IX.2-30}]$$

$$k_g = (1 + \delta) \frac{5}{2} \eta c_v . \quad [\text{IX.2-31}]$$

The parameter δ in the Chapman's equation [IX.2-31] is a very small number; Equations [IX.2-30] and [IX.2-31] can thus be regarded identical for practical purposes.

The density (ρ) and the mean speed ($\bar{v}$) of gas molecules are given by

$$\rho = \frac{M}{22.4 \text{ liter}} = 4.461 \times 10^{-5} M \text{ g/cc} , \quad [\text{IX.2-32}]$$

and

$$\bar{v} = \frac{2}{\sqrt{\pi}} \sqrt{\frac{2RT}{M}} , \quad [\text{IX.2-33}]$$

where M is the molecular weight (g), T is the temperature (K), and R is the universal gas constant (8.318 J/mole-°K).

The mean free path, $L(T)$, of gas molecules at temperature T is

$$L = \frac{L_o}{1 + C/T} = \frac{L_o T}{T + C} , \quad [\text{IX.2-34}]$$

where L_o is the mean free path at room temperature and C is a constant, both depending on the gas species.

Kennard⁽²⁾ gives a revised equation for gas viscosity that also includes temperature dependence, viz.,

$$\eta(T) = \frac{c_1 \rho \bar{v} L_o}{1 + C/T} = \frac{c_2 T}{1 + C/T} , \quad [\text{IX.2-35}]$$

where c_1 is very close to 0.5 and c_2 can be calculated from Eqs. [IX.2-32] - [IX.2-34].

The classical theory of specific heat of a gas ensemble (in which the gas molecules possess no other form of energy than translational) gives

$$U > \frac{3}{2} RT, \quad c_v > \frac{3}{2} R, \quad c_p > \frac{5}{2} R, \quad \gamma < \frac{5}{3} , \quad [\text{IX.2-36}]$$

where U is the total energy, c_v and c_p are the specific heats at constant volume and constant pressure, respectively, $\gamma = c_p/c_v$, and R and T have their usual meanings. Depending on the molecular type (e.g., rigid nonsymmetrical, dumbbells, etc.), theoretical calculations of c_v and c_p including relative vibrational energy of asymmetrical molecules are available.⁽²⁾ The

calculations compare reasonably well to the experimental results, especially for rare gases such as helium and argon.(2)

All the elements required for the theoretical calculation of the gas thermal conductivity k_g in Eq. [IX.2-30] have been defined by Eqs. [IX.2-32] - [IX.2-36]. Since the gap interface between the solid breeder and structural materials is filled with helium, a theoretical equation for the helium gas thermal conductivity, k_{He}^t , can be obtained using the helium parameters given in Table IX.2-2.:

TABLE IX.2-2. PARAMETER VALUES USED FOR DERIVING k_{He}^t
(HELIUM GAS AT ATMOSPHERIC PRESSURE)

	Molecular Data	Equation No.
M (g)	4.002	IX.2-32
L_0 (cm)	18.62×10^{-6}	IX.2-34
C (K)	0.5	IX.2-34
c_2 (g/cm-s-°K ^{1/2})	1.2066×10^{-5}	IX.2-35
c_v (J/g-°K)	12.63	IX.2-36
γ	1.659	IX.2-36

$$k_{He}^t = 9.525 \times 10^{-3} \frac{T^{1.5}}{T + 0.5} \text{ W/m-°K} , \quad [\text{IX.2-37}]$$

which reduces to

$$k_{He}^t \approx 9.525 \times 10^{-3} T^{0.5} \text{ W/m-°K} , \quad [\text{IX.2-38}]$$

since T is generally much greater than 0.5.

The calculated k_{He}^t from Eq. [IX.2-38] as a function of temperature is displayed in Fig. IX.2-2, along with a correlation for helium gas derived from experimental data by Gandhi and Saxena,(3) viz.,

$$k_{He}^e = 3.366 \times 10^{-3} T^{0.668} \text{ W/m-°K} . \quad [\text{IX.2-39}]$$

Figure IX.2-2. Comparison of theoretical (k_{He}^t) and experimental (k_{He}^e) helium-gas thermal conductivities as a function of temperature.

The two sets of results are in good agreement over the interface temperature range (573 - 773 K) of interest. Within that temperature range, the theoretical k_{He}^t is lower than the experimental k_{He}^e by no more than 8%. The reported uncertainty⁽³⁾ of k_{He}^e is $\pm 5\%$. Deviations at higher temperatures are expected since the force actions between gas molecules may no longer be ignored.

Temperature Jump Distances, g_1 and g_2

Poisson suggested long ago that at a wall bounding an unequally heated gas, there might be a discontinuity of temperature,

$$T_K - T_w = g \frac{\partial T}{\partial n}, \quad [\text{IX.2-40}]$$

where T_w is the wall temperature and T_K is what the temperature of the gas would be if the temperature gradient along the outward-drawn normal to the wall, $\partial T/\partial n$, continued without change right up to the wall itself. The constant g has a unit of length and is called the temperature jump distance.

From kinetic theory considerations⁽²⁾ g can be written as

$$g = \frac{2-a}{a} (2\pi RT)^{1/2} \frac{k_g}{(\gamma + 1) c_v p}, \quad [\text{IX.2-41}]$$

or alternatively,

$$g = \left(\frac{2-a}{a} \right) \frac{4C}{(\gamma + 1)} \left(\frac{k_g}{\eta c_v} \right) L, \quad [\text{IX.2-42}]$$

where a is the accommodation coefficient introduced by Knudsen, p is the gas pressure, and all other parameters have their previous definitions.

Substituting the parameter values for helium from Table IX.2-2 and noting that $L \simeq L_0$ and $k_g/\eta c_v = 5/2$, one obtains

$$g_i = 3.501 \times 10^{-7} \left(\frac{2-a}{a} \right)_i, \quad [\text{IX.2-43}]$$

where i denotes surface i against which the temperature jump distance of helium gas is to be estimated.

If all the gas molecules behave like elastic spheres and have random direction of motion, it can be shown that⁽²⁾

$$a = \frac{2 m_1 m_2}{(m_1 + m_2)^2} , \quad [\text{IX.2-44}]$$

where m_1 and m_2 are the masses of gas and wall molecules, respectively. Strictly speaking, Eq. [IX.2-44] applies only when the kinetic energy of translation is considered, and if each gas molecule struck the surface only once before returning into the gas.

Denoting the mass of helium gas as m_1 and the mass of Li_2O or structural material (assumed to be Fe) as m_2 obtains

$$a_1 (\text{He} - \text{Li}_2\text{O}) = 0.208; a_2 (\text{He} - \text{Fe}) = 0.125 , \quad [\text{IX.2-45}]$$

from which the temperature jump distances from Eq. [IX.2-43] can be calculated:

$$g_1 (\text{He} - \text{Li}_2\text{O}) = 3.016 \times 10^{-6} \text{ m}; g_2 (\text{He} - \text{Fe}) = 5.252 \times 10^{-6} \text{ m}. \quad [\text{IX.2-46}]$$

Separation Distance (Gap Size), d

For a gap interface in a solid breeder blanket, the initial separation distance, d_0 , is generally known or can be estimated. Difficulty resides in the determination of d during blanket operation. Since d is the distance between the two adjacent boundaries of the solid breeder and structural materials, any change in the boundary locations (either or both) will change d accordingly. Processes affecting the boundary locations during blanket operation include: (1) differential thermal expansion between solid breeder and structural materials, (2) solid breeder cracking and cracked fragment relocation, (3) solid breeder densification due to thermal/radiation-enhanced sintering, (4) solid breeder thermal and radiation-enhanced creep, (5) solid breeder radiation-induced swelling, and (6) time-dependent deformation, i.e., creep and swelling of the structural materials. Since each of these processes is driven by temperature and stress in the solid breeder as well as in the structural materials, and since temperature and stress are in part determined by $h_g \approx k_g/d$, the problem is of necessity of an iterative nature requiring

integrated thermal and mechanical analysis. This integrated approach will be taken in future lifetime/performance evaluation of the solid breeder blanket, but requires understanding of the mechanical and irradiation behavior of solid breeder and structural materials. The latter information will be generated from programs on fusion materials.

Every element needed for the determination of h_g [Eqs. IX.2-26, IX.2-27] has been defined. Explicit design equations for h_g are given and qualified in the following section.

IX.2.2.2 Qualification of the Design Equations for h_g

When the gap is large, i.e., $d \gg g_1 + g_2$, the effect of the temperature jump distances on h_g is negligible compared to that of d . Equation [IX.2-26] then becomes

$$h_g = 3.366 \times 10^{-3} \frac{T^{0.668}}{d} \quad \text{W/m}^2\text{-}^\circ\text{K} \quad . \quad [\text{IX.2-47}]$$

For very small d , i.e., on the order of surface roughness, Eq. [IX.2-26] becomes

$$h_g = \frac{3.366 \times 10^{-3} T^{0.668}}{d_m + 8.268 \times 10^{-6}} \quad \text{W/m}^2\text{-}^\circ\text{K} \quad , \quad [\text{IX.2-48}]$$

d_m being the root-mean-square roughness of the two surfaces given by

$$d_m = \left[\left(d_1^2 + d_2^2 \right) / 2 \right]^{1/2} , \quad [\text{IX.2-49}]$$

where d_1 and d_2 are the roughnesses of surfaces 1 and 2, respectively. To get numerical results, d_1 and d_2 are assumed to be 10^{-7} m, which are on the same order of magnitude as those used in the fission-reactor fuel element modeling.⁽⁴⁾ With $d_m = 10^{-7}$ m, Eq. [IX.2-48] becomes

$$h_g^{\text{max}} = 4.022 \times 10^2 T^{0.668} \quad \text{W/m}^2\text{-}^\circ\text{K} \quad , \quad [\text{IX.2-50}]$$

which gives an upper bound for h_g as a function of temperature.

Figure IX.2-3 shows the variation of h_g as a function of temperature for $d = 1, 2, 3$ and 4 mm. Over the interface temperature range (573-773K) of

Figure IX.2-3. Variation of gas-phase conductance with temperature and gap size d .

Figure IX.2-4. Variation of maximum gas-phase conductance ($d=0$) with temperature.

interest during blanket operation, the average h_g are $\sim 260, 130, 87,$ and $65 \text{ W/m}^2\text{-}^\circ\text{K}$, respectively, for $d = 1, 2, 3$ and $4 \times 10^{-3} \text{ m}$. For the same heat flux (q'') at the solid breeder surface, the temperature drop (ΔT) will double for doubling gap size according to $\Delta T = q''/h_g$. Assuming the coolant conditions remain the same, the solid breeder temperature must increase substantially.

The other extreme case for h_g as a function of temperature is displayed in Fig. IX.2-4 based on Eq. [IX.2-50]. Over the interface temperature of interest, h_g^{max} is $\sim 3.12 \times 10^4 \text{ W/m}^2\text{-}^\circ\text{K}$. This would be close to the maximum h_g during blanket operation.

Experimental study of interface conductance has received considerable attention in the fission-reactor community. Work is also being planned at GA to perform measurements of h_I under the FW/B/S PE-II program. Qualification of Eqs. [IX.2-47] and [IX.2-48] against the available experimental data will lend confidence to their application in the design analysis. Valuable insights can also be gained on the planned GA experiments.

Recently Nygren made available a rather substantial data set on the helium gas thermal conductivity by Le Neindre et al.⁽⁵⁾ Since k_{He} determines h_g through Eqs. [IX.2-26] - [IX.2-27], it would be of interest to compare Le Neindre et al.'s results to the calculated values from Eq. [IX.2-39] which is a correlation based on the experimental data of Gandhi and Saxena (with reported uncertainties of $\pm 5\%$).⁽³⁾ The computed nominal k_{He} at $376, 402,$ and 505°C are $0.252, 0.259,$ and $0.285 \text{ W/m}^\circ\text{C}$, respectively, compared to $0.267, 0.274,$ and $0.304 \text{ W/m}^\circ\text{C}$ of Le Neindre et al. Adjusting the calculated k_{He} upward by 5% obtains $0.265, 0.272,$ and $0.299 \text{ W/m}^\circ\text{C}$, which are almost identical to those of Le Neindre et al. It should be noted that Le Neindre et al. performed their experiments at low pressures ($< 0.01 \text{ atm.}$) and the above values are extrapolated results to atmospheric pressure. The data of Gandhi and Saxena, on the other hand, are quoted for atmospheric pressure. Whether or not this causes the systematic difference between the two data sets is presently unknown.

In an ex-reactor experimental investigation, Garnier and Begej⁽⁶⁾ measured the interface conductance between UO_2 and Zr-4 as a function of temperature in an helium environment (atmospheric pressure) for $d = 2.18 \times 10^{-5}$ and $6.05 \times 10^{-6} \text{ m}$. Their results are shown in Figs. IX.2-5 and IX.2-6. Notice that for the helium cases at 500 and 700 K , the calculated

Figure IX.2-5. Measured and calculated gas-phase conductance (h_g) values as a function of temperature (Garnier and Begej, $d = 2.18 \times 10^{-3}$ cm).

Figure IX.2-6. Measured and calculated gas-phase conductance (h_g) values as a function of temperature (Garnier and Begej, $d = 6.05 \times 10^{-4}$ cm).

values of h_g (Kennard) fall within the scattered bands of the experimental data for both the large (Fig. IX.2-5) and the small gaps (Fig. IX.2-6). The Kennard's model for the helium thermal conductivity has been given before, and comparison of that model with the experimental correlation (Gandhi and Saxena), Eq. [IX.2-39] indicated that the two differ by at most 8% between 573 and 773°K. Table IX.2-3 shows a direct comparison between the data and calculations using Eq. [IX.2-47] in which d is replaced by the sum of the interface distance of Garnier and Begej and the temperature jump distances. The agreements are reasonable considering the experimental uncertainties.

TABLE IX.2-3. COMPARISON OF THE MEASURED AND CALCULATED INTERFACE CONDUCTANCES^a

T(K)	$d = 2.18 \times 10^{-5} \text{ m}$				$d = 6.05 \times 10^{-6} \text{ m}$			
	300	470	670	870	300	470	670	870
$h_g^D (\times 10^4)$	0.55	0.70	0.75	0.88	1.4	1.55	1.65	2.0
Uncertainty (+)	0.01	0.02	0.03	0.05	0.05	0.08	0.12	0.17
$h_g^C (\times 10^4)$	0.51	0.68	0.86	1.03	1.06	1.43	1.81	2.16

^a All units of h_g^D and h_g^C are in $\text{W/m}^2\text{°K}$; superscripts D and C denote data and calculation, respectively.

In summary, the design equations for h_g compare favorably with the measured h_g for the $\text{UO}_2/\text{Zr-4}$ system with open gaps. The results also show that when the gap is relatively large compared to the temperature jump distances, the surface effects are minimal and the helium-gas thermal conductivity (k_{He}) and the gap size (d) determine $h_g \simeq k_{\text{He}}/d$, irrespective of the materials of the adjacent surfaces. It is only when d is comparable to the temperature jump distances that the effects of surface materials become important. The implications of this conclusion to the GA PE-II gap conductance experiments are that the experimental focus should be on the small gap situation, with interface pressure variation and with only a few large gap measurements to confirm the above results.

IX.2.3 Solid Breeder Configuration/Confinement

Prior solid-breeder blanket designs have primarily focused on the neutronic performance (i.e., tritium breeding ratio, TBR). Blanket configuration was selected largely to minimize the volume ratio of structure and coolant to solid breeder in order to enhance the TBR. For the STARFIRE and DEMO designs,^(1,7) this resulted in a blanket configuration for which a monolithic block of solid breeder is employed, with channels and holes provided for the passage of coolant tubes and purge gas. For other types of designs, e.g., the GA Li_2O /helium concept (IX.4) and the Grunman Li_2O /helium concept,⁽⁸⁾ either beam- or round-pellet-type solid breeder is enclosed within a metallic cladding. For the purpose of the following discussions, the STARFIRE and DEMO designs will be called breeder outside tube (BOT), and the others, breeder in tube (BIT) where "tube" is interpreted as being the breeder containment structure (not necessarily of tubular geometry).

To illustrate the concerns in the BOT concept, the DEMO $\text{Li}_2\text{O}/\text{H}_2\text{O}$ system is used as an example. This system has received much attention because of its superior tritium breeding performance. One-dimensional neutronic calculation showed that the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ system in the BOT configuration gives the highest TBR without a neutron multiplier. To realize such a performance potential, however, the Li_2O solid breeder must be kept within a narrow temperature range during blanket operation. The lower and the upper temperature limits placed on Li_2O , for example, were chosen so that appreciable tritium diffusion will occur and undesirable materials changes (i.e., mass transport, sintering) are avoided.

Extensive thermal hydraulic analysis⁽¹⁾ has been performed for the DEMO blanket to ensure that the Li_2O temperatures stay within the temperature limits. This is accomplished by a combination of the coolant tube dimensions, tube spacings and coolant flow conditions, under the assumption that the thermal conductance (h_I) at the interface of the Li_2O breeder and the Prime Candidate Alloy (PCA) coolant tube is known and can be controlled during blanket operation. As discussed previously in IX.2.2, however, great difficulty can be expected in controlling h_I due to the uncertainty in the gap size d . Figure IX.2-7 shows schematically the change in d as the DEMO blanket is brought into operation. An initial gap, d_0 , can be defined as the assembled distance between the solid breeder and the coolant tube. This

initial gap is dictated by the clearance/tolerance requirements which may not be uniform along the toroidal direction (3 m length) of the blanket module (i.e., due to the tube out-of-roundness, sagging, etc.). When the blanket is brought to power, the differential thermal expansion between Li_2O and PCA will first widen the gap because the Li_2O expands more than the PCA. (The gap will be reduced somewhat by the elastic deformation of the tube under coolant pressure.) This gap will be significantly reduced, or even closed, when the thermal-stress-induced cracking in Li_2O occurs and the cracked fragments relocate. At later stages of the blanket operation, other time-dependent dimensional changes also take place. The PCA tube will deform by irradiation-enhanced creep as a result of coolant pressure and by irradiation-induced swelling, both in the direction of reducing d . The time-dependent deformation of solid breeder, on the other hand, can occur in either direction. Densification due to sintering tends to increase d by contracting the solid breeder surface, whereas swelling (may be caused by the retained helium-gas bubbles) and creep tend to increase d (or expanding the solid breeder surface). Since each of these deformation processes is driven by temperature and stress in the solid breeder, as well as those in the coolant tube, and since temperatures and stresses are in part determined by h_T for which d has a decisive influence, the problem is of necessity of an iterative nature which requires continuous tracking of the two surface boundaries.

Two additional phenomena can further complicate the situation. One (i.e., gap asymmetry) is illustrated at the bottom of Fig. IX.2-7. The picture on the left shows an ideal situation with a uniform gap around the tube circumference. The picture on the right shows what might happen after breeder cracking and relocation. Notice that the gap is no longer symmetric with respect to the tube axis, i.e., $d=0$ at one side, and $d=2d_0$ at the other side. As a result of these asymmetric gap sizes, significant variations in the heat transfer across the gap and hence in breeder temperatures can be expected.

The second phenomenon involves the transferring of gap from the solid-breeder/tube interface to some other regions in the solid breeder. One such evidence has been observed in the cracking pattern of the Li_2O pellets used in

- THERMAL EXPANSION
- BREEDER CRACKING & RELOCATION
- TIME-DEPENDENT DIMENSIONAL CHANGES
 - CREEP (TUBE)
 - DENSIFICATION/SINTERING (BREEDER)
 - SWELLING (TUBE/BREEDER)
 - ASYMMETRY

Figure IX.2-7. Breeder/tube gap size variation during blanket operation (DEMO BOT).

the FW/B/S PE-II helium purge flow experiment.⁽⁹⁾ In addition to the radial and axial cracks that one would expect from the dominant thermal stress components, slanted cracks deviating away from the radial and axial directions of the pellet have also been seen. Since the predominant heat flow in the solid breeder is in the radial direction, any crack slanting creates gaps which are similar to the gap at the breeder/tube interface. These breeder gaps complicate the heat transfer within the breeding zone. However, they also tend to ease the asymmetry problem at the breeder/tube interface. This is because in the solid breeder, besides the internal porosity and the helium purge flow channels, gaps are the only other type of voidage space. Transferring portions of that gap space into the breeding zone reduces the available gap space at breeder/tube interface, hence reducing the severity of the asymmetry problem. Furthermore, since the gaps in the breeder are at higher temperatures than that at the breeder/tube interface, h_g will be higher because k_{He} increases with temperature (Fig. IX.2-2).

The interface conductance problem in a BOT concept (e.g., STARFIRE or DEMO) has long been recognized as a critical issue on blanket performance. It has also been recognized that if one can adequately control the distance (d) between the solid breeder and coolant tube, then the interface conductance problem is largely solved. Following are several potential methods that have either been proposed or can be considered for controlling the interface conductance; the principles of each will be reviewed.

a. Solid Breeder Segmentation - The principle underlying this approach is to precrack the solid breeder so that the thermal gradient is insufficient to cause further cracking. The method lowers the uncertainty on d due to breeder relocation and reduces the gap asymmetry problem. The method also distributes the gap at breeder/tube interface to other regions in the breeder. Radial and axial breeder segmentations have been proposed, but the assembly and fitting of such pie-shape segments would prove very difficult. Plate- and beam-type solid breeders have also been considered in the coolant panel concept (IX.3). For these, a thickness constraint is required to prevent cracking (IX.2.1).

Breeder segmentation also implies other forms of solid breeder such as a sphere-pac configuration (Sec. VII.6.3). The same principle again applies with one important difference: the interface conductance variation is

potentially much less in a sphere-pac blanket module, and an effective thermal conductivity is probably adequate in determining the solid breeder temperatures for such a system. It is also possible to use high-density ($> 90\%$ TD) solid breeder particles which greatly reduces the problem associated with sintering of the internal porosity. External sintering of sphere-pac particles can still occur in the high temperature region of the breeder and there may be problems of ensuring adequate flow path for the purge stream. In addition, one would expect fabrication difficulties for the hygroscopic Li_2O particles.

b. Solid Material Interface - The principle underlying this approach is to use a solid interface material metallurgically bonded to the solid breeder and PCA. The interface conductance variation is eliminated if the interface bond stays intact during blanket operation, and if the breeder does not crack elsewhere. Braze tests conducted at MDAC⁽¹⁰⁾ (with metallic felt and Ti-Cu-Ag porous brazing material) indicated bond integrity during the cooldown phase; the Li_2O , however, developed cracks both parallel and near perpendicular to the bonded interface. This illustrates that for a temperature gradient across the three materials, PCA, solid material bond, and solid breeder, cracking will still occur when the thermal stresses exceed the fracture strength of the weakest material, i.e., most likely the ceramic solid breeder in this case. This method alone does not appear capable of solving the interface conductance problem for the BOT blanket approach.

c. Dispersion Toughening - The principle underlying the approach is to introduce second-phase materials, such as BeO , to increase the toughness (or reduce brittleness) of the solid breeder. Use of the method is established in ceramic engineering, although the level of toughening achieved may vary considerably for different systems. Much of the effect will depend on the form of the dispersion phase, i.e., as particles or layers, the dispersant strength, and the characteristics of the interface between the matrix and the second phase. Transformation toughening with which the volume change of second phase during transformation introduces compressive stresses in the matrix is another possibility. For the $\text{Al}_2\text{O}_3\text{-ZrO}_2$ system, factors of 4-5

increase in toughness have been demonstrated by the martensitic transformation of ZrO_2 .⁽¹¹⁾

Concerns for the solid breeder configuration/confinement in the BOT concept have been discussed above from phenomenological viewpoints. The narrow temperature window and the low fracture strength of the solid breeder, and the difficulty in controlling the breeder/tube interface conductance, together make it difficult to ensure satisfactory performance of a DEMO-like BOT concept employing Li_2O/H_2O .

Three alternative means for controlling the interface conductance have been discussed, two (segmentation, solid material interface) imply changes in the solid-breeder configuration (i.e., from monolithic block to beam, sphere pac), whereas the third may retain the block configuration, but rely on breeder toughness enhancement (i.e. by dispersion toughening). Among these three alternatives, the sphere-pac configuration appears most attractive and has been chosen as the reference solid breeder configuration for the Li_2O/H_2O system. The beam-type solid breeder is considered in the backup design of the Li_2O/H_2O coolant panel concept.

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IX.3 Li₂O/H₂O Blanket Concept

IX.3.1 Selection of Design Approaches for Evaluation

The selection of the basic approaches to the blanket design and general arrangement was based in large part on the results of a number of recent design studies⁽¹⁻⁴⁾ which examined tokamak power reactor blankets using solid breeders and water coolant in depth. Some of the principal conclusions from those studies that relate to such blanket designs are discussed in Section IX.3.1.1. These conclusions served as the basis for consideration of the many remaining design-related issues that are relevant to selection of the Li₂O/H₂O blanket concept general arrangement. The primary issues among these are discussed briefly in Section IX.3.1.2, and in more detail in other subsections. The three design approaches selected for analysis and evaluation in this study for the Li₂O/H₂O concept are described in Section IX.3.1.3.

IX.3.1.1 Principal Conclusions from Previous Studies

The previous design studies of water-cooled solid breeder blankets in References 1 through 5 were performed for tokamak power reactors, except for the INTOR blanket⁽⁵⁾ which was not required to produce recoverable energy nor to be self-sufficient in tritium breeding. There were a number of conclusions from those studies which were examined in this study and determined to be essentially generic in their applicability to water cooled solid breeder concepts. Those conclusions, discussed below, were therefore adopted as guidelines for the process of selecting the blanket design approaches to be evaluated.

Maintenance - The sector removal and replacement approach should be used instead of in-situ repair or replacement of sector parts (modules). The principal reason is the reduced downtime anticipated for removal of a failed sector and immediate replacement with a new sector. Time-consuming repairs are more easily accomplished in a hot cell, where much remote handling and fabrication equipment is available, with the reactor meanwhile being returned to operation.

Tritium Recovery - The removal of the solid breeder from the blanket for the purpose of recovering the bred tritium is not desirable compared to in-

situ recovery using a low-pressure, low flow rate helium purge gas. Batch recovery schemes result in excessive reactor downtime with reduced availability and high cost of electricity (COE, \$/kWhr). Schemes for recirculation of the solid breeder through the blanket have generally been viewed as very complex and of questionable reliability (see Chapter X).

Water Coolant Conditions - Water coolant for power reactor blankets should be kept at pressure and temperature conditions similar to those for pressurized water reactors, to obtain reasonable thermal energy recovery efficiencies. Values typically proposed are inlet and outlet temperatures of 280°C and 320°C respectively, with a coolant pressure of ~15.2 MPa (2200 psia).

Coolant Containment - The high pressure necessary for the water coolant is a key factor in the choice of coolant containment method. The use of internally-pressurized modules with large-area flat surfaces, or large-diameter tubes in multiple rows, generally results in much larger % volume fractions of both structure and water than does the use of methods with smaller cross-section flow areas such as small-diameter tubes or panels with rectangular-cross-section channels. The sharp reduction in tritium breeding ratio (TBR) associated with the much larger % volumes of structure and water is a severe relative disadvantage for the pressurized module and tube bank approaches.

Coolant Flow Direction - For blanket modules with small-diameter coolant tubes or coolant panels, coolant flow in the poloidal or toroidal directions is preferable to flow in the radial direction (i.e., normal to the plasma scrapeoff surface) from the standpoint of TBR. Radial flow schemes require in general a relatively thick water-filled plenum at or directly behind the first wall, which reduces TBR. Also, the spacing and quantity of tubes or panels must be selected to accommodate the highest nuclear heating rates, which occur at the front of the blanket nearest the first wall (FW). The relatively high % volume fractions of structure and water at this point must continue through the breeding zone to the rear of the blanket, because the combining or branching-away of coolant flows in the interior of the zone is very complex. Thus all but the first few centimeters depthwise of the breeding zone will

have more structure and water than is necessary for heat removal purposes, so that the maximum breeding potential of these regions cannot be attained.

First Wall Integration with Blanket - The first wall and blanket should be fully integrated, both structurally and mechanically. A first wall that is separate from the blanket results in added complexity for the structure, mechanical attachments, and plumbing connections. The first wall coolant should be part of the blanket primary coolant loop. However, there are three possible reasons for changing this conclusion, which are being examined in this study: (1) FW lifetime much less than blanket lifetime (e.g., due to high erosion rates); (2) high surface heat fluxes, requiring lower coolant temperatures to keep FW stresses within limits; or (3) excessive tritium permeation from the plasma through the first wall structure into the coolant, resulting in high costs for detritiation.

IX.3.1.2 Primary Design Considerations in Approach Selection

Table IX.3-1 lists the principal considerations, issues or concerns, relevant to selection of the design approach for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket concept as well as candidate design detail options. Many of these issues were previously addressed in the STARFIRE⁽³⁾, DEMO⁽⁷⁾, and US INTOR⁽⁶⁾ studies. All were reviewed and re-evaluated for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket concept within the context of the study's objectives and guidelines. The information in the table assumes the following first wall/blanket materials: PCA structure; pressurized water coolant; helium purge gas; and Li_2O breeder. The reasons for the importance of the listed issues, and the advantages and disadvantages of the design detail options, are discussed in the remainder of Section IX.3.

IX.3.1.3 Design Approaches Selected for Evaluation

The design considerations and guidelines of most importance in discriminating among possible design approaches for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept were the following:

- o Coolant should be contained within small cross-sectional area flow channels.
- o Structure and coolant volume fractions should be kept low, to maximize tritium breeding ratio.
- o Principal flow direction within the blanket should be toroidal or poloidal.

TABLE IX.3-1. PRINCIPAL DESIGN CONSIDERATIONS FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET CONCEPT

Considerations	Primary Issues or Concerns	Options
First wall configuration	<ul style="list-style-type: none"> o Plasma disruption loads o Reliability against leaks o Thermal-hydraulics 	<ul style="list-style-type: none"> o Coolant containment method: <ul style="list-style-type: none"> - Tubes - Panels - Front wall of large pressurized blanket module
First wall/blanket coupling	<ul style="list-style-type: none"> o Effects on breeding ratio o Maintainability/availability o FW and blanket design life 	<ul style="list-style-type: none"> o Integrated with or separable from blanket <ul style="list-style-type: none"> - Structurally - Mechanically
Breeder temperature control	<ul style="list-style-type: none"> o Predictability of thermal conductance at breeder/structure interface o Predictability of breeder local thermal conductivity 	<ul style="list-style-type: none"> o Helium gap o Controlled-thickness ceramic insulation on structure o Metal felt between breeder and structure o Breeder fabrication: <ul style="list-style-type: none"> - Sintered and machined shapes - Sphere-pac o Breeder containment by structure o Breeder/structure metallurgical bond
Breeder zone coolant containment	<ul style="list-style-type: none"> o Reliability against leaks o Structure and coolant volume minimization o Structure temperature limit o Coolant pressure, ΔT, T_{max} 	<ul style="list-style-type: none"> o Coolant containment in breeder zone: <ul style="list-style-type: none"> - Small-diameter tubes - Panels - Large pressurized modules o Plenum location: <ul style="list-style-type: none"> - Module sides - Rear of blanket o Coolant flow direction
Safety	<ul style="list-style-type: none"> o Effects of breeder/coolant contact in accident o Blanket afterheat removal 	<ul style="list-style-type: none"> o Blanket protection methods: <ul style="list-style-type: none"> - Double walled coolant tubes - Relief (blow-out) plugs - Module walls designed for full coolant pressure o Afterheat removal: <ul style="list-style-type: none"> - Dual parallel coolant circuits - Emergency coolant circuit - Via radiation and conduction only

The basic design approaches that best satisfy these can be classified with respect to the relative position of coolant and breeder (as in the US INTOR study, Ref. 6) as:

- o Breeder outside of coolant tube, BOT
- o Breeder inside tube, BIT

A third approach uses flat panels instead of tubes as the coolant-containing structure, with panels and layers of Li_2O interleaved depthwise through the breeding zone. Figure IX.3-1 illustrates the three approaches.

IX.3.2 Mechanical and Structural Design

The following subsections discuss the design detail options considered for the BIT, BOT and layered approaches to the blanket arrangement. The first wall, neutron multiplier (alternate), breeding, reflector and manifold zones are discussed in separate subsections. Consistent with study guidelines and evaluation criteria (Chapter III), emphasis was placed on identifying those design options that could best be combined into a power reactor first-wall/blanket design. The major goals were safety, environmental and economical attractiveness, and low risk in terms of eventual satisfaction of design requirements.

The most important factors in evaluating and comparing the design detail options for each approach were found to be tritium breeding, breeder temperature control, breeder fabrication, breeder containment, and blanket fabrication.

The most important design requirements and considerations differ significantly among the basic zones of the first-wall/blanket system. The first wall must withstand high surface heat fluxes with resultant high temperature gradients and thermal and mechanical stresses. The first wall must also withstand a limited number of plasma disruptions. The amount of water coolant and structure must be minimized to maximize blanket tritium breeding ratios. The solid neutron multiplier (if used) must be cooled and must be structurally supported. The solid breeder must be cooled, yet kept within specific temperature limits to assure stable physical properties and acceptably low tritium inventories at steady-state conditions. Tritium must be removed from the blanket on a continuous or periodic basis without requiring removal of the breeder.

BIT (BREEDER-IN-TUBE) BOT (BREEDER-OUT-OF-TUBE) CYLINDRICAL CELL RECTANGULAR CELLLAYERED (COOLANT PANELS)

Figure IX.3-1. Blanket design approaches selected for evaluation.

IX.3.2.1 First Wall

There are two initial concerns in the first wall design for power reactors. The first, applicable principally to tokamaks, is removal of first wall surface material through erosion during normal operation and from ablation or vaporization during plasma disruptions. These phenomena could result in a relatively short first wall design life due either to excessive material removal rates or excessively high thermal strains in a thick wall. The second, applicable to tandem mirrors and tokamaks, is radiation damage to the structure. Radiation creep, swelling, and loss of ductility are the primary concerns. The interaction of these phenomena with each other and with non-irradiation phenomena such as thermal expansion could also result in reduced design life due to excessive deformations or combined stresses.

Design life for first walls in the study, on the basis of erosion only, has been calculated using the study guideline of a nominal 1-mm/yr erosion rate. Radiation damage effects for first wall designs for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept will be addressed during the second year of the study.

The following paragraphs discuss first wall design concepts considered for this blanket concept. Only bare first wall plasma-side surfaces have been assumed (i.e., no coatings or armor). The three basic mechanical design concepts studied were tubes, ribbed panels and embossed panels. These concepts are illustrated in Fig. IX.3-2. The first wall was assumed to be water-cooled and constructed of PCA austenitic stainless steel.

Tubes - A first wall design with free standing tubes, i.e., not mounted to each other or to supporting structure across the face of the first wall, results in a minimum of structural material (excluding any erosion allowance), which enhances tritium breeding. However, the tube concept can result in a relatively large equivalent volume of water coolant in the first wall zone, which will significantly degrade the tritium breeding, especially if a neutron multiplier is used.

For a single bank of contiguous tubes, it is necessary to support the tubes at discrete points along the first wall, so that motions due to thermal strains, flow-induced vibrations, plasma disruptions and seismic events would not result in excessive strains or chafing of adjacent tubes. Erosion of tube surfaces is expected to be non-uniform, and could open up gaps between tubes permitting neutron streaming and erosion of surfaces behind the first wall.

Figure IX,3-2. First wall design concepts.

The use of two staggered banks of tubes with significant lateral and vertical spaces between adjacent tubes would alleviate these problems but would add more steel and coolant, further degrading tritium breeding. Supporting the tubes results in added thermal strain problems for both the tubes and supports. The supports must be attached to the blanket front face and to the individual tubes. Adequate cooling of the tube supports is also a problem. Welding of the tubes to manifolds also presents considerable fabrication difficulties.

Ribbed Panels - In this concept, first wall coolant flows through channels of square or rectangular cross-section. The panel is formed by joining a front and rear wall with ribs which separate adjacent channels. Major advantages of the ribbed panel concept compared to the tubes relate to simpler support, reduced concerns for vibration and fretting, and the possibility of using the first wall panel as an integral part of the blanket.

A ribbed panel with square channels has thermal-hydraulic and heat transfer characteristics similar to those for tubes of the same hydraulic diameter, but contains more structure than the tubes due to its less efficient structural shape for containing pressure. However, if the channels are designed with rectangular channels, the equivalent thickness of coolant present in the first wall can be reduced. This is important since the moderating properties of water are detrimental to tritium breeding with solid breeders.

Selection of a fabrication method for the panels involves a number of considerations (Table IX.3-2). The most important of these are costs, reliability against leaks into the breeder, and retention of the cold-worked (CW) properties of the parent metal desirable for reduced radiation swelling and higher strength allowables. Some form of resistance welding appears overall to be the best method. Brazing would likely be the preferred option except that the viability of the commonly-used braze alloys in a fusion environment is not known at this time.

Both thermal-hydraulic and thermo-structural analyses have been performed to establish the viability of the ribbed panel approach, for use as a first wall and for cooling of the breeding zone (see Sections IX.3.3 and IX.3.4). However, the effects of radiation on its performance remain to be studied.

TABLE IX.3-2. COOLANT PANEL FABRICATION

o Principal Design Considerations

- Minimize weld zone to retain CW properties
- Avoid high-temperature methods (e.g., diffusion bonding)
- Avoid fusion welding (distortion)
- Strive for multiple barriers between coolant and breeder

o Resistance Weld Method Appears Best Overall

- Adaptable to high-speed production
- Minimum heat-affected zone; weld properties $\approx$ cold-worked in regard to radiation damage resistance
- Two-step welding could provide multiple barriers against leaking:
 - (1) Seam weld two skins, both sides of panel
 - (2) Weld ribs to inside of lower skins

(1)

(2)

o Brazing is an Alternate Method

- Viability of braze alloys in fusion radiation environment is unknown at this time

Embossed Panels - The coolant channels for this panel concept are formed by joining two stainless steel sheets, one or both of which have been embossed or corrugated to form a channel shape. The sheets are joined between adjacent channels by a continuous resistance weld or overlapping spot welds accomplished by passing two rollers along the outsides of the two-sheet assembly while flowing an electric current between the rollers through the sheets. The embossed shapes in each sheet can be semi-circular, semi-elliptical or trapezoidal.

The embossed panel concept has several significant advantages. First, it minimizes the equivalent volume of water coolant required in the first wall compared to the other concepts. Second, the resistance weld method is believed to cause minimal degradation of the structural material's radiation damage resistance. Any separation or cracking of welds between channels results only in a minute passage of coolant between adjacent channels. This is not significant since the flow is in the same direction for all first wall channels. The probability of a leak through a front or rear sheet to the plasma region or to the multiplier region is considered to be low.

Although the embossed panel concept was the reference first wall design for STARFIRE⁽³⁾ and DEMO⁽⁷⁾, there are major concerns at present regarding its attractiveness with high-pressure water coolant. The extrapolations of burst test results for actual corrugated panels (Sec. IX.3.4.2) indicates that, for water coolant at 15.2 MPa at the required channel flow areas, the total skin thicknesses necessary would be far above those previously assumed. These skin thicknesses (~2.5 cm total) could far exceed those necessary for the ribbed panel concept, and probably are well beyond the maximum thickness for which channel shapes could be die-stamped into the skins. Thus the embossed panel concept, although attractive in many respects, may be restricted to lower-pressure coolants (e.g., molten salts or liquid metals) in power reactors.

IX.3.2.2 Mechanical and Structural Integration of First Wall and Blanket

In the more recent design studies of power reactor blankets with solid breeders and water coolant, the first wall and blanket have been fully integrated both structurally and mechanically. There are, however, several possible reasons for adopting a separate first wall (i.e., one that can be removed from the blanket and then replaced). These reasons are listed and

discussed in Table IX.3-3. The reasons most relevant to the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept relate to first wall erosion, and to the desire to extend blanket sector life and/or first wall life by permitting change-out of the first wall only or by enabling an increase in the allowable thickness of erodable material.

For a tandem mirror reactor first wall, if erosion rates and surface heat fluxes are both very low, then the only reason for making the first wall separable would be reduced first wall life (compared to the blanket) due to radiation damage to the structure. However, this situation is considered unlikely since the first few centimeters of the blanket structure will experience roughly the same radiation damage as the first wall. Thus the first wall and blanket for this concept should be integral if used in a tandem mirror reactor.

The basic concepts for integral and separable first walls are shown schematically in Fig. IX.3-3. Several of the basic assumptions for the comparison are also listed. The added complexity of the separable first wall approach is apparent from the figure. Various design aspects of the two approaches are compared in Table IX.3-4, based on a STARFIRE-size tokamak.

The preliminary conclusion for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept is that the integral first wall is preferable to the separate first wall. A quantitative economics comparison of the relative effects of separate and integral first walls has not been performed, but will be if and when one or more of the principal incentives for a separate first wall (Table IX.3-3) is considered likely to exist. The comparison would be performed in terms of COE, with the primary factors being spares costs, changeout times and frequency, availability, and reactor capital costs as affected (for example) by differences in blanket thickness needed to achieve a specific breeding ratio.

IX.3.2.3 Neutron Multiplier Zone

The use of a neutron multiplier to improve tritium breeding has been considered for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept. Solid beryllium is the only multiplier material considered thus far; the use of lead as a multiplier in concepts with solid breeders and water coolant has been examined elsewhere (Section IX.5). At this time the addition of the beryllium neutron multiplier to the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept is considered to be optional. A blanket with no neutron multiplier is in general the most desirable from a number of standpoints, including relia-

TABLE IX.3-3. POSSIBLE REASONS FOR SEPARATE FIRST WALL

<u>Condition</u>	<u>Comments</u>
o First wall design life << blanket design life	o Erosion or radiation damage to first wall may cause this o Separable first wall could reduce spares costs if blanket is re-used
o High mechanical and/or thermal stresses in first wall	o Lower-temperature and/or lower-pressure coolant could reduce stresses and extend first wall fatigue or creep life
o High first wall erosion rates	o Lower-temperature coolant could increase allowable thermal gradient through erodable material, thus increasing allowable thickness

TABLE IX.3-4. DESIGN COMPARISON OF SEPARATE AND INTEGRAL FIRST WALL APPROACHES

CONSIDERATION	INTEGRAL	SEPARATE
BREEDING RATIO ^b (1-D, FULL COVERAGE)	~ 1.20 - 1.25 ?	ADDED PANEL TO COOL FRONT OF BLANKET WILL REDUCE BREEDING RATIO BY ~ .05 - .15
THERMAL STRESS	REDUCED BY BACKSIDE HEATING FROM BREEDER OR MULTIPLIER	---
REPLACEMENT	WITH BLANKET	INDIVIDUAL LARGE PANELS
RELIABILITY	IMPROVED THROUGH FEWER PIECES AND FEWER PRESSURE BOUNDARY WELDS	---
FABRICATION	SIMPLER -- FEWER PARTS AND WELDS	---
FIRST WALL SUPPORT METHOD	WELDED	MECHANICAL ATTACHMENTS
PRIMARY CONCERNS	REPLACEMENT COSTS	REDUCED BREEDING → HIGHER COSTS; INCREASED COMPLEXITY; FIRST WALL SUPPORTS

^aFOR STARFIRE. DOES NOT INCLUDE FW EXTENSIONS DOWN THE MODULE SIDES.

^bASSUMES $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLKT; NO EROSION THICKNESS ALLOWANCE.

ASSUMPTIONS

- o "SEPARATE" IMPLIES FW CAN BE DETACHED BOTH STRUCTURALLY AND MECHANICALLY FROM BLANKET AND REATTACHED
- o FW DESIGN IS MODULAR (MULTIPLE PIECES IN A SECTOR); BLANKET MAY OR MAY NOT BE MODULAR
- o MAINTENANCE APPROACH: REPLACE FW/B SECTOR IMMEDIATELY WITH SPARE SECTOR, REMOVE AND REPLACE FW LATER IN HOT CELL
 - DOWNTIME COSTS ~ 1 \$M PER DAY, NON-RECOVERABLE
 - ALTERNATIVES:
 - o REMOVE AND REPLACE FW WHILE SECTOR STANDS NEXT TO REACTOR
 - o PERFORM REPAIRS IN HOT CELL WHILE REACTOR IS SHUT DOWN

Figure IX.3-3. Comparison of separate and integral first walls.

bility, safety, costs, and resources. However, if neutronics analyses indicate that the tritium breeding ratio for the concept is too low (based on the evaluation of the concept, Chapter III) then beryllium will be incorporated.

Design considerations for various locations of the neutron multiplier within the blanket were discussed in the STARFIRE report.⁽³⁾ Those discussions are briefly summarized below, following a summary of design guidelines for the use of beryllium as a neutron multiplier.

Design Guidelines for Beryllium - The evaluation of beryllium (Section V.2) for use as a neutron multiplier resulted in several key findings relevant to design. These are summarized in Table IX.3-5 together with the more important design guidelines for its use in slab or plate form within the blanket. Overall, the addition of the beryllium is judged to moderately increase the blanket's complexity. Other aspects of the beryllium, i.e. toxicity, resource questions and radiation effects, are discussed in Sections V.2 and XI.3.

Homogeneous Breeder and Multiplier - In theory, a solid breeder and a neutron multiplier can be homogeneously mixed together (Fig. IX.3-4). For this approach to be feasible, the materials must be compatible both for normal operation and for accident situations. Thus, BeO may be required instead of Be; this would reduce the potential improvement in breeding. The multiplier must also be compatible with the bred tritium. Design complexity is similar to that for the approach with no multiplier. However, thermal-hydraulic characteristics will differ because of the mix of different materials properties, e.g., thermal conductivities.

Multiplier in Front of Breeder - In this option, the multiplier is separated from the breeder for reasons of compatibility. This increases design complexity because the multiplier must be cooled separately from the breeder. Since the multiplier is in a discrete region, it must be structurally supported as a separate entity from the breeder. The first wall can be used to remove part of the multiplier's nuclear heat. Tritium breeding ratio can be changed by varying multiplier and breeder zone thicknesses and by varying the ^6Li enrichment level in the breeder.

Locating the multiplier in any discrete zone presents an added complication to the design, not only because of cooling requirements, but

TABLE IX.3-5. DESIGN CONSIDERATIONS FOR BERYLLIUM NEUTRON MULTIPLIER

Key Results from Beryllium Evaluation (Sec. V.2)

- o Be exhibits significant swelling (He) under irradiation.
- o Swelling of Be can be accommodated for nonstructural applications where a containment/cladding is used. Some initial porosity may be beneficial for accommodating the swelling.
- o Thermal conductivity of Be is quite high even when degraded by swelling or porosity. Classical equations can be used to predict effects of swelling/porosity on thermal conductivity.
- o 800°C maximum temperature limit based on evaporation consideration.

Design Guidelines for Beryllium (Plate Form)

<u>Guideline</u>	<u>Reason</u>
o Non-structural	o Low physical integrity at anticipated fluence levels
o Cool from both faces	o Simpler fabrication; higher allowable thickness
o Seal off from breeder	o Acts as sink for tritium
o Good thermal conductance at interface with structure	o Increases useable range of temperatures (up to 800°C) and thus increases allowable thickness
o Vent generated helium	o Prevent pressure buildup in multiplier compartment

Figure IX.3-4. Possible arrangements of solid breeder and neutron multiplier.

because of the magnetic forces induced during plasma disruptions. These forces act uniformly over the multiplier and tend to pull it toward the plasma chamber. The first-wall/blanket structure must also support the multiplier mass and react all seismic and gravity loads.

Multiplier Layers in Breeder Zone - This option uses one or more layers of multiplier, with thicknesses and depths chosen to maximize tritium breeding ratio. Assuming the multiplier layer faces are cooled by coolant panels, the approach (for a single multiplier layer) is only slightly more complex than having the multiplier directly behind the first wall, due to the presence of an added panel. Each added layer requires two additional panels. Neutronics analyses indicate (Sec. IX.3.5.3) that use of a single layer of beryllium immediately behind the first 4-5 cm of Li_2O results in the maximum tritium breeding ratio for the $\text{Li}_2\text{O}/\text{H}_2\text{O}/\text{Be}$ concept.

This option and the homogeneous breeder/multiplier mixture option will be evaluated further to determine which of the three approaches is best overall, should a neutron multiplier be needed for this concept.

IX.3.2.4 Tritium Breeder Zone

The more recent studies^(2,3,6,7) of blankets using solid breeder and water coolant each selected the BOT approach for the breeder zone. Some sintered form of breeder was selected in each case, with the breeder-to-tube interface considered to be a helium gap, metallurgical bond, or ceramic insulator. However, the preliminary conclusions in this study (Section IX.2) that cracking of the sintered Li_2O breeder is highly probable under the anticipated thermal gradients, has caused a reconsideration of the approaches to (1) blanket arrangement, (2) breeder-to-tube interface, and (3) breeder fabrication method. The breeder zone configuration choice depends strongly on the results in all three areas.

In the following paragraphs, the importance of the breeder-to-structure interface conditions in assuring acceptable blanket performance is briefly discussed, followed by a discussion of the anticipated performance of the sintered product and sphere-pac approaches to breeder fabrication. The BOT, BIT and layered blanket approaches are then discussed, with emphasis on the effects of the two breeder fabrication methods on the breeder-to-structure

interface, as well as mechanical and structural design considerations for each of the three blanket design approaches.

Breeder-to-Structure Interface - The degree of predictability of the heat conductance, h_g , across the breeder-to-structure (B/S) interface is of major importance in determining the range of breeder temperatures allowable for the design under normal operating conditions. If h_g is invariant and is known exactly at all points, then the full range (410°C to 800°C) of allowable Li_2O temperatures (see Section VIII.1) may be utilized for design conditions. However, any uncertainty—plus or minus—in the nominal h_g value must be accounted for by increasing the design minimum and decreasing the design maximum breeder temperature by some amount, so that the breeder does not operate outside the allowable temperature range regardless of the true h_g value. As an example, for the DEMO $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket⁽⁷⁾ an uncertainty of only $\sim \pm 20\%$ in h_g results in an increase and decrease of $\sim 30^\circ\text{C}$ in breeder T_{max} and T_{min} respectively. The breeder's design temperature range would thus have to be reduced by $\sim 60^\circ\text{C}$ (about 15%) just to accommodate such an uncertainty range.

Methods to accomplish the B/S interface were examined. The use of a discrete helium-filled gap (> 0.1 mm) is considered not feasible for power reactor conditions. The extreme sensitivity of h_g --and thus breeder temperatures--to very small changes in gap width (see Sec. IX.2) results in gap tolerance requirements that are unacceptable even for blanket fabrication, let alone achievable during long-term blanket operation. The B/S metallurgical bond (brazed) suggested for the DEMO blanket⁽⁷⁾ is attractive, and conceptually feasible (see Fig. IX.3-5). However, tests at McDonnell Douglas indicate that the fracture strength of samples of sintered Li_2O ($\sim 80\%$ dense), produced using current fabrication techniques, is too low to prevent fracture of the breeder at its interface with the metallic felt. Techniques to significantly increase Li_2O fracture strength, such as the addition of chopped fibers, need to be demonstrated before this interface method can be utilized. A third method is to place the breeder in direct contact with the structure (or some intermediary insulator such as a ceramic or metallic felt) and to maintain some amount of compressive load at the interface. This method appears to yield a reasonable degree of predictability in h_g (Sec. IX.2); the key factor is the method or process by which the interface compression is maintained.

Figure IX.3-5. Solid breeder brazed to structure via metallic felt.

Breeder Fabrication - The sphere-pac and sintered product methods for fabricating Li_2O were described in Sec. VIII.5, and the design implications of expected sintered breeder cracking and of B/S relative motion normal to their interface were discussed in Sec. IX.2.

Cracking of the solid breeder can affect blanket performance through two principal mechanisms. First, the creation of crack surfaces normal to the direction of heat flow produces additional impedances to heat flow. A temperature gradient, ΔT_g , will exist across each such crack, the magnitude of which is primarily a function of the crack interface pressure or the crack gap width, and the helium purge gas pressure (if present). Each ΔT will raise the breeder maximum temperature by approximately that same magnitude. It is not possible at present to predict the number, orientation, and characteristics of such cracks in a given design. Second, if breeder fragments are allowed to shift their positions slightly relative to the surrounding breeder, the ΔT_g values will become far higher due to the opening up of much larger gaps at the irregular surfaces of the cracks. The danger also exists of continued motion and ratchetting of the fragments, and a progressive breaking-up of the breeder.

The real potential for such cracking, and its effects on blanket performance for specific design approaches, needs to be examined through out-of-reactor tests with candidate solid breeders. Lacking the results of such tests, the most prudent design approach at present for using sintered product would seem to be (1) to maintain relatively high B/S interface pressures, and (2) to contain the breeder in some manner. Methods for satisfying these requirements are discussed in the following paragraphs for all three blanket approaches.

The sphere-pac breeder fabrication approach appears to offer a way to preclude breeder cracking or relative motion (shifting) while retaining the same overall porosity percentage and thermal conductivity (k_T) achievable with the sintered product. Briefly, the sphere-pac approach uses a specific mixture of three different sizes of ~100% dense breeder spheres. The mixture can be literally poured into a cavity, with the desired %TD and k_T resulting with little additional work.

Three key factors involved with sphere-pac Li_2O are expected to relieve the initial problems of cracking and of interface predictability. First, if

the 85% dense breeder is kept below 800°C, sintering among pellets is not expected to occur, and therefore macroscopic cracking of the bulk breeder cannot occur. Second, the Li_2O is expected to expand with temperature at a rate considerable faster than the steel structure. Coupled with the radiation-induced bulk swelling expected for the Li_2O (Sec. VIII.4), this means that the breeder, if properly confined, can be held in contact with the structure at all times. Third, the mixture is expected to remain "loose" (with 15% porosity and no sintering) to accommodate the movements of the structure. In combination with the higher thermal expansion of the Li_2O , this is expected to keep reasonable B/S interface contact pressure at all times during blanket operation. The blanket design must of course be properly configured to achieve this, as discussed below.

Breeder Containment - Methods to contain the solid breeder, in sintered or sphere-pac form, were examined (see Fig. IX.3-6). The methods are in general applicable to all three blanket approaches. The best method overall appears to be the use of structure or clad. If desirable, a woven mesh (felt), ceramic coating or perforated clad could be used between the structure and breeder to provide a path for the helium purge gas.

BIT Approach - For the breeder-in-tube approach as defined here, two concentric tubes are used for each element of the blanket with solid breeder contained inside the inner tube and water coolant confined to the annulus between tubes (see Fig. IX.3-7). These elements would be arrayed within the blanket, oriented poloidally or toroidally, with void regions among adjacent tubes.

The principal advantages and disadvantages of this approach are listed in Fig. IX.3-7. The principal advantage is the confinement of the breeder offered by the inner tube. The B/S interface should remain in compression, which is desirable as previously discussed. A small-diameter central tube would likely be necessary to hold open the purge gas flow channel at the breeder cylinder's center, for either breeder fabrication method. Design and fabrication complexity are significant disadvantages. The biggest disadvantage, however, results from the use of thick-walled steel tubes to contain the high-pressure water coolant. Sizing the tubes to meet ASME requirements (Sec. IX.3.4.6) results in ~40-50% volume fractions of steel. This amount of steel, coupled with the flux depression effect associated with

o CERAMIC COATING

- SAME PHYSICAL INTEGRITY PROBLEMS AT BREEDER

- TRITIUM PERMEATES THROUGH COATING TO PURGE STREAM

o SOLID CLADDING OR STRUCTURE

- TWO HELIUM PURGE SYSTEM CONNECTIONS REQUIRED PER BREEDER CELL

o PERFORATED CLADDING

- HELIUM PERMEATION THROUGH PINHOLES; SINGLE CONNECTION PER BREEDER CELL MAY BE REQUIRED

o WOVEN OR SINTERED MESH

- POTENTIAL FOR BRAZE ATTACHMENT TO STRUCTURE

- TRITIUM PERMEATES THROUGH MESH TO PURGE STREAM

Figure IX.3-6. Solid breeder confinement options.

OPTIONS FOR ELEMENT ENDS

ADVANTAGES

- o COOLANT CONTAINED IN SEAMLESS TUBES
- o BREEDER CONTAINMENT PROVIDED BY INNER TUBE

DISADVANTAGES

- o TBR SIGNIFICANTLY REDUCED COMPARED TO BOT
- MORE VOIDS, MORE TUBES, LARGER TUBE DIAMETERS RESULT IN HIGHER % V STEEL AND WATER
- FLUX DEPRESSION EFFECT ACROSS BREEDER CYLINDER
- o HIGH LI BURNUP RATES NEAR OUTSIDE OF BREEDER CYLINDER
- o COOLANT PLENUMS REQUIRED TO BE LOCATED AT MODULE ENDS
- o ADDITION OF INTERNAL FRAMES CAUSES FURTHER VOIDS

Figure IX.3-7. Breeder-in-tube (BIT) approach.

this approach, severely degrades tritium breeding (see Sec. IX.3.5.2) to the point that tritium breeding ratio is well below unity. Net breeding is not achievable, unless perhaps a neutron multiplier is added to the blanket.

BOT Approach - This basic approach was adopted for the STARFIRE⁽³⁾, US INTOR⁽⁶⁾, and DEMO⁽⁷⁾ solid breeder/water coolant designs. In the BOT approach the water coolant is contained in small-diameter tubes which are surrounded by breeder (Fig. IX.3-8). The tubes can terminate in plenums at the module side walls (as in US INTOR), or they may be formed into a U-shape and terminate in the manifold region behind the module back wall (STARFIRE, DEMO). Both options are possible for toroidal or poloidal tube orientations.

Design options for breeder containment are expected to differ for the two breeder fabrication approaches. For sintered product, some type of jacket or containment structure is considered to be necessary, in order to minimize ΔT_g values at the breeder cracks and to keep all breeder segments/fragments in compressive contact with the coolant tubes. For sphere-pac breeder, the cracking concern does not exist, and the confinement offered by the module structure--front wall, back wall, end walls, and side walls or internal actively cooled frames--is considered adequate to retain the breeder. This confinement method is also expected to maintain compressive contact at the B/S interface, given the breeder swelling and thermal expansion characteristics previously mentioned.

A conceptual method for providing a helium purge gas path for either sintered or sphere-pac breeder would be a relatively low-density metal (stainless steel) felt, assembled as a continuous sleeve around each coolant tube before the ends are welded. Locating the purge gas path between the water coolant tube and the breeder is logical for two reasons: (1) minimum transport of LiOH vapor will occur if the purge channel is at the coldest part of the breeder; (2) a temperature increase of ~70 to 110°C needs to be maintained in any event between the structure surface and the coldest breeder surface, to keep the breeder temperature at or above 410°C.

Blanket fabrication is relatively simple for the sphere-pac breeder, mainly because individual jackets to confine the breeder around each tube are not required. The breeder can be added from one end of the module, filling all voids, and the fill opening sealed. The jackets required for the sintered product present an added complication, especially if U-bend tubes are used.

ADVANTAGES

- o COOLANT CONTAINED IN SMALL-DIAMETER THIN-WALLED TUBES--LOW STRUCTURE FRACTION
- o HIGHER AVERAGE TEMPERATURE THAN FOR BIT, THEREFORE LOWER TRITIUM INVENTORY

DISADVANTAGES

- o LARGE NUMBER OF TUBES AND TUBE END WELDS LEAD TO RELIABILITY CONCERNS
- o INEFFICIENT USE OF SPACE FOR BREEDER
 - VOIDS AMONG CYLINDERS OR RECTANGLES AT TIGHTER SPACING
 - LOWER % V BREEDER THAN ALLOWABLE WHENEVER TUBES ARE ORIENTED NORMAL TO FW
- o BREEDER/STRUCTURE α -DIFFERENCE LEADS TO PROBLEMS
 - CONTAINMENT IS AT HIGHEST BREEDER TEMPERATURE
 - EXPANDING BREEDER MAY YIELD CONTAINMENT, RENDERING IT INEFFECTIVE

Figure IX.3-8. Breeder-out-of-tube (BOT) approach.

The elements—each consisting of the jacket, many breeder segments, and the coolant tube—will have to be individually fabricated, then assembled together in stacks into the blanket, with welding of the tube ends accomplished as the near-final step for the breeder zone.

Layered Approach - In this approach, the water coolant is contained in a ribbed panel (very similar to that considered for the first wall, Sec. IX.3.2.1) with flow channels of rectangular cross section (Fig. IX.3.-9). The panels extend the width of the module toroidally, and terminate at the end walls either in plenums or a self-contained manifold which is in turn piped to the blanket manifold region.

Starting immediately behind the first walls, layers of breeder are interleaved with the panels. Each breeder layer is sized according to the nuclear heating rate in its region (Sec. IX.3.3.1), and each is cooled from both faces by the adjoining panels. Panels terminate in the poloidal direction at the internal frames which connect the front and rear walls, or at the actively cooled module side walls. Helium purge gas flow is (preferably) directed toroidally along the B/S interface, the coldest part of the breeder layer, through grooves in the sintered breeder or through a metal felt which forms the breeder/structure interface. The purge flow enters and exits the breeding zone through the rear wall at points close to the module end walls.

The confinement for the sintered breeder provided by the panels may be adequate without any additional confinement structure. However, the addition of a metal felt or perforated clad to completely surround each breeder layer should assure adequate confinement, and could also provide (as for the BOT approach) both the necessary purge gas flow path and the thermal resistance required to cause the 70°-110°C temperature rise from the structure surface to the breeder surface. The panels and module walls combined are expected to provide adequate containment for the sphere-pac breeder. Purge gas flow paths would be provided as above except that the felt could be attached directly to the panels prior to module assembly.

A number of complex design areas remain to be worked out; these principally involve the mechanical/structural connections between the end plenums and the coolant panels, including the first wall (Fig. IX.3-10). The one-piece omega-cross-section seal is commonly used at openings in nuclear pressure vessels to accommodate relative thermal and mechanical deflections

ADVANTAGES

- o MOST EFFICIENT USE OF BREEDER
- o PANELS IDENTICAL EXCEPT FOR ORIFICING

DISADVANTAGES

- o END PLENUMS/MANIFOLDS REDUCE TBR
- o RELIABILITY CONCERNS FOR FLEXIBLE SEALS

PROBLEM AREAS TO BE WORKED OUT

- o PANELS SERVE AS BREEDER CONTAINMENT?
- o PLENUM/PANEL ATTACHMENTS
- o INTERNAL FRAME REQUIREMENTS-- ACTIVE COOLING?
- o PLENUM SIZING

Figure IX.3-9. Layered approach.

Figure IX.3-10. Detail design options for layered approach.

while serving as a boundary for high-pressure water coolant. This and other design detail options for such areas will be studied. The most important requirements for these areas--especially for any connections to the first wall--will likely be (1) the accommodation of predicted irradiation effects, primarily creep and swelling, and (2) reliability against leaks.

Fabrication of coolant panels for the breeding zone would be very similar to that for the first wall panel (Sec. IX.3.2.1). Blanket assembly for sintered breeder might involve sequential stackup of panels and layers of breeder segments, with each panel welded into place at the plenums and at any other connections before the next breeder layer is added. An alternate method would be to first incorporate each breeder layer as a subassembly with containment structure, for example a relatively stiff metal felt or a perforated clad. Fabrication using sphere-pac breeder would be similar to that for the BOT approach, except that the helium purge path (e.g., metal felt) would be added to the panel's outside faces before adding the breeder to the module cavity.

IX.3.2.5 Reflector and Manifold Zone(s)

Most of the previous solid breeder/water cooled blanket designs incorporated a neutron reflector region behind the breeder zone, to enhance tritium breeding without adding more breeder to the blanket. The manifold region is located at the rear of the blanket assembly. Since the water and steel in the manifolds are also fairly good reflector materials, the reflector and manifold regions can logically be combined into a single zone as in STARFIRE.⁽³⁾ The resulting zone thickness is typically less than the combined thickness of two separate zones.

The primary design considerations for the reflector and manifold are listed in Table IX.3-6. The design of the reflector/manifold zone for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept has not been emphasized to this point in the study, principally because it is considered less important to the overall blanket than the design of the other blanket zones. Its main impact is expected to be on tokamak inboard wall/blanket/shield combined thickness ($\Delta_{\text{WBS}}^{\text{i}}$). Thicknesses for the outboard $\Delta_{\text{WBS}}^{\text{o}}$ are not as critical since more room generally exists for these components within the TF coil outside leg. This area will be examined further as the design for the blanket concept is developed.

TABLE IX.3-6. DESIGN CONSIDERATIONS FOR REFLECTOR AND MANIFOLD REGIONS

Consideration	Comment
<u>MANIFOLD</u>	
o Flow velocity	o Size for 6 m/s maximum, higher only for long uniform pipe runs
o Inboard blanket	o Minimize manifold thickness, to keep Δ_{WBS}^i as low as possible
o No. of separate coolant loops	o Use two separate loops if possible, for reliable removal of afterheat
o Reliability against leaks resulting in shutdown	o Use sealed, vacuum-pumped jacket around manifolds within sector
	o Use dual welds where possible to form two independent barriers
<u>REFLECTOR</u>	
o Materials	o Graphite, water, steel are leading candidates
o Inboard blanket	o Minimize reflector thickness to keep Δ_{WBS}^i as low as possible
o Cooling	o $\lesssim$ 5% volume water, steel required for heat removal
o Integration with manifold zone	o Generally attractive from overall reactor, blanket design standpoints

Reflector - The reflector serves to reflect low-energy neutrons back into the breeder which results in a slight gain in tritium breeding ratio. Graphite and a water/steel combination are the most attractive materials from the standpoints of materials compatibility, fabricability, costs, and design integration. Graphite is highly stable, low in cost, and requires relatively small thicknesses (~ 15 cm) for adequate performance. It can be machined or molded into desired configurations and can be bonded to itself. The graphite and the metal structure surrounding it must be cooled to maintain acceptable structural temperatures. For a given thickness, a water/steel combination is generally the neutronic equivalent of graphite as a reflector. A reflector could be designed which consists of a steel panel with internal chambers filled with slowly-circulating water. The water coolant in the manifold tubes (and headers if present) at the rear of the blanket could also serve as part of the reflector. Bringing the header/manifold system into the reflector zone and removing graphite from that part of the region, is an attractive option from the standpoint of overall blanket design simplicity, as discussed below.

Manifolds - The most importance considerations in designing the manifold region are (1) reliability against leaks causing reactor shutdowns, (2) providing dual coolant circuits if possible, and (3) the degree of integration of reflector and manifold zones. The three basic design approaches for the manifolds are shown schematically in Fig. IX.3-11 for the BOT blanket approach. The three options would differ somewhat for the layered (panel) blanket approaches since the manifolds would be connected to the end plenums of each blanket module.

In the tubular manifold option, the breeder coolant tubes are welded directly to stubs which extend from the inlet and outlet manifold tubes. The manifold tubes extend poloidally along the full length of each module. Flexibility in the manifold lines can be provided by bellows welded into the manifold at or near the welded joints which connect the manifolds at the intersections of adjacent modules. For a STARFIRE-size reactor with ten blanket sectors, the coolant volume flow rate and the 6 m/s flow velocity limit together make it desirable to have three or more inlet and outlet manifold tubes, in order to keep manifold tube diameters ≤ 30 cm and to reduce total blanket thickness.

Figure IX.3-11. Manifold options for BOT approach.

For the second option, the breeder coolant tubes are welded to stubs extending from headers which are in turn connected by tubes to the manifolds. The addition of headers may offer advantages, the principal one being a reduced concern over possible thermal strain problems between coolant tubes and manifolds. This option has the disadvantage of requiring additional volume for the coolant system, which is taken either from the breeder zone or from the reactor vacuum plenum between the manifold zone and the shield.

The third option, rectangular channel manifolds, completely integrates the reflector and manifold zones. The manifold coolant flow is contained in contiguous channels of rectangular cross section which span the width of the blanket sector. The channels are formed by ribs connecting the flat plate at the rear of the breeder zone to a similar plate at the rear of the manifold zone. The concept offers significant advantages in simplification of the blanket mechanical design. It eliminates any need for graphite and the associated cooling requirements. The concept provides an inherently strong "backbone" for individual modules, and when integrated with sector support frames results in a strong blanket sector structure with minimum deflections from mechanical loads. It may also be feasible to let this manifold serve as the blanket sector support structure, by structurally joining all manifold walls and internal ribs between adjacent modules, making the flow channels and pressure boundaries continuous around the sector. (This would be similar to the structural support provided by the manifold region of the self-cooled liquid lithium blanket design and the helium cooled liquid breeder and solid breeder blanket designs.) The total depth required is the least of the three options, thus more volume can be provided for the vacuum plenum between the blanket and shield, and/or the reactor size can be slightly reduced to achieve a capital cost saving. Improved reliability can be achieved by incorporating dual welds around each of the breeder coolant tube joints, as described in Ref. 3, so that two separate welds at any given coolant tube end must independently crack through before water can contact the solid breeder.

With all three manifold options, for the inboard blanket it is presently considered that the reflector/manifold region will be eliminated completely as in STARFIRE⁽³⁾, to provide a minimum-thickness inboard breeding blanket. The coolant tubes or panels for the single inboard blanket module will extend poloidally (vertically) through the full length of the module, terminating in

header regions behind the blankets at top and bottom. The headers are in turn connected to the manifolds which extend around the remainder of the sector.

Further development and comparison of the first and third manifold options will be performed later in the study.

IX.3.3 Thermal-hydraulics Analysis

For the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept, the thermal-hydraulics analysis efforts have focused on the BOT and layered approaches to the blanket design. The analyses to determine allowable breeder sizes (as a function of nuclear heating rates) for each of the three approaches are described in Sec. IX.3.3.1. The results were used primarily in deriving materials volume fractions for the neutronics analyses. The development of the thermal models for the first wall and blanket coolant panels is discussed in Sec. IX.3.3.2. These models were analyzed for various heating conditions using the McDonnell Douglas finite-difference code HEATRAN. The resulting temperature distributions were later overlaid onto the panel finite element model (Sec. IX.3.4.3) for thermo-structural analysis, primarily using the NASTRAN code. The requirements for breeder-to-structure interface heat conductance (h_g) with the layered approach (panels) are presented in Sec. IX.3.3.3.2, and similar requirements are given for the BOT approach in Sec. IX.3.3.3.3. A comparison of breeder bulk average temperatures, which relate to blanket tritium inventory, is discussed in Sec. IX.3.3.3.4 for typical $\text{Li}_2\text{O}/\text{H}_2\text{O}$ and $\text{Li}_2\text{O}/\text{He}$ designs.

IX.3.3.1 Breeder Sizing for Blanket Design Approaches

Basic sizing of breeder geometric elements was performed as needed to determine material volume fractions for use in economics analyses (principally bulk materials costs and fabrication costs based on assumed \$/kg rates) and in neutronics and shielding analyses.

The assumptions and methodology for the sizings are shown in Fig. IX.3-12 for all three blanket design approaches. The 1-D cylinder sizing methods were similar to those used in STARFIRE⁽³⁾ and DEMO⁽⁷⁾ for the BOT approach, and in US INTOR⁽⁶⁾ for the BIT approach. The method for the layered approach assumes the breeder layer to be a simple plate cooled equally from both sides and subjected to a uniform nuclear heating rate. More sophisticated analyses

BOT APPROACH

For $\frac{dT}{dr} = 0$ @ r_o , $T = T_{min}$ @ r_i :

$$(T_{max} - T_{min}) = \frac{q_N'' r_o^2}{4k_B} \left[-1 - 2 \ln \left(\frac{r_i}{r_o} \right) + \left(\frac{r_i}{r_o} \right)^2 \right]$$

BIT APPROACH

For $\frac{dT}{dr} = 0$ @ r_i , $T = T_{min}$ @ r_o :

$$(T_{max} - T_{min}) = \frac{q_N'' r_o^2}{4k_T} \left[1 - \left(\frac{r_i}{r_o} \right)^2 - \left(\frac{r_i}{r_o} \right)^2 \ln \left(\frac{r_o}{r_i} \right)^2 \right]$$

LAYERED APPROACH

For $2T = T_{max}$ @ $x = \frac{d}{2}$, $T = T_{min}$ @ $x = 0$, d :

$$(T_{max} - T_{min}) = \frac{q_N'' d^2}{8k_T}$$

NOMENCLATURE

- T_{max} = Breeder maximum temperature, 800°C (Li_2O)
 T_{min} = Breeder minimum temperature, 410°C (Li_2O)
 r_o, r_i = Breeder cylinder outer and inner radius, mm
 q_N'' = Local average nuclear heating rate, W/mm³
 k_T = Breeder thermal conductivity, 3.4 W/m-K (Li_2O @ 15% porosity and 1000 K)

GENERAL APPROACH TO SIZING BREEDER ELEMENTS:

1. Generate curve of r_o (or d) versus q_N'' .
2. Obtain or estimate q_N'' versus depth into blanket.
3. Beginning at first wall depthwise through blanket, use the two curves to estimate maximum size of breeder element for which temperature limits are not violated at the average q_N'' predicted for the element midpoint depthwise. Account for tube (BIT) or panel (layered) thicknesses in determining total thickness of each bank.
4. Use results of (3) to generate volume fractions for each breeder zone material as function of depth.
5. Perform neutronics analysis using results of (4).
6. Compare q_N'' curve from (5) to that assumed in (2). If necessary, use the new curve in successive iterations of steps (3) through (5) until reasonable agreement (within 5 to 10%) of "old" and "new" q_N'' curves is obtained.
7. Final breeder sizing is given by results of (3) and (4) for the final iteration.

Figure IX.3-12. Methodology for sizing breeder elements.

involving 2-D and 3-D heat flow with a depthwise variation in nuclear heating are planned to support the further development of the design.

The treatment of coolant tube spacing and sizing of theoretical cylinders for the BOT approach requires elaboration. Figure IX.3-13 shows, in cross section, four basic ways in which (hypothetical) breeder elements or cells can be physically arranged within the breeding zone. The main criteria for each is that no part of the Li_2O breeder can exceed the T_{max} limit (800°C); the breeder surface at the coolant tube is assumed to be held at T_{min} (410°C). Note that for all but the fourth option, square cells, a breeder confinement structure of some kind is implied, to retain the cylinder shape. Thus only the fourth option is applicable to sphere-pac breeder unless a confinement structure is arbitrarily added to hold the breeder around the coolant tubes.

The first option is dense packing of cylinders, the diameters of which increase with depth into the blanket as the local nuclear heating rate decreases. The sketch indicates how the spacing becomes increasingly non-uniform after the first bank, both laterally and depthwise, if one attempts to pack all cylinders together as closely as possible without overlapping. The actual arrangement is not easily susceptible to mathematical derivation but could be laid out fairly accurately using interactive graphics. However, the option is illogical from the fabrication standpoint, mainly because of the lack of symmetry, and probably would not produce significantly higher tritium breeding ratios than the best of the remaining three options. Therefore the first option was not further pursued.

Neutronics analyses (Sec. IX.3.5.2) indicated that the third option, no packing (fully separated rows) of various-diameter cylinders, yielded a slightly higher breeding ratio than the second or fourth options. Breeder confinement structure (for sintered breeder) was assumed in the first three cases. However, for the sphere-pac breeder fabrication method no additional confinement structure is considered necessary. In this case, the second and third options, which involve cylinders, cannot be physically realized, and the square cell case must automatically apply for analysis of real blankets if breeder temperature limits are not to be violated.

It should be noted that the neutronics results are rather sensitive to the assumptions made for purge gas path (e.g., metallic felt), confinement

(1) DENSE PACKING - CYLINDERS OF VARYING DIAMETER

- o CELLS SIZED TO LOCAL $\dot{Q}_n$.

IN THIS ARRANGEMENT, THE OFFSET TUBE-TO-TUBE VERTICALLY WITHIN A GIVEN BANK, Δ , VARIES Laterally ALONG THE BANK AND BUILDS UP TO A LARGE FRACTION OF D_i NEAR THE BACK OF THE BLANKET. THE ACTUAL VOID % IS SOMEWHERE BETWEEN THOSE OF ARRANGEMENTS (2) AND (3). GIVEN THE REDUCED BREEDING EFFICIENCY OF A GIVEN CELL AS Δ BECOMES LARGER, THE "EFFECTIVE" VOID % IS PROBABLY CLOSE TO THAT OF (2). (IT WOULD TAKE SOME EFFORT TO PROVE THIS, HOWEVER.)

(2) MAXIMUM PACKING - CELLS OF EQUAL DIAMETERS

- o ALL CYLINDERS SAME DIAMETER AS THOSE IN FIRST BANK.
- o MINIMUM VOID AREA AMONG CYLINDERS (~9.3%).
- o VERY INEFFICIENT IN REAR OF BLANKET.

(3) NO PACKING - CELLS OF VARIOUS DIAMETERS

- o ALL CYLINDERS SIZED BY $(\dot{Q}_n)$ LOCAL.
- o ~21.5% OF CROSS-SECTION AREA OF BLANKET IS VOID REGION.
- o $D_i > \dots > D_4 > D_3 > D_2 > D_1$.
- o DEPTH SPACING: $d_i = \frac{(D_i) + (D_{i+1})}{2}$

(4) SQUARE CELLS

- o NO VOIDS AMONG CELLS.
- o $\delta = (D_{cyl}) \times .707$ WHERE D_{cyl} IS SIZED TO $(\dot{Q}_n)$ LOCAL.
- o RELATIVELY HIGH FRACTIONS OF STEEL AND WATER COMPARED TO BREEDER.

Figure IX.3-13. Four options for breeder arrangement with BOT approach.

structure, and tube wall thickness. Thus the relative differences in breeding ratios indicated for the options will vary according to design detail choices.

IX.3.3.2 Coolant Panel Thermal Model

A 328 node steady state thermal model of the coolant panel was constructed using the McDonnell Douglas finite difference heat transfer code HEATRAN. The model has direct node correspondence to the NASTRAN finite element model of the same panel (Sec. IX.3.4.3). The symmetry of the panel and the assumption of uniform heating and a uniform cross section along the panel length, permitted a two dimensional thermal model of the panel which consists of half of one channel as pictured in Fig. IX.3-14. Conduction through the structure and convection to the coolant were the only heat transfer modes used. Adiabatic boundaries exist at the midplane between channels and at the midplane of the channel itself. Plasma heating, nuclear heating of the stainless steel, and nuclear heating of the breeder were input as fluxes on the front face, structure cross section, and rear face respectively. Parametric analysis was performed, varying both the incident heat flux on the front face and the front wall thickness. The temperatures derived from the model were compared to maximum allowable temperatures and also used in the panel stress analysis.

A matrix of analysis cases were examined, varying the front face heating from 10 to 100 W/cm² and the front face thickness from 2.54 to 7.62 mm (0.10 to 0.30 in.). The stainless steel structure nuclear heating for each case was assumed to vary linearly from 66 W/cc at the front of the panel to 50 W/cc at the rear of the panel. Breeder nuclear heating was assumed to vary according to the equation $q'' = 60 - 7.5x$ W/cm², where x is the depth in cm. This variation, along with a 390°C maximum ΔT to the breeder adiabatic boundary and a 4 W/m-K breeder thermal conductivity, produced a two dimensional heat flux of 36 W/cm² on the rear face of the panel. The required flow to remove the panel heating was based on a 3-m-long panel and a 40°C temperature rise in the coolant. For the baseline case of a 8.9 mm thick panel and a 50 W/cm² front face heat flux, 0.11 kg/sec water flow is required, which produces a film coefficient between the coolant and stainless steel of 5.4 W/cm²-K. The required flow and film coefficient varied as the heating and thicknesses changed. The coolant temperature used was assumed to be the maximum allowable at 320°C and the inlet coolant pressure was 15.2 MPa.

Figure IX.3-14. First wall thermal model geometry.

The peak temperatures are plotted in Fig. IX.3-15 against the front face thickness for the three front face heat fluxes. At 100 W/cm^2 the temperature limit of 550°C is exceeded for any front face thickness greater than 3.8 mm. The 50 W/cm^2 flux causes the 550°C limit to be exceeded for front face thicknesses greater than 6.5 mm, while the low flux of 10 W/cm^2 never causes the limit to be exceeded for thicknesses examined. Each case was also examined for boiling. At high fluxes and a thick front face, 100 W/cm^2 and 7.62 mm respectively, incipient boiling will occur at coolant inlet pressures of 15.2 MPa and flow capacities sized for total heat removal only. This is not a severe problem, since boiling can be prevented by moderately increasing the inlet pressure or flow rate.

The first wall model was modified to study blanket cooling optimization. The changes consisted of dimensional variations, heating conditions and flow conditions. The following characteristics were desired: 1) neutronically-thin panel; 2) minimum peak temperatures and gradients; 3) minimum stresses. These were achieved by varying coolant channel height and width, spacing between channels, and wall thickness. The following limits were applied to the optimization: 7 m/s maximum flow rate; 1.27 mm minimum wall thickness, a 1:1 ratio of wall thickness to rib thickness and a maximum coolant temperature rise of 40°C . The heating was assumed constant for each case. The front face heating used was 40 W/cm^2 , the nuclear heating of the stainless steel was 45 W/cm^3 and the back face heating due to the breeder nuclear heating was 35 W/cm^2 . This was based on a breeder temperature range of 410°C to 800°C and a thermal conductivity of the breeder of 3.4 W/m-K . The geometries modeled are illustrated in Fig. IX.3-16. The temperatures from these geometries were applied to the NASTRAN model to determine stresses of the panel. Results of the analysis are discussed in Sec. IX.3.4.

IX.3.3.3 Breeding Zone Thermal Hydraulic Analysis

The analysis presented herein considers only lithium oxide breeder cooled by pressurized water. The underlying design approaches (BIT, BOT, layered) are discussed in Sec. IX.3.2. As discussed in Sec. IX.3.2.4, the BIT concept is considered unacceptable with high-pressure water coolant for a power-producing reactor. Hence, only the coolant panel and BOT are included in the thermal hydraulic analysis discussed here.

Figure IX.3-15. Peak temperature vs. front face thickness.

Figure IX.3-16. Blanket cooling optimization.

IX.3.3.3.1 Design and Operating Conditions

For a given neutron wall loading, the primary design considerations are the operating conditions for the coolant, breeding material, and structural material. The operating conditions for the heat transport system is essentially similar to the current pressurized water reactors. A discussion of the operating temperature window (i.e., the upper and lower temperature limits) for Li_2O is given in Sec. VIII.1. The thermal hydraulic analysis is based on the following data and operating conditions.

Structural Material	PCA SS
Breeding Material (sphere-pac or sintered product)	Li_2O (85% TD)
Neutron Wall Loading (MW/m^2)	5.0
Coolant	Pressurized water
Coolant Inlet/Outlet Temperature ($^{\circ}\text{C}$)	280/320
Breeder Minimum/Maximum Allowable Temperatures ($^{\circ}\text{C}$)	410/800
Surface Heat Flux on the First Wall (W/cm^2)	10-100
Average Thermal Conductivity of Li_2O at the Operating Temperature Range ($\text{W}/\text{m-K}$)	3.4
Coolant Tube ID (mm)	10.0
Coolant Tube OD (mm)	11.50
Thermal Barrier Between Coolant Channel Wall and Li_2O Breeder	SS Felt, He, Li_2ZrO_3

IX.3.3.3.2 Layered Approach

A discussion of the coolant panel design is given in Section IX.3.2.2, and a blanket module using alternate layers of coolant panels and the blanket material is shown schematically in IX.3.3.3.1. The distance between the coolant panels was determined based on the temperature window for Li_2O and the exponentially varying nuclear power field (Sec. IX.3.3.1). The thickness of the slab between the coolant panels is shown in Table IX.3-7. From the heat fluxes over the coolant panels, the required gap conductances to provide the temperature window were determined. The required thicknesses of three conceptual thermal barriers (helium, SS felt, and Li_2ZrO_3 as a representative ceramic) are presented in Table IX.3-7, which shows the thickness of the stainless steel felt at regions near the reflector/shield to be high. Such large thicknesses may penalize the tritium breeding ratio; alternate interface

TABLE IX.3-7. SUMMARY OF CALCULATIONS--WATER-COOLED COOLANT PANEL

Blanket Layer #	Nuclear Heating Rate (W/cc)	Slab Thick. (cm)	Blanket Depth (cm)	Inlet Gap Conductance ($\text{W}/\text{cm}^2 \times \text{K}$)	Helium Gap Thickness (mm)	SS Felt Thickness (mm)	Li_2ZrO_3 Thickness (mm)
1	50.0	1.46	2.10	0.364	0.067	0.83	0.275
2	39.1	1.65	4.38	0.313	0.078	0.96	0.319
3	31.2	1.84	6.87	0.277	0.089	1.09	0.361
4	24.9	2.06	9.57	0.245	0.100	1.22	0.408
5	16.8	2.51	12.73	0.197	0.124	1.52	0.506
6	14.7	2.69	16.05	0.180	0.137	1.67	0.557
7	10.7	3.15	19.84	0.152	0.162	1.97	0.659
8	7.51	3.76	24.24	0.124	0.198	2.42	0.806
9	4.92	4.64	29.52	0.100	0.245	2.99	1.00
10	2.93	6.02	36.18	0.077	0.320	3.91	1.305
11	1.78	7.72	44.54	0.060	0.407	4.97	1.658
12	0.73	12.06	57.23	0.38	0.648	7.91	2.638

concepts should be examined. The question of the interface thermal barrier is further discussed in Sec. IX.3.3.4.

The temperature distribution in the coolant panel was calculated using a three-dimensional conduction model. The steady state temperatures at a few critical locations are summarized in Table IX.3-8. The steady state temperature profile for a typical coolant panel is given in Fig. IX.3-17.

The heat flux over the coolant panels, the breeder layer thickness, and the required gap conductance for the indicated breeder layers are listed for the 12 breeder layers in Table IX.3-7. Thicknesses for helium, SS, felt and Li_2ZrO_3 were estimated from the required interface conductances. An examination of the helium gap thicknesses shows that it is not practical to provide such small gas gaps in a controlled and predictable manner. The thickness of the stainless steel felt is high. The use of neutron-absorbing stainless steel felt thicker than 1 mm instead of ceramic results in lower breeder material fractions, and thus lower tritium breeding ratio. The thickness of the required Li_2ZrO_3 appears to be practical. It may be noted that since Li_2ZrO_3 is a tritium breeder, there will be little or no breeding penalty from its use as a thermal barrier. However, the integrity of Li_2ZrO_3 and other candidate coatings under thermal and irradiation environment needs to be assessed.

The temperature distribution in the coolant panels as listed in Table IX.3-7 indicate that the temperatures are well within the temperature limits for PCA.

IX.3.3.3.3 BOT Approach

The BOT approach is essentially similar to the designs considered in the STARFIRE⁽³⁾ and DEMO⁽⁷⁾ studies. The primary difference lies in the use of sphere-pac lithium oxide breeder.

The basic BOT design approach is depicted in Fig. IX.3-8. The relative position of the coolant tubes in the radial direction (depthwise into the blanket) takes into account the exponential decay of nuclear power. In addition, for the cylindrical blanket cell concept, the relative position of the coolant tubes in adjacent banks in the poloidal (lateral) direction is also important if the temperature window for the solid breeder is to be

TABLE IX.3-8. TEMPERATURE DISTRIBUTION IN COOLANT PANELS*

Nodes	CASE A	CASE B	CASE C
151	354	362	388
155	335	339	350
161	340	345	357
165	365	376	407
286	353	361	385
290	331	333	338
296	335	338	342
300	364	374	404

*Refer to Figure IX.3.3-3 for the node numbers.

Figure IX.3-17. Temperature distribution in a typical coolant panel.

strictly maintained. The rectangular blanket cell concept avoids this difficulty.

Two-dimensional thermal hydraulic analysis was performed for the BOT approach. The blanket model consists of a dense pack of breeder cylinders containing coolant channels at their centers (Fig. IX.3-18). For analytical simplicity, intercell heat transfer is neglected. Since the coolant temperature is significantly below the minimum allowable operating temperature for the breeder, a thermal barrier between the coolant tubes and the breeder is necessary. Three separate methods of providing the thermal barrier were considered. These are: (1) helium gas gap; (2) ceramic coating over the coolant tubes; (3) stainless steel felt sleeves over the coolant tubes. In addition, design calculations were carried out assuming an arbitrary 1.5-mm thick stainless steel felt (60% porosity, thermal conductivity = 3 W/m-K) between the coolant channels and the breeder.

The analytical results for the BOT approach are summarized in Table IX.3-9. The breeder cell diameter, coolant velocity, heat flux over the coolant tubes, thickness of the thermal barrier, the bulk average breeder temperature, and the material fractions are shown in Table IX.3-9. The material fractions, coolant velocities, heat fluxes and heat transfer coefficients, and the inlet and the outlet gap conductances are also shown graphically in Figs. IX.3-19 through IX.3-22. To account for the 40°C inlet-to-outlet coolant temperature, the outlet gap conductances must be larger than the inlet gap conductances. The inlet-to-outlet gap conductance ratio is plotted in Fig. IX.3-23 which shows that the gap conductance ratio varies between 1.75 to 1.55 over the thickness of the blanket. The thickness of the thermal barrier corresponding to the required interface conductance at the coolant inlet are shown in Fig. IX.3-24. (Note: The gap thickness for helium is one-tenth the value shown.) The use of such small helium gas gaps as a means of temperature tailoring is not practical from the fabrication standpoint. Both Li_2ZrO_3 coating and stainless steel felt appear to be practical, although the felt could lead to some penalty in the tritium breeding ratio if used alone.

Thermal analysis was performed for the BOT design for a fixed thickness of thermal barrier consisting of 1.5 mm stainless steel felt. The results are summarized in Table IX.3-10. The structural material fractions and breeder fractions are plotted in Fig. IX.3-25. By comparing the structural material

TABLE IX.3-9. SUMMARY OF CALCULATIONS--BOT DESIGN (VARIABLE GAP CONDUCTANCE)

Breeder Region #	Breeder Region Dia., cm	Coolant Velocity m/s	Heat Flux W/cm ²	Material Fraction %		Gap Thickness mm			Bulk Avg. Temp., °C
				Structure	Breeder	Helium	SS Felt	Li ₂ ZrO ₃	
1	2.37	6.32	63.2	5.72	76.5	0.044	0.76	0.18	700
2	2.52	5.65	57.4	5.08	79.2	0.050	0.85	0.20	703
3	2.70	5.02	51.7	4.44	81.8	0.056	0.96	0.23	705
4	2.89	4.67	46.9	3.87	84.1	0.063	1.07	0.25	708
5	3.11	3.96	42.4	3.34	86.3	0.070	1.20	0.28	711
6	3.34	3.60	38.8	2.89	88.1	0.077	1.32	0.31	713
7	3.70	3.16	34.7	2.35	90.4	0.088	1.50	0.36	717
8	4.08	2.81	31.2	1.94	92.1	0.098	1.67	0.40	720
9	4.62	2.48	27.7	1.51	93.8	0.111	1.90	0.45	724
10	5.30	2.15	24.7	1.15	95.3	0.127	2.16	0.51	728
11	6.20	1.88	21.5	0.84	96.6	0.144	2.47	0.59	732
12	7.23	1.68	19.4	0.62	97.5	0.163	2.78	0.66	736

TABLE IX.3-10. SUMMARY OF CALCULATIONS--BOT DESIGN
(1.5 MM SS FELT THERMAL BARRIER)

Breeder Region #	Breeder Region Dia. (cm)	Material Fraction %		Bulk Average Temp. (c)	Coolant Velocity
		Structure	Breeder		
1	2.27	18.1	65.4	687	6.24
2	2.62	16.1	69.4	690	5.68
3	2.80	14.1	73.2	694	5.02
4	3.01	12.2	76.8	696	4.62
5	3.25	10.5	80.1	700	4.14
6	3.49	9.1	82.7	704	3.91
7	3.88	7.3	86.0	707	3.40
8	4.29	6.0	88.6	711	3.06
9	4.86	4.7	91.1	716	2.71
10	5.60	3.5	93.3	721	2.36
11	6.51	2.6	95.1	726	2.08
12	7.68	1.9	96.4	731	1.89

Figure IX.3-18. One-dimensional model of breeder outside tube design.

Figure IX.3-19. Breeder and structural material fractions.

Figure IX.3-20. Coolant velocity in Li_2O solid breeder blanket.

Figure IX.3-21. Heat flux and heat transfer coefficient for Li_2O breeder.

Figure IX.3-22. Inlet and outlet gap conductances for water-cooled Li_2O breeding blanket.

Figure IX.3-23. Ratio of outlet/inlet gap conductance.

Figure IX.3-24. Thickness of thermal barrier.

Figure IX.3-25. Material fractions for a fixed thermal barrier.

fractions as shown in Tables IX.3-9 and IX.3-10 and in Figs. IX.3-19 and IX.3-25, especially near the first wall region, significant increase in structural material fraction for the fixed thermal barrier design may be observed. This indicates that designs using 1.5 mm stainless steel felt (i.e., a fixed thermal barrier material and thickness) could have significant loss in tritium breeding ratio.

IX.3.3.3.4 Breeder Bulk (Volumetric) Average Temperature

Previous studies^(3,7) have shown that the diffusive inventory of tritium in solid breeder blankets is a strong function of the breeder temperature. Hence, it is desirable to operate the solid breeders at as high a temperature as possible, but within the maximum allowable temperature limit. The temperature distribution in solid breeder blankets depends on not only on the coolant selection (e.g., helium, pressurized water) but also on selection of coolant flow direction (e.g., radial flow vs. flow in the toroidal direction). The coolant flows in the toroidal direction (i.e., the coolant flow direction is perpendicular to the direction of nuclear heat flux) in the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ layered and BOT designs. In the $\text{Li}_2\text{O}/\text{He}$ design (see Sec. IX.4), the coolant flows in the radial direction (i.e., coolant flow direction is parallel to the direction of nuclear heat flux). Since the nuclear heat flux decreases exponentially in the radial direction (depth-wise direction of the blanket), the present reference designs for the two concepts provide radically different temperature profiles across the breeder.

The volumetric average temperature of the blanket was calculated for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ design approaches based on one-dimensional heat conduction in Li_2O . Figures IX.3-8 and IX.3-9 schematically present the two design approaches. For the BOT design, as a first approximation, one may use one-dimensional heat conduction equations for a power generating hollow cylinder to represent the temperature distribution. Similarly, for the coolant panel design, one-dimensional equations may be adequate to estimate an approximate temperature profile.

The bulk average temperatures were calculated for the 12 regions in the blanket for the two approaches using equations for temperatures similar to those shown in Fig. IX.3-12. Table IX.3-11 summarizes the pertinent data for the water-cooled and the helium-cooled Li_2O blanket. The bulk average temper-

TABLE IX.3-11. SUMMARY OF BLANKET CALCULATIONS

Region #	Nuclear Heating Rate	Breeder Region Diameter	Blanket Depth	Heat Flux		Average Temperature		Temperature Gradient	
	Q_N	D_B	X_L	Q/A	QA	T_{avg}	T_{avg}	ΔT_B	ΔT_B
	W/cc	mm	cm	W/cm ²	W/cm ²	°C	°C	°C	°C
				Water-Cooled	He-Cooled	Water-Cooled	He-Cooled	Water-Cooled	He-Cooled
1	59.0	24.44	2.44	54.6	29.5	699	728	390	217
2	40.0	26.77	5.12			703	680	390	147
3	30.0	28.76	7.99			706	650	390	110
4	23.5	30.67	11.06			708	625	390	86
5	18.5	32.74	14.33			711	595	390	68
6	13.0	36.21	17.96			715	571	390	48
7	9.1	40.30	21.99			719	550	390	33
8	6.1	45.70	27.56	23.6	3.05	723	530-	390	22
9	3.9	52.95	31.85			728	520	390	14
10	2.45	62.12	38.06			733	515	390	9
11	1.55	73.14	45.38			738	512	390	6
12	0.85	91.25	54.50	13.9	0.43	744	510-	390	3

atures are plotted in Fig. IX.3-26. By comparing the heat flux over the coolant channels, it may be noted that for the water-cooled design the heat flux varies from 55 W/cm^2 to 14 W/cm^2 . However, for the helium cooled-design, the heat flux varies from 30 W/cm^2 to less than 0.5 W/cm^2 . There lies the major difference in the two design concepts which leads to significantly lower bulk average temperatures for the helium-cooled blanket. For example, the bulk average temperature for the water-cooled design approaches varies from 700°C to 740°C . However, for the helium-cooled design, the bulk average temperature varies between 730°C and 510°C . The temperature gradient across the breeder regions at steady state is 390°C for all regions for the water-cooled designs. This is in contrast to the temperature gradients varying from approximately 220°C to less than 5°C for the helium-cooled design. It should be noted that two-dimensional thermal analysis of the plate-type breeder elements would be necessary for an exact estimate of the volumetric average temperatures.

The discussion in Section VIII.1 indicated that in order to reduce the diffusive inventory of tritium in solid breeders, the bulk average temperature should be maintained at as high a temperature as possible. The recent experimental results from the TRIO-01 tests show that the tritium release rates are strongly dependent on the temperature of the breeder. Hence, the tritium inventory for the helium-cooled radial flow design should be assessed from the standpoint of tritium release characteristics of Li_2O .

IX.3.4 Structural Analysis

The stress analysis effort for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept focused on three specific configurations:

- (1) a corrugated actively cooled panel (also known as panelcoil), proposed for the first wall;
- (2) a ribbed coolant panel with internal rectangular-shaped channels, proposed for the first wall and for blanket cooling; and
- (3) a double wall tube configuration proposed for the breeder zone, with breeder contained within the smaller tube and water coolant within the annulus.

Structural sizes were determined based on mechanical and thermal stresses only. Design allowables were based on unirradiated material properties for PCA stainless steel. The effects of irradiation on degradation of material

Figure IX.3-26. Bulk average temperature distribution in Li_2O blanket.

properties were briefly examined, together with the effects of swelling of the coolant panels. The effects of irradiation on material properties, swelling effects and creep effects will be considered in greater detail in the upcoming year.

IX.3.4.1 Mechanical Properties, Design Allowables and Loads

IX.3.4.1.1 Material Properties

The structural material considered thus far for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept is PCA stainless steel. Properties for Type 316 stainless steel were used since PCA properties were unavailable. Cold worked (20% CW) properties were assumed for the corrugated and ribbed coolant panels, except for local areas. The resistance welds used to fabricate both types of panels were judged by the Structural Materials group to be sufficiently localized that the parent metal cold worked properties should not be significantly degraded. The vertical ribs in the ribbed panel, however, were sized based on annealed properties because the weld extends across the width of the rib. The material properties for Type 316 stainless steel used for the analysis are shown in Table IX.3-12. PCA stainless steel has similar material properties (Chapter IV).

IX.3.4.1.2 Design Allowables

The design allowables were based on the ASME Boiler and Pressure Vessel Code (Code), Section III, Division 1, Class 1.⁽⁹⁾ Section III is for design of Nuclear Power plant components. Class 1 design allowables are essentially the same as Section VIII, Division 2 design allowables, which are for design of pressure vessels.

The criteria for acceptability of a design is that the calculated stress intensities shall not exceed specified allowable limits. At any point in the structure, the principal stresses are calculated and then combined to an equivalent stress intensity by means of a theory of failure for combining stresses. The theory of failure used in the Code for combining stresses is the maximum shear stress theory of failure. The maximum shear stress at a point is equal to one-half the difference between the algebraically largest and smallest of the three principal stresses. The equivalent stress determined from the theory of failure can then be compared with the mechanical

TABLE IX.3-12. TYPE 316 SS MATERIAL PROPERTIES AND DESIGN ALLOWABLES
ASSUMED FOR ANALYSES

<u>Material Properties</u>				
Modulus of elasticity, $E = 1.96 \times 10^5$ MPa (28.4×10^6 psi)				
Poisson's ratio, $\nu = 0.28$				
Condition	Annealed		20% Cold Worked	
Temperature	25°C	300°C	25°C	300°C
σ_u^a , MPa	517 (75.0)	420 (60.9)	800 (116)	710 (103)
σ_y , MPa	207 (30.0)	132 (19.2)	630 (91.4)	555 (80.5)
<u>Allowable Stress Intensities at 300°C</u>				
Condition	Annealed		20% Cold Worked	
Temperature	0.8	1.0	0.8	1.0
S_m , MPa	70.6 (10.2)	88.3 (12.8)	189 (27.4)	237 (34.3)
1.5 S_m , MPa	106 (15.4)	132.4 (19.2)	284 (41.1)	355 (51.5)
3.0 S_m , MPa	212 (30.7)	265 (38.4)	568 (82.3)	710 (102.9)

^aValues in parentheses are in 10^3 psi (ksi) units.

properties of the material obtained from uniaxial tests. The equivalent stress intensity or stress intensity is defined as twice the maximum shear stress.

The Code criteria for determining the design stress intensity (S_m) is as follows. The design stress intensity at any temperature shall be no larger than the least of the following:

- (1) 1/3 of the specified minimum tensile strength at room temperature
- (2) 1/3 of the tensile strength at temperature
- (3) 2/3 of the specified minimum yield strength at room temperature
- (4) 2/3 of the yield strength at temperature

The Code provides allowable stress levels that are dependent on the type of loading. There are five types of loadings, general primary membrane stress (P_m), local primary membrane stress (P_L), primary bending stress (P_b), secondary stress (Q), and peak stress (F). The calculated stress intensity for each type of loading must not exceed the allowable value. General primary membrane stress is a stress which is so distributed that no redistribution of load occurs as a result of yielding. A local primary membrane stress is associated with a primary or discontinuity effect which would produce excessive distortion in the transfer of load to other parts of the structure. Primary bending stresses are the variable component of normal stress which may or may not vary linearly across the thickness. A secondary stress is self limiting and is developed by the constraint of adjacent material or by self-constraint of the structure. Local yielding and minor distortions can satisfy the conditions which cause the secondary stress to occur and failure from one application of a secondary stress is not to be expected. The basic characteristic of a peak stress is that it does not cause any noticeable distortion and is objectionable only as a possible source of a fatigue crack or a brittle fracture.

IX.3.4.1.3 Applied Loads

The primary loads on the first wall/blanket panels were mechanical loads resulting from coolant pressure and the one-atmosphere external pressure differential (first wall only), and thermal loads. The coolant design pressure is 15.2 Mpa (2200 psi). The one-atmosphere pressure differential will not be a problem for the first wall panels since they will be supported

normal to their planes by frames in the blanket or by the side walls. Thermal loads on the first wall and blanket panels are discussed in Section IX.3.3.2.

IX.3.4.2 Corrugated Panel Burst Pressure Capabilities

The objective of this effort was to extrapolate existing panelcoil burst pressure data to determine the panel required thickness and coolant channel configuration.

The allowable pressure for corrugated panels is governed by Section VIII, Appendix 14 of the Code. This appendix covers design, fabrication, and inspection of dimpled or embossed skins. The appendix establishes a maximum allowable skin thickness of 6.35 mm (0.25 in.) The maximum allowable working pressure (MAWP) can be established either by analysis or by burst/proof test.

The equation for allowable operating pressure based on bursting test procedure is:

$$P_{all} = \frac{B E S_o}{5 S_t} \quad [IX.3.4-1]$$

where

P_{all} = operating pressure, MPa

B = burst pressure, MPa

E = weld efficiency

S_o = allowable stress at operating temperature, MPa

S_t = allowable stress at test temperature, MPa

For the first wall/blanket panels the operating pressure is 15.2 Mpa (2200 psi). The permissible weld joint efficiency factor from Appendix 14 is 0.8. The required burst pressure for a 316 stainless steel first wall/blanket panel at an operating temperature of 300°C is 7.14 times the operating pressure. Therefore, the required burst pressure is 108.4 Mpa (15700 psi).

The burst pressures for three corrugation patterns⁽¹⁰⁾ shown in Fig. IX.3-27 were calculated by extrapolating burst test data from Ref. 11 obtained for various total skin thicknesses, as shown in Fig. IX.3-28. The allowable operating pressure for each pattern, calculated from equation IX.3-27 based on the burst pressure extrapolated to STARFIRE first wall thicknesses (5.0 mm total) is also shown in Fig. IX.3-27. The allowable operating pressures for these patterns are seen to be far below the required

- o SKIN THICKNESS = $t_o = 1.5$ mm; $t_i = 3.5$ mm; TOTAL = 5.0 mm (0.197 in)
- o ALLOWABLE OPERATING PRESSURE = (BURST PRESSURE) : (7.14)

PATTERN	BURST PRESSURE	OPERATING PRESSURE
SINGLE-EMBOSSED PATTERN 701	24.13 MPa	3.38 MPa (1) 5.17 MPa
SINGLE-EMBOSSED PATTERN 401	13.79 MPa	1.93 MPa
SINGLE-EMBOSSED PATTERN 301	10.34 MPa	1.45 MPa

(1) STARFIRE HEIGHT IS 4 MM COMPARED TO 6 MM FOR PATTERN 701.
 $P_{\text{STARFIRE}} = P_{701} \left(\frac{h_{701}}{h_{\text{STARFIRE}}} \right) = 1.53 P_{701}$

Figure IX.3-27. Allowable operating pressure for STARFIRE first wall.

Figure IX.3-28. Extrapolations of panel coil burst pressure test data.

operating pressure of 15.2 MPa. Pattern 701, which has a semi-circular-embossed sheet welded to a flat sheet, had the highest operating pressure of 5.17 MPa (750 psi). For a panel with any of the three patterns to reach the required 15.2 MPa operating pressure, skin thickness will have to be increased. For pattern 701 the total panel structural thickness required to meet the 15.2 MPa operating pressure is 22.4 mm (0.88 in.).

A panelcoil design sized for the 15.2 MPa operating pressure will be unacceptable for the first wall/blanket panels. Skin thicknesses of approximately 11 mm (.44 in.) far exceed the maximum Code allowable of 6.35 mm. The thick gages required will make fabrication almost impossible via the embossing (die stamping) process. Peak skin temperatures between channels and resulting thermal stresses will exceed the allowable limits for stainless steel.

IX.3.4.3 Coolant Panel Structural Model

For the first wall/blanket concept with internally machined coolant passages a NASTRAN⁽¹²⁾ finite element model was constructed. This model was used for both stress and deflection analyses. Mechanical stresses were calculated by representing the internal coolant passages as concentrated forces applied to the grid points on the inside surfaces of the channel. Thermal stresses were calculated by inputting the average temperature for each element.

Based on symmetry of the internally machined coolant passage concept only one-half of a coolant passage and one-half of a vertical rib needed to be modeled. Constraints were applied to the model to represent the complete structure. The model was constructed using 224 two-dimensional plate elements. Effects of the third dimension were included by using effective material properties for the modulus of elasticity, E , and Poisson's ratio, ν . The effective modulus of elasticity is given by the following relationship:

$$\bar{E} = \frac{E}{1 - \nu^2} \quad [\text{IX.3.4-2}]$$

and the effective Poisson's ratio, $\bar{\nu}$, is given by

$$\bar{v} = \frac{v}{1 - v}$$

[IX.3.4-3]

The finite element model and applied constraints are shown in Fig. IX.3-29.

IX.3.4.4 First Wall Coolant Panel

A trade study was conducted to determine a configuration for the internally machined coolant passage concept which would meet stress limits for the 15.2 MPa coolant pressure at operating temperatures. Initially, thermal stresses were not included in the analysis and only the coolant pressure was considered. The finite element model discussed in Section IX.3.4.3 was used in the trade study.

Both coolant passage width and skin thickness were varied. Figure IX.3-30 shows the effect of varying skin thickness on peak combined stresses due to coolant pressure. With a coolant passage spacing held constant at 3.81 mm (0.15 inch), varying the skin thickness has a minimal effect on stress levels. Based on fabrication considerations, the minimum acceptable skin thickness selected was 1.3 mm (0.05 in.). With this thickness, weld burn through and sheet thickness tolerances should not be a problem. To determine the effect of passage width on peak combined stresses the skin thickness was set at 2.54 mm (0.10 inch). The effects of coolant passage width on peak combined stresses is shown in Fig. IX.3-31. The maximum acceptable passage width based on stress limitations is 13.0 mm (0.51 inches). For the baseline coolant panel a coolant passage spacing of 3.81 mm was selected. With this spacing, if for some reason the weld for any one internal rib is not effective, the panel skins will still meet the design allowables since the pressure load is safely beamed to the adjacent ribs.

For the reference first wall design, as shown in Fig. IX.3-29, a study was conducted to determine how much of the thickness of the plasma facing skin can be allowed to erode away due to sputtering and plasma disruptions. The stress analysis included the effects of both mechanical stresses and thermal stresses. For thermal stresses, the panel was constrained from out-of-plane bending but was free to expand axially. To help reduce thermal stresses, panel heating from the back side (i.e., the breeder) was assumed. This assumption is consistent with the integral first wall approach adopted (Sec. IX.3.2.2).

Figure IX.3-31. Increasing passage width increases stresses due to coolant pressure.

The method of analysis employed was to vary the plasma facing skin thickness and applied surface heat flux and to compare these results with design allowables. The plasma facing skin thicknesses considered in the study were 2.54 mm (0.1 in.), 5.08 mm (0.2 in.), and 7.62 mm (0.3 in.). The surface heat flux values were 10, 50, and 100 W/cm². Temperature distributions were obtained from the HEATRAN models as discussed in Section IX.3.3.2. Peak temperatures were compared with the maximum allowable structural temperature for stainless steel of 550°C. Combined in-plane stresses were calculated using the NASTRAN finite element model applying both the mechanical and temperature loads. Out-of-plane thermal stresses were calculated using the McDonnell Douglas PLATSA⁽¹³⁾ computer code. The combined maximum principal stresses were then compared with the allowable design stress intensities from the Code.

The peak combined mechanical stress due to the 15.2 MPa coolant pressure occur at the intersection of the plasma facing skin and the internally machined rib. Increasing the plasma facing skin thickness decreased mechanical stresses. This effect was shown in Fig. IX.3-30.

The main source of thermal stresses in the ribbed panel is the thermal gradient through the plasma facing skin. Peak compressive thermal stresses occur at the centerline of the coolant passage at the plasma side surface of the plasma facing skin. Peak tensile thermal stresses occur at the intersection of the backside skin and the vertical ribs at the surface in contact with the coolant. As the plasma-facing skin increases in thickness, backside skin thermal stresses also increase.

The combined mechanical and thermal peak stress intensities are shown in Fig. IX.3-32 as a function of peak surface temperature. Thermal stresses are the main driver in increasing peak stress intensities with increasing surface heat flux and skin thickness. Although mechanical stresses decrease with increasing skin thickness the large increase in thermal stresses more than offsets this decrease. Peak stress intensities are also shown in Fig. IX.3-32 for both the plasma facing skin and the backside skin. The peak combined stresses in the plasma facing skin occur on the plasma side surface at the centerline of the coolant passage. Peak combined stresses in the backside skin occur at the intersection of the rib and the backside skin. When the two skins are the same thickness, peak combined stresses in the coolant panel

Figure IX.3-32. Stress intensity increases with increasing skin thickness.

occur in the plasma facing skin. Increasing the plasma facing skin thickness increases stresses in the backside skin to approximately the same magnitude as the peak combined stress in the plasma facing skin. These stresses increase because the cooler backside skin is trying to resist a larger volume of hotter plasma facing skin. The peak combined stress intensities were compared with the design allowables for 20% cold worked 316 stainless steel. Cold worked material properties can be used because the weld joining the two skins can be placed in the rib away from the skins in an area of low stress. When cold worked material properties are used the limitation on applied surface heat flux becomes the maximum allowable structural temperature limit of 550°C.

Weld joint efficiencies depend on the type of joint and on the degree of inspection of the joint. Section VIII Division 1 of the Code states that for resistance welds the maximum weld joint efficiency shall not exceed 0.80. For corrugated configurations, the maximum weld efficiency is also 0.80. The Code does not indicate that the weld efficiency can be increased when a higher degree of inspection is used for resistance welds.

Increasing the surface heat flux reduces the allowable erosion thickness, as shown in Fig. IX.3-33. The allowable erosion thickness shown is the total plasma facing skin thickness minus the minimum skin thickness of 1.27 mm (0.05 in.) required at end of life for mechanical stresses. Allowable erosion thickness is very dependent on the surface heat flux. Shown in Fig. IX.3-33 is the allowable erosion thickness based on both the 550°C maximum allowable structural temperature limit and the allowable stress intensity limit. For all values of surface heat flux the allowable erosion thickness is determined by the structural temperature limit. Allowable erosion thickness could be increased for this first wall by approximately 40% for any given surface heat flux if the allowable structural temperature were removed as a limiting criteria.

Life of the first wall is shown in Fig. IX.3-34 for various erosion rates. For the 1.0 mm/yr erosion rate assumed by the study, erosion life for 50 and 100 W/cm² surface heat fluxes will be 5.2 yr and 2.6 yr respectively if the maximum allowable erosion thicknesses for those conditions are included in the first wall panel.

Figure IX.3-33. Increasing the surface heat flux reduces the allowable erosion thickness.

Figure IX.3-34. First wall erosion life based on 550°C material structural temperature limit.

IX.3.4.5 Blanket Coolant Panel

The analyses conducted for the blanket coolant panels were similar to the analyses performed for the first wall coolant panels. The three panel configurations considered in this study are shown in Fig. IX.3-35. Three skin thicknesses were selected for this study based on results from the first wall analyses. The coolant passage flow area was then optimized based on thermal-hydraulics considerations of coolant pressure and flow rates, as discussed in Sec. IX.3.3.2.

All three panel configurations are acceptable from the strength standpoint (Fig. IX.3-36). They all have an adequate margin of safety against failure for mechanical (primary) stresses only and also for combined mechanical and thermal (primary plus secondary) stresses. (Margin of safety is a measure of the residual load carrying capability of a structure above the minimum required by the acceptance criteria.) For mechanical stresses only ($1.5 S_m$ allowable design stress intensity), the margin of safety increases with increasing skin thickness. Increasing the skin thickness from 1.27 mm to 2.54 mm results in a 36% increase in margin of safety. This increase is not as large as would be expected because coolant passage width is also increasing with skin thickness. When thermal stresses are included with mechanical stresses $3.0 S_m$ allowable design stress intensity, the margin of safety decreases. This is due to the rapidly increasing thermal stresses with increasing thickness, as shown in Section IX.3.4-4.

IX.3.4.6 Tube Sizing for BIT Approach

As described in Sec. IX.3.1, for the breeder-in-tube (BIT) concept the breeder material is contained within the inner tube of a double wall tube assembly, and the coolant flows in the annulus region between the concentric tubes. A parametric study was conducted to size the inner and outer tube. The same material properties and design allowables used for the ribbed first wall panels were used in this study, except that annealed properties were used because the fusion welds necessary at the tube ends would degrade the cold worked properties. The average structural temperature was assumed to be 300°C. The length of the tubes was assumed to be 3 meters with the ends held circular but not fixed. The assumed coolant pressure is 15.2 Mpa.

The tubes were sized based on the design allowables given in Section III,

Sizing of the inner tube, which is subjected to an external pressure, was based on design charts in the Code which are based on an approximate factor of safety of 5.0 against buckling. Because the tube length is relatively long with respect to the tube radius, end fixity of the tubes will not significantly influence the allowable external pressure. Although length effects are included on the design charts, the tube parameters place it in an area of the charts that classifies the tubes as "long". For a tube under external pressure the relationship of required minimum tube thickness for a given inside tube radius is shown in Fig. IX.3-37.

Figure IX.3-37. Required minimum tube thickness increases with increasing tube radius.

IX.3.5 Neutronics

IX.3.5.1 BIT, BOT, and PANEL Layered Approaches

This section presents a comparison of tritium breeding potential for several blanket concepts studied for Li_2O -breeder/ H_2O -coolant systems. They include BIT (Breeder-In-Tube), BOT (Breeder-Out-of-Tube), and Layered Concepts.

Prior to detailed neutronics analyses of each concept, the calculational accuracy was studied for BOT and BIT by comparing one-dimensional ANISN (Ref. 14) (S_8 - P_3) transport calculation and two-dimensional MORSE Monte Carlo (Ref. 15) (P_3) calculation. The transport and response rate calculations were carried out using the VITAMIN-C (Ref. 16) and MACKLIB-IV (Ref. 17) cross section libraries, both collapsed to 46-neutron/21-gamma energy groups. Figure IX.3-38 illustrates the computational model used for the Monte Carlo method. In the breeding blanket region which is represented as an annular ring of an infinite cylinder in the z-direction, five rows of sub-infinite cylinders are located to fill the ~55-cm blanket region. Each of these subcylinders consists of four co-axial cylinders as shown in Fig. IX.3-39. Depending on the design concept (BOT or BIT), the material arrangement and the region size are varied. The material compositions in each sub-blanket region (BANK) are calculated as shown in Tables IX.3-13 and IX.3-14 for the BOT and BIT concepts, respectively. It is noted that BIT contains much more PCA structure than BOT, which leads to substantially lower TBR in BIT as will be seen shortly. The material compositions listed in Tables IX.3-13 and IX.3-14 are those used for one-dimensional homogeneous calculations by ANISN.

The one-dimensional heterogeneous ANISN calculations were performed by arranging several material layers, the effective thicknesses of which are estimated from the volume fractions of individual materials (see Fig. IX.3-40). The comparison of TBR among the Monte Carlo, one-dimensional heterogeneous and one-dimensional homogeneous calculations is shown in Table IX.3-15 for BOT and in Table IX.3-16 for BIT. In all the cases the first wall is assumed to be a 1-cm thick (65% PCA + 35% H_2O) homogeneous mixture and the reflector to be ~15 cm thick (90% carbon + 5% PCA + 5% H_2O) homogeneous mixture.

It is found that for the BOT concept, both one-dimensional heterogeneous and one-dimensional homogeneous (in particular, the former) methods show very good accuracy in the TBR evaluation, compared to the Monte Carlo calculation.

Figure IX.3-38. A schematic view of breeder tube arrangements used for Monte Carlo analysis.

Figure IX.3-39. Material arrangements in breeder tubes for BOT and BIT concepts.

TABLE IX.3-13. ONE-DIMENSIONAL HOMOGENEOUS TREATMENT
FOR BREEDER-OUT-OF-TUBE CONCEPT

	Volume Percent				
	BANK-1	BANK-2	BANK-3	BANK-4	BANK-5
PCA					
- Coolant tube	5.5	5.1	4.7	4.3	4.0
- Cladding	14.9	14.4	13.8	13.4	12.9
H ₂ O	7.1	6.5	6.0	5.6	5.2
Li ₂ O ^a	51.0	52.5	54.0	55.2	56.4
Void	21.5	21.5	21.5	21.5	21.5

^a@ 100% T.D.

TABLE IX.3-14. ONE-DIMENSIONAL HOMOGENEOUS TREATMENT
FOR BREEDER-IN-TUBE CONCEPT

	Volume Percent				
	BANK-1	BANK-2	BANK-3	BANK-4	BANK-5
PCA					
- Coolant tube	14.9	14.4	13.8	13.4	12.9
- Cladding	11.8	11.5	11.2	10.9	10.6
H ₂ O	13.3	12.9	12.5	12.1	11.7
Li ₂ O ^a	38.5	39.7	41.0	42.1	43.4
Void	21.5	21.5	21.5	21.5	21.5

^a@100% T.D.

TABLE IX.3-15. A COMPARISON OF TRITIUM BREEDING RATIO
FOR BREEDER-OUT-OF-TUBE CONCEPT

	Tritium Total Breeding Ratio ^a		
	Monte Carlo ^b	One-Dimensional	
		Heterogeneous	Homogeneous
BANK-1	0.512 ($\pm 0.8\%$) ^c	0.512	0.510
BANK-2	0.289 ($\pm 0.9\%$)	0.283	0.277
BANK-3	0.178 ($\pm 1.3\%$)	0.167	0.165
BANK-4	0.094 ($\pm 2.1\%$)	0.090	0.089
BANK-5	0.064 ($\pm 2.8\%$)	0.056	0.058
T ₆	0.934 ($\pm 0.2\%$)	0.908	0.898
0.85 T ₇	<u>0.202 ($\pm 0.9\%$)</u>	<u>0.199</u>	<u>0.202</u>
System total	1.136 ($\pm 0.2\%$)	1.107	1.099

^aWith -15% reduction for T₇.

^bMØRSE-CG with 15,000 neutron histories.

^cStandard deviation.

In all of the BANK regions the deviation of one-dimensional calculations from the Monte Carlo evaluation is more or less within the statistical uncertainty of the Monte Carlo method. On the other hand, the TBR evaluation for the BIT concept results in significantly different values depending on the method used. In particular, the one-dimensional homogeneous calculation indicates a quite incorrect tritium production rate, being most pronounced in the first

$$A_B = \pi (r_2^2 - r_1^2)$$

$$A_x = \pi (r_2 + r_1) (2 \delta)$$

$$\delta = \frac{r_2 - r_1}{2} \left(\frac{A_x}{A_B} \right)$$

Figure IX.3-40. One-dimensional heterogeneous treatment.

TABLE IX.3-16. A COMPARISON OF TRITIUM BREEDING RATIO FOR BREEDER-IN-TUBE CONCEPT

	Tritium Total Breeding Ratio ^a		
	Monte Carlo ^b	One-Dimensional	
		Heterogeneous	Homogeneous
BANK-1	0.446 ($\pm 0.8\%$) ^c	0.439	0.516
BANK-2	0.259 ($\pm 1.2\%$)	0.259	0.285
BANK-3	0.150 ($\pm 1.6\%$)	0.142	0.156
BANK-4	0.077 ($\pm 2.6\%$)	0.069	0.075
BANK-5	0.043 ($\pm 3.4\%$)	0.035	0.042
T ₆	0.837 ($\pm 0.3\%$)	0.805	0.932
0.85 T ₇	<u>0.138 ($\pm 0.9\%$)</u>	<u>0.139</u>	<u>0.141</u>
System total:	0.975 ($\pm 0.3\%$)	0.944	1.073

^aWith -15% reduction for T₇.

^bMØRSE-CG with 15,000 neutron histories.

^cStandard deviation.

few banks. As clearly illustrated in Fig. IX.3-39, the neutrons that enter into the Li₂O region of the BIT design must pass through the double walls of PCA structure. The neutron loss due to the parasitic neutron absorption by PCA in this process is quite significant, particularly that from the PCA tube enclosing the breeder since the neutron spectrum is well softened by the presence of the water coolant. This heterogeneity effect thus yields a TBR much less than that evaluated for a homogeneous mixture in which all neutrons can interact with the Li₂O breeder with a certain probability. In fact, this probability is appreciably high even for a system with high PCA content. For example, the material composition of BANK-1 for the BIT design (see Table IX.3-14) indicates that the probability of the ⁶Li(n,α)t reaction amounts to ~95% at the thermal neutron energy.

Table IX.3-17 summarizes the computational errors associated with the one-dimensional ANISN evaluations. The error of the one-dimensional homogeneous treatment for BIT amounts to $\sim +10\%$, resulting in a BR of 1.07 compared to 0.98 by the Monte Carlo calculation. The one-dimensional homogeneous calculation for BOT also shows a somewhat high percentage of error in comparison with Monte Carlo, while the one-dimensional heterogeneous calculations for both BOT and BIT result in an error of $\sim 3\%$.

TABLE IX.3-17. COMPUTATIONAL UNCERTAINTIES OF TBR EVALUATION FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET DESIGNS

	Method		
	2-D Monte Carlo ^a	1-D ANISN	
	Heterogeneous	Heterogeneous	Homogeneous
(A) BIT concept			
TBR	0.98 ($\pm 0.3\%$)	0.94	1.07
δ (%)	-	-3.2	+10.1
(B) BOT concept			
TBR	1.14 ($\pm 0.2\%$)	1.11	1.10
δ (%)	-	-2.6	-3.3

^aMORSE-CG with 15,000 neutron histories.

Although it is expected that the difference in error between the homogeneous and heterogeneous calculations becomes smaller as the size of breeder cylinders is reduced, a particular caution should be exercised in evaluating TBR based on a one-dimensional homogeneous model.

During the course of the present study, a variety of $\text{Li}_2\text{O}/\text{H}_2\text{O}$ designs have been investigated. The neutronics models used for some of these designs are illustrated in Figs. IX.3-41a through 41c and their overall material compositions are summarized in Table IX.3-18. For more detailed system descriptions of these designs, readers are referred to Sec. IX.3.1.3. All of these calculations were performed based on one-dimensional homogeneous (regionwise) cylindrical models. The resulting TBRs are listed in Table IX.3-19. All of

		(A) MAXIMUM PACKING (CELLS OF EQUAL DIAMETERS)	
PLASMA	BANK-1 (15.2)	BANKS 2-23 (30.5 x 22 = 671 (mm) Li ₂ O*: 59.2% PCA: 13.8% H ₂ O: 9.7%	SHIELD
SCRAPE-OFF	51.0		
FIRST WALL	12.1 8.4		

		(B) NO PACKING (CELLS OF DIFFERENT DIAMETERS)	
PLASMA	BANK-1 (61.1)	BANK-2 (80)	BANK-3 (150)
SCRAPE-OFF	51.0	55.2	63.5
FIRST WALL	12.1 8.4	10.5 6.8	7.7 4.3
		BANK-4 (205 mm)	BANK-5 (195)
		Li ₂ O*: 70.2% PCA: 5.0% H ₂ O: 2.4%	73.5 3.6 0.8
			SHIELD

		(C) MAXIMUM PACKING (SQUARE CELLS OF DIFFERENT SIZES)	
PLASMA	BANK-1 (43.4)	BANK-2 (72.7)	BANK-3 (100)
SCRAPE-OFF	48.0	57.0	69.5
FIRST WALL	21.7 16.7	18.4 13.5	13.7 9.2
		BANK-4 (125)	BANK-5 (130)
		81.0 9.2 5.2	89.5 6.1 2.4
		BANK-6 (220 mm)	Li ₂ O*: 92.0% PCA: 5.0% H ₂ O: 1.5%
			SHIELD

Li₂O: a 85% T.D.
FIRST WALL: 8.9 mm (74% PCA + 26% H₂O)
SHIELD: 300.0 mm (90% Fe₁₄Mn₂Cr₂Ni + 10% H₂O)

Figure IX.3-41a. A comparison of various BOT arrangements
(Model: DM-040583)

PLASMA					
SCRAPE-OFF					
FIRST WALL	BANK-1 (21) 20.8 45.3 11.7	BANKS 2-19 (36.4 x 18 = 655.2 mm) Li ₂ O: 24.4% PCA: 52.0% H ₂ O: 13.5%			SHIELD

(A) MAXIMUM PACKING

(CYLINDERS OF EQUAL DIAMETERS)

PLASMA					
SCRAPE-OFF					
FIRST WALL	BANK-1 (91.1) 21.3 45.8 10.9	BANK-2 (125) 23.3 47.2 7.5	BANK-3 (150 mm) Li ₂ O*: 25.3% PCA: 48.8% H ₂ O: 4.5%	BANK-4 (150) 26.2 49.5 3.0	BANK-5 (175) 26.3 49.5 2.8
					SHIELD

(B) NO PACKING

(CYLINDERS OF DIFFERENT DIAMETERS)

Li₂O: a 85% T.D.
FIRST WALL: 74% PCA + 26% H₂O
SHIELD: 300.0 mm (90% Fe-1422 + 10% H₂O)

Figure IX.3-41b. A comparison of various BIT cylinder arrangements
(Model: DM-040583).

PLASMA	
SCRAPE-OFF	
FIRST WALL	
Li ₂ O*	(19 mm)
SS*	
Li ₂ O*	(21)
SS*	
Li ₂ O*	(23.5)
SS*	
Li ₂ O*	(26)
SS*	
Li ₂ O*	(31)
SS*	
Li ₂ O*	(37)
SS*	
Li ₂ O*	(46)
SS*	
Li ₂ O*	(57)
SS*	
Li ₂ O*	(74)
SS	
Li ₂ O*	(101)
SS*	
Li ₂ O*	(130)
SS	
REFLECTOR	
SHIELD	

Li₂O* 96% Li₂O (a 85% T.D.) + 4% PCA
 SS* 6.4 mm (64% PCA + 36% h₂O)

FIRST WALL: 8.9 mm (74% PCA + 26% H₂O)

REFLECTOR: 55.2 mm (90% C + 5% PCA + 5% H₂O)

SHIELD: 300.0 mm (90% Fe₂₄Mn₂Cr₂Ni + 10% H₂O)

Figure IX.3-41c. A layer arrangement of Li₂O/H₂O panel design
 (Model: IM-060183).

TABLE IX-3-18. A COMPARISON OF AVERAGE COMPONENT VOLUME FRACTIONS

Concept	Packing Fraction (%)	Fraction (%)			
		PCA	H ₂ O	@100% TD	@85% TD
<u>BOT</u>					
(A) Maximum packing (cells of equal diameter)	82.5	16.7	11.7	71.6	60.8
(B) No packing (cells of different diameters)	76.2	8.2	4.3	87.5	74.4
(C) Maximum packing (square cells of different sizes)	95.4	9.8	5.6	84.6	72.9
<u>BET</u>					
(A) Maximum packing (cylinder of equal diameters)	89.6	57.9	15.0	27.1	23.1
(B) No packing (cylinder of different diameters)	78.5	61.9	6.3	31.8	27.1
Panel (DM-060183)	100.	10.6	4.0	85.4	72.6

TABLE IX.3-19. A COMPARISON OF TRITIUM BREEDING RATIOS
FOR VARIOUS $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET DESIGNS*

Concept	T_6	T_7	Total	With -15% T_7 Correction
<u>BOT</u>				
(A) Maximum packing ^a (equal diameter)	0.903	0.282	1.185	1.142
(B) No packing ^a (different diameter)	0.894	0.317	1.217	1.163
(C) Maximum packing ^a (different square cells)	0.907	0.256	1.164	1.125
<u>BIT</u>				
(A) Maximum packing ^a (equal diameter)	0.804	0.075	0.879	0.868
(B) No packing ^a (different diameter)	0.772	0.078	0.850	0.839
Panel (DM-060183) ^b	0.900	0.321	1.221	1.173

* Li_2O : natural lithium - 85% T.D.

^aNo reflector

^b5-cm reflector

the blanket designs based on the BOT concept and the panel concept studied result in approximately the same TBR of ~ 1.15 . This result stems from the compositional similarity in the two concepts, particularly in the regions near the first wall, as described in Figs. IX.3-41a and IX.3-41c. Note that the reference BOT design which will be used for a three-dimensional neutronics analysis later in Sec. IX.3.5.3 has even less PCA ($\sim 7.0\%$ on average) volume fraction than those shown in Table IX.3-18. This decreased PCA volume reflects the fact that the maximum breeder temperature is increased to $\sim 800^\circ\text{C}$ from $\sim 690^\circ\text{C}$ assumed for the earlier version of Table IX.3-18. As will be shown shortly, the reference BOT blanket can yield a full TBR of ~ 1.16 with a substantially thinner breeding zone than that used here.

The TBRs for the BIT concept are notably lower than those for the other concepts because of the extremely high content of PCA in the BIT concept. Note that these TBRs for BIT might be even overestimated according to the

analyses of the heterogeneity effect presented earlier. Clearly, from the standpoint of self-sufficient tritium fuel production, the blanket designs based on the BIT concept should be ruled out from the consideration for their fusion applications unless effort is made to substantially reduce the structure volume fraction.

The blanket designs currently being studied feature relatively high fractions of the PCA structure and water coolant volumes, as described in Table IX.3-19. These fractions are compared to ~6.6% PCA/3.3% H₂O of the STARFIRE (Ref. 3) blanket and to ~5.0% Type 316 stainless steel/5.0% H₂O of the DEMO blanket.⁽⁷⁾ In fact, these high structure and water contents have led to relatively low TBRs in the current blanket designs because of the sizable increase of the parasitic neutron absorption in PCA due to the enhanced neutron spectrum softening. For example, the TBRs of ~1.15 obtained for these BOT designs are compared to ~1.23 of the Li₂O/H₂O DEMO blanket that is also based on the BOT concept.

The lithium burnup is of concern in the soft neutron spectrum systems such as the current Li₂O/H₂O blanket designs. Emphasis is placed on the ⁶Li burnup particularly for those designs using natural lithium. A burnup analysis was conducted for the three-element system (⁶Li-⁷Li-⁶He) depicted in Fig. IX.3-42, using one of the earlier versions of Li₂O/H₂O designs. The blanket

Figure IX.3-42. Lithium burnup analysis.

compositions of this version are presented in Table IX.3-20. The analysis was done by making use of the RACC code.⁽¹⁸⁾ Table IX.3-21 presents the fractional burnup rates of ${}^6\text{Li}$, ${}^7\text{Li}$, and total lithium atoms for an integral neutron wall load of 25 MW-yr/m^2 (i.e., 5-yr plant operation at a neutron wall load of 5 MW/m^2). As anticipated, the lithium burnup is quite high in a few banks near the first wall. In particular, the burnup of ${}^6\text{Li}$ in the first bank amounts to almost half of all ${}^6\text{Li}$ atoms, leading to a total burnup rate of ~6%. As a result, the tritium breeding performance in BANK-1 appreciably deteriorates (from 0.244 at the beginning to 0.218 at the end without T_7 correction) as shown in Table IX.3-22. Most of the neutrons that have not been captured in BANK-1 tend to penetrate through deeper blanket regions, being eventually captured by lithium atoms in these regions. Table IX.3-22 clearly indicates this tendency, showing a 2 to 4% increase of TBR in BANKs-3 through -5.

In consequence, the overall TBR is not likely to change to any appreciable degree (from ~1.21 at the beginning to ~1.20 at the end), although the high burnup of lithium tends to increase the probability of neutron capture by the PCA structure to some extent.

Of another concern associated with the high lithium burnup is the local variation of differential response rates such as nuclear heating rate, distinguished from the integral response such as TBR. Figure IX.3-43 plots the variation of several response rates relative to their initial values as a function of time after reactor startup. The neutron wall load is assumed to be 5 MW/m^2 . As indicated in the figure the microscopic response rate of the ${}^6\text{Li}(n,\alpha)t$ reaction (denoted as $\langle\sigma\phi\rangle_6$) increases significantly while the ${}^6\text{Li}$ atom density, N_6 decreases with burnup. The net effect is a quite small variation of the product, $T_6 = N_6\langle\sigma\phi\rangle_6$. The variation of nuclear heating of Li_2O , $\delta k_{\text{Li}_2\text{O}}$ is largely determined by the T_6 variation, δT_6 , but generally less than it because of some contribution from ${}^7\text{Li}$, which is appreciably smaller than δT_6 . Based on the result of Fig. IX.3-43, the maximum variation of the average nuclear heating of Li_2O in BANK-1 over the plant life is estimated to be only about -2.5%. The maximum local variation immediately behind the first wall is somewhat higher and is estimated to be about -7% (variation from ~57 to ~53 MW/m^3).

As the lithium breeder is burned up, a stable carbon isotope ${}^{13}\text{C}$ is produced via the ${}^{16}\text{O}(n,\alpha)$ reaction. The average production rate in BANK-1 is

TABLE IX.3-20. MATERIAL COMPOSITIONS OF THE $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET
USED FOR THE LITHIUM BURNUP ANALYSIS

Region	Thickness (cm)	Composition (%)		
		Li_2O^a	PCA	H_2O
First wall	1	--	65	35
Blanket				
BANK-1	3	61.8	14.2	13.9
BANK-2	5	67.8	12.1	11.2
BANK-3	12	76.0	8.5	8.1
BANK-4	20	85.0	6.4	4.6
BANK-5	29	92.0	4.1	1.9
Shield	30	--	90.0 (Fe-1422)	10.0

$^a\text{Li}_2\text{O}$: @85% of T.D.

TABLE IX.3-21. LITHIUM BURNUP RATES* (%)
(Model: DM-021183)

Region	^6Li	^7Li	Total
BANK-1	49.2	2.37	5.85
BANK-2	33.2	1.54	3.90
BANK-3	18.3	0.72	2.02
BANK-4	6.7	0.17	0.66
BANK-5	3.4	0.04	0.29

*At an integral wall load of 25 MW-yr/m².

TABLE IX.3-22. VARIATION OF TRITIUM BREEDING RATIO
DUE TO LITHIUM BURNUP

Region	TBR	
	Beginning of Life	End of ^a Life
BANK-1	0.244	0.218
BANK-2	0.270	0.266
BANK-3	0.395	0.402
BANK-4	0.247	0.256
BANK-5	0.052	0.054
Total	1.207 (1.160) ^b	1.196 (1.148) ^b

^aIntegral wall load of 25 MW-yr/m².

^bWith a -15% correction for ${}^7\text{Li}(n,n'\alpha)t$.

estimated to be $\sim 5.8 \times 10^{19}$ atom/cc-yr at a neutron wall load of 5 MW/m². The total accumulation of ${}^{13}\text{C}$ at the end-of-plant life comes to $\sim 2.9 \times 10^{20}$ atom/cc on average in BANK-1, which can be translated to ~ 2500 appm of Li_2O density ($\sim 1.17 \times 10^{23}$ atom/cc at an integral wall load of 25 MW-yr/m²). It remains to be investigated what impact the breeder contamination by the carbon formation has on the tritium recovery mechanism in the breeder material.

IX.3.5.2 Use of Beryllium Neutron Multiplier

Tritium breeding can be enhanced by use of a neutron multiplier. The primary incentive for the breeding enhancement is twofold. First, for a given TBR requirement, the use of a multiplier can potentially broaden the scope of blanket viability by permitting the accommodation of (a) a divertor (instead of a limiter) for plasma impurity control; (b) a neutral beam injector (instead of rf diodes) for plasma heating; and (c) nonbreeding inboard blanket. Fusion is still in an early stage of research and development. It is very important, therefore, that fusion blankets can be designed with high potential for the self-sufficient fuel reproduction for a broad range of technology choices and design concept selections. Secondly, for a given blanket design definition or technology choice, the multiplier use can expand the

Figure IX.3-43. Relative variations of several response rates due to lithium burnup.

range of possible tritium inventories [$I_R(\infty)$] and doubling time (t_d).⁽¹⁹⁾ At present, there are large uncertainties concerning the magnitude of tritium inventory attainable in blanket designs, particularly in solid-breeder blanket designs, as addressed in Sec. III.2.3.1. As a result, depending on ultimate $I_R(\infty)$ values, the possible doubling time range may become very limited. Therefore, it is clear that multipliers may be necessary especially for the first years if fusion power is to become a mainstream power system. The two incentives mentioned above complement each other and bring about a great deal of design flexibility in terms of self-sufficient tritium production. Furthermore, there are several advantages to using a multiplier: reducing lithium inventory as well as tritium inventory, increasing blanket power density, etc.

The principal demand for fusion reactors is expected to be created by the demand for increased electrical power generation in the future.

According to Rhinehammer and Wittenberg,⁽²⁰⁾ two scenarios have been considered⁽²¹⁾ for the growth rate of installed electrical generating capacity during the half century (2000-2050): a base case of 1.4% annual growth rate and a high-electrical growth case of 2.5%. In these total electrical generating capacities, the demand for the 1000-GW(e) fusion power economy is projected to occur ~50 yr after the initial fusion power reactor construction when the annual growth rate of total electricity is 1.4%, and ~25 yr when the high growth rate of 2.5% is demanded. Figure IX.3-44 illustrates the projection of the installed fusion capacity in the future, that is tabulated in Ref. 20. Also shown in Fig. IX.3-44 is the corresponding doubling time requirement

Figure IX.3-44. Projection of installed fusion capacity.

that has been calculated from the projection of fusion capacity, for the two scenarios of the annual growth rate.

There exists very little difference in the doubling time requirement between the two scenarios, and both cases indicate a quite rapid increase of fusion construction particularly in the first several years, while the doubling time becomes reasonably long (>5 yr) within ~ 10 yr after the initial construction.

The observation made here strongly suggests a need of a "staged blanket design" development, namely: (a) for the first ~ 10 yr where $\sim 1-5$ yr doubling time is required, breeding blankets be designed using a neutron multiplier (most favorably, beryllium); and (b) thereafter (>10 yr) when the doubling time required is greater than ~ 5 yr, blankets be designed without multiplier. Earlier works⁽²²⁾ have shown that beryllium multiplier is the most promising and perhaps the only viable material for use in fusion systems among several candidate multipliers such as lead and lead-zirconium.

One of the concerns regarding the use of beryllium multiplier is the resource limitation. Based on the result shown in Fig. IX.3-44, Table IX.3-23 estimates the necessary beryllium resources for the previous two cases (1.4% and 2.5% annual growth cases), to meet the demand of high tritium production in the early stage (the first ~ 10 yr) of fusion development. The estimate is based on the STARFIRE model which is a 1.2-GW(e) power plant and operates over the 40-yr plant life with a plant availability of 75%. Each blanket sector is assumed to be replaced once every six years. This periodic blanket replacement results in a large difference in the required beryllium resource depending on whether the beryllium is recycled or not. The requirement varies from the minimum of $\sim 3\%$ of the U.S. reserves and $\sim 1\%$ of the known U.S. resources (1.4% growth rate/with recycling) to the maximum of $\sim 44\%$ of the U.S. reserves and $\sim 15\%$ of the U.S. resources (2.5% growth rate/without recycling). If the beryllium is recycled, even the high growth rate case requires only $\sim 7.5\%$ of the U.S. reserves and $\sim 2.5\%$ of the U.S. resources. The values shown here indicate that beryllium can be used in fusion blankets without much of an impact on the beryllium resource.

How can we maximize the effectiveness of using a beryllium multiplier in fusion blankets? Previous works^(7,22) have demonstrated that tritium production can be substantially enhanced by arranging the multiplier in a "sandwich"

TABLE IX.3-23. BERYLLIUM RESOURCE REQUIREMENT FOR FUSION REACTOR
CONSTRUCTION IN THE FIRST TEN YEARS*

	Annual Growth Rate of Total Electrical Capacity (%)	
	1.4	2.5
Average fusion capacity, GW(e)	18	45
Number of STARFIRES, [1.2 GW(e)]	15	37.5
Total power generation, GW(th) ^a	1.7×10^{10}	4.3×10^{10}
No. of D-T reactions ^b	5.1×10^{30}	1.3×10^{31}
Beryllium burnup, atom ^c	2.5×10^{30}	6.3×10^{30}
Net beryllium requirement, Mg		
over 10 yr	38	95
over 40 yr	114	285
Beryllium resource requirement, Mg, over lifetime ^d		
a. with beryllium recycling	7.5×10^2	1.8×10^3
b. without beryllium recycling	4.5×10^3	1.1×10^4
U.S. reserves, ^e Mg	2.5×10^4	
U.S. resources, ^e Mg	7.3×10^4	
World reserves, Mg	3.8×10^5	
World resources, Mg	1.1×10^6	

* See Ref. 20

^a Thermal conversion factor: 1/3.

^b 21 MeV/D-T.

^c Be(n,2n): 0.5/D-T.

^d 40 yr with 75% availability; 1/6 blanket replacement/yr.

^e STARFIRE (Ref. 3).

fashion as shown in Fig. IX.3-45 (System B) as opposed to a conventional design (System A). Further breeding optimization was carried out this time by changing the thickness of the breeder zone (BANK-1) in between the first wall and the multiplier. Figure IX.3-46 shows the breeding variation with beryllium zone thickness for several different BANK-1 breeder thicknesses. The computation was made using the DEMO design⁽⁷⁾ based on one-dimensional infinite cylinder model and the blanket composition used is 75% Li₂O (@ 85% T.D.) + 15%

Figure IX.3-45. Schematic view of multiplier arrangements.

Figure IX.3-46. Effect of multiplier thickness and BANK-1 thickness on tritium production.

PCA + 10% H₂O. It is found that a beryllium thickness of ~7.5 cm (corresponding to an effective thickness of ~6.8 cm at 90% of the theoretical beryllium density used) always affords the maximum tritium production. There exists a large incentive for placing the multiplier closer to the first wall. In fact, Fig. IX.3-47 exhibits the optimum distance of less than 10 mm from the back side of the first wall for all the multiplier thicknesses studied, with and without internal coolant for the multiplier. The optimum case increases TBR by more than ~0.15 for externally cooled multiplier designs and by ~0.05 for internally cooled designs, relative to the case investigated in the DEMO study using a 4-cm thick BANK-1 breeder. In practice, the multiplier location must evolve from the feasible mechanical and thermal hydraulic design (which can be estimated to ~1-2 cm at least), but one can still expect a substantial breeding enhancement in comparison with the DEMO case. Comparing to the conventional multiplier design in which the multiplier is placed immediately behind the first wall, the breeding difference amounts to ~0.15 (the maximum TBR of 1.20 for ~3 cm beryllium in the conventional design versus the maximum TBR of 1.35 for ~7.5 cm beryllium in the sandwich design; both with an internal coolant).

Figure IX.3-47. Effect of multiplier distance from first wall on tritium production.

The breeding enhancement by using the beryllium multiplier results in a significant saving on the required breeder blanket thickness, hence the breeding material inventory as well. Figures IX.3-48 and IX.3-49 demonstrate how these two quantities vary with TBR requirement. Table IX.3-24 summarizes the result for several TBR criteria. As an example, for a full breeding TBR requirement of 1.1, the blanket thickness (relative to the sandwich design) required for the no-multiplier design (Case A) and the conventional multiplier design (Case B) are respectively 3.1 and ~1.3 and the corresponding relative Li_2O inventories come to ~6.1 and ~1.4. In the case of a full breeding requirement of 1.5, both Cases A and B become unfeasible to meet the criterion while the sandwich design requires a blanket thickness of only ~28 cm (including the multiplier) and results in an Li_2O inventory of ~220 Mg. Such a drastically reduced Li_2O requirement will bring about a proportional reduction in potential solubility tritium inventory. The reduced Li_2O inventory is also of importance to make the solid breeder blanket designs viable without impact on the relatively scarce lithium resource, due to the possible breeder activation.⁽²³⁾

IX.3.5.3 A Comparative Study of $\text{Li}_2\text{O}/\text{H}_2\text{O}$ and $\text{Li}_2\text{O}/\text{Helium}$ Blanket Designs

This section presents three-dimensional Monte Carlo analyses for two Li_2O blanket designs: water-cooled Li_2O design and helium-cooled Li_2O design. The analysis is vital to assessing the multidimensional effects (e.g., the presence of limiter and rf-wave guide penetrations through the blanket, plasma shift, etc.) on tritium production. Figure IX.3-50 illustrates a schematic view of the poloidal cross section of the STARFIRE reactor⁽³⁾ used in the present analysis. Figure IX.3-51 illustrates the system dimensions of the two (H_2O -cooled and helium-cooled) blanket designs at the reactor midplane without inclusion of the limiter system. Table IX.3-25 presents the material compositions used in the two designs.

In order to supplement the expensive three-dimensional Monte Carlo calculations, several one-dimensional analyses have been performed by using ANISN (Ref. 14) with the $S_8\text{-P}_3$ approximation. Two infinite cylinder models were used for these analyses: (1) major radius model where the plasma major axis is chosen as the cylinder axis and both inboard and outboard regions consist of co-axial cylindrical annuli around the axis, taking the system dimensions

Figure IX.3-48. Tritium breeding ratio versus blanket thickness.

Figure IX.3-49. Tritium breeding ratio versus Li_2O inventory.

TABLE IX.3-24. REQUIRED $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET THICKNESS
AND Li_2O^* INVENTORY

Full Breeding TBR Requirement	Blanket Design	Blanket Thickness (cm)	Li_2O Inventory (Mg)
1.1	A. No multiplier ^a	40	430
	B. Conventional design ^b	17	100
	C. Sandwich design ^c	13	70
1.3	A. No multiplier	Not feasible	
	B. Conventional design	24	170
	C. Sandwich design	18	120
1.5	A. No multiplier	Not feasible	
	B. Conventional design	Not feasible	
	C. Sandwich design	28	220

* Li_2O : 85% of T.D.

^aNatural lithium.

^b30% ^6Li enriched Li_2O ; 7.5 cm beryllium without internal coolant.

^c30% ^6Li enriched Li_2O ; 7.5 cm beryllium (1.25 cm from the first wall) without internal coolant.

Figure IX.3-50. A cross sectional view of the STARFIRE reactor used for Monte Carlo analysis.

THE $\text{Li}_2\text{O}/\text{H}_2\text{O}$ DESIGN MODEL AT REACTOR MIDPLANE

MAJOR RADIUS (cm)	401	416	456	466	478.5	483.9	484.8	486	506	700	894	914	915.2	916.1	921.5	934	948.3	984	999
THICKNESS (cm)	15	40	10	12.5	5.4	0.9	1.2	20	194	194	20	1.2	0.9	5.4	12.5	14.3	35.7	15	
	INBOARD SHIELD	INBOARD HOT SHIELD	IBLK-4	IBLK-3	IBLK-2	IBLK-1	FIRST WALL	SCRAPE-OFF		PLASMA	SCRAPE-OFF	FIRST WALL	ØBLK-1	ØBLK-2	ØBLK-3	ØBLK-4	PLENUM	OUTBOARD SHIELD	

THE Li_2O HELIUM DESIGN MODEL AT REACTOR MIDPLANE

MAJOR RADIUS (cm)	401	416	428	438	480	486	506	700	894	914	920	962	963	984	999
THICKNESS (cm)	15	12	10	42	6	20	194	194	20	6	42	--	21	15	
	INBOARD SHIELD	PCA PLENUM	IBLK-2	IBLK-1	FIRST WALL	SCRAPE-OFF		PLASMA	SCRAPE-OFF	FIRST WALL	ØBLK-1	ØBLK-2	PCA-PLENUM	OUTBOARD SHIELD	

Figure IX.3-51. The Li_2O design models at reactor midplane.

TABLE IX.3-25. THE MATERIAL COMPOSITIONS USED FOR
MONTE CARLO ANALYSIS FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$
AND $\text{Li}_2\text{O}/\text{HELIUM}$ BLANKET DESIGNS

	Composition (%)					
	Inboard			Outboard		
	PCA	H_2O	Li_2O^a	PCA	H_2O	Li_2O^a
<u>$\text{Li}_2\text{O}/\text{H}_2\text{O}$</u>						
First wall	80.6	19.4	----	80.6	19.4	
Blanket-1	1.6	0.9	97.5	1.6	0.9	97.5
Blanket-2	12.4	17.8	62.3	12.4	17.8	62.3
Blanket-3	8.5	11.8	74.5	8.5	11.8	74.5
Blanket-4	5.8	7.7	83.1	5.5	7.0	84.3
Plenum	----	----	----	47.4	30.7	21.9 (void)
Hot shield	90	10	----	----	----	----
Shield	80 (Fe-1422)	20	----	80 (Fe-1422)	20	----
<u>$\text{Li}_2\text{O}/\text{Helium}$</u>	PCA	He	Li_2O^a	PCA	He	Li_2O^a
First wall	9.7	----	----	9.7	----	----
Blanket-1	7.5	8.5	84.0	7.5	8.5	84.0
Blanket-2	18.2	8.5	73.3	18.2	8.5	73.3
Plenum	10	----	----	10	----	----
Shield	80 (Fe-1422)	20 (H_2O)		80 (Fe-1422)	20 (H_2O)	----

^a Li_2O : natural lithium; 85% T.D.

presented in Fig. IX.3-51; and (2) minor radius model where the plasma minor axis is selected as the cylinder axis (for this model the inboard blanket and the outboard blanket are separately analyzed).

Table IX.3-26 summarizes the results of the one-dimensional analyses performed prior to the three-dimensional analyses. The two models (major radius model and minor radius model) yield a good agreement on the total TBRs, showing a difference of less than 1% in both $\text{Li}_2\text{O}/\text{H}_2\text{O}$ and $\text{Li}_2\text{O}/\text{helium}$ designs. The major radius model, however, overestimates the contribution from the inboard blanket when one considers the actual inboard wall coverage of ~20% (the ratio of the inboard wall area of $\sim 151 \text{ m}^2$ to the total wall area of $\sim 756 \text{ m}^2$). Nevertheless, the major radius model is quite useful in accounting for the toroidal curvature effect, the individual contributions from the inboard/outboard regions, the blanket effect on the inboard radiation shielding, etc., in the context of one-dimensional analyses. This model will be particularly useful in showing a blanket comparison on a relative basis.

The helium-cooled Li_2O design yields a higher TBR (by ~ 0.1) than the water-cooled design because of the relatively thin breeding zone thickness in the latter design. In fact, the full breeding ratio of this design increases from ~ 1.09 to 1.16 as the inboard and outboard breeder zone thicknesses are increased respectively, from 28.8 cm to 56.8 cm , and from 33.1 cm to 47.8 cm , as indicated in Table IX.3-27. These extended blanket thicknesses are identical to those of the helium-cooled design. It is concluded, therefore, that on the basis of a consistent comparison (in terms of breeding zone thickness), the difference between the two coolant systems amounts only to ~ 0.03 in the full TBR. Extending the inboard breeding zone thickness of the water-cooled design to 68.8 cm (filling up the region corresponding to the inboard plenum region of the helium-cooled design) is not of great assistance in increasing the TBR as shown in Case C of Table IX.3-27. Such a blanket extension will rather result in a worse inboard shielding performance without much benefit for breeding enhancement.

Table IX.3-28 compares the impact of the several inboard blanket designs on the inboard shielding in terms of the leakage current into the shield and the average particle energy. It is found that both neutron and gamma currents leaking into the shield substantially increase as the inboard blanket thickness is increased in the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ design. At the same time, the average

TABLE IX.3-26. FULL TRITIUM BREEDING ANALYSIS*
BASED ON ONE-DIMENSIONAL MODELS

	Breeding Ratio		
	Inboard	Outboard	Total
<u>Li₂O/H₂O Design</u>			
A. Major radius model	0.323	0.764	1.087
B. Minor radius model			
1. Inboard model	1.064	---	
2. Outboard model	---	1.083	
3. 20% inboard + 80% outboard ^a	0.213	0.866	1.079
<u>Li₂O/Helium Design</u>			
A. Major radius model	0.363	0.824	1.187
B. Minor radius model			
1. Inboard model	1.211	---	
2. Outboard model	---	1.167	
3. 20% inboard + 80% outboard ^a	0.242	0.933	1.176

* Li₂O: natural lithium; 85% T.D.: -15% T₇ correction included.

^aBased on the ratio of inboard wall area (~151 m²)/total wall area (~756 m²) of the STARFIRE design.

TABLE IX.3-27. EFFECT OF BREEDING ZONE THICKNESS OF $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET DESIGN ON FULL TRITIUM BREEDING RATIO BASED ON ONE-DIMENSIONAL MAJOR RADIUS MODEL

Inboard/Outboard ^a Breeding Zone Thickness (cm)	Breeding Ratio		
	Inboard	Outboard	Total
<u>$\text{Li}_2\text{O}/\text{H}_2\text{O}$ Design</u>			
A. 28.8/33.1	0.323	0.764	1.087
B. 56.8/47.8	0.345	0.808	1.155
C. 68.8/47.8	0.349	0.808	1.157
<u>$\text{Li}_2\text{O}/\text{Helium}$ Design</u>			
D. 56.8/47.8	0.363	0.824	1.187

^aThe variation of thickness is made only by changing the last blanket zone (Blanket-4) thickness.

TABLE IX.3-28. A COMPARISON OF RADIATION LEAKAGE INTO INBOARD SHIELD FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ AND $\text{Li}_2\text{O}/\text{HELIUM}$ BLANKET DESIGNS BASED ON ONE-DIMENSIONAL MAJOR RADIUS MODEL

Design	Inboard Breeding Zone Thickness (cm)	Leakage/D-T		Average Leakage Energy (MeV)	
		Neutron	Gamma	Neutron	Gamma
$\text{Li}_2\text{O}/\text{H}_2\text{O}$	28.8	3.88 (-4)	1.86 (-4)	1.25 (0)	5.13 (0)
	56.8	7.04 (-4)	6.60 (-4)	2.31 (0)	4.82 (0)
	68.8	7.81 (-4)	3.81 (-3)	3.60 (0)	3.07 (0)
$\text{Li}_2\text{O}/\text{helium}$	56.8	1.03 (-2)	4.02 (-3)	1.58 (0)	5.73 (0)

neutron energy of the leakage current increases from ~1.3 MeV (28.8 cm blanket case) to ~3.6 MeV (68.8 cm case), while the average gamma energy indicates a somewhat decrease due to the less pronounced (n,γ) reaction in the thinner hot-shield zone (90 vol-% PCA + 10 vol-% H_2O). Comparing the water-cooled design to the helium-cooled design for the same blanket thickness (56.8 cm case), it is found that the water-cooled design brings about a drastic reduction in the particle leakage into the shield, i.e., a factor of ~15 reduction

in the neutron current, and a factor of ~6 in the gamma current. These differences are expected to result in an appreciable impact on the inboard shield in terms of the inboard magnet protection. A detailed analysis on the inboard radiation shielding is presented elsewhere in this report.

Table IX.3-29 compares the overall performance of the Li_2O designs from the standpoint of energy deposition. Except for small differences for components external to the blankets (due to the difference in the shield designs), the overall profile of energy deposition is more or less identical among all of the designs studied. The system energy deposition results in slightly less than ~18 MeV (a blanket energy multiplication of ~1.27) in all the cases, leading to a total energy production of ~21.5 MeV per fusion.

TABLE IX.3-29. A COMPARISON OF ENERGY DEPOSITION FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ AND $\text{Li}_2\text{O}/\text{HELIUM}$ BLANKET DESIGNS BASED ON ONE-DIMENSIONAL MAJOR RADIUS MODEL

Blanket:	$\text{Li}_2\text{O}/\text{H}_2\text{O}$			$\text{Li}_2\text{O}/\text{He}$
Inboard breeding zone thickness (cm):	28.8	56.8	68.8	56.8
Outboard breeding zone thickness (cm):	33.1	47.8	47.8	47.8
Component	Energy Deposition (MeV/D-T)			
A. Inboard				
- First wall	0.609	0.607	0.607	0.303
- Blanket	4.129	4.429	4.450	4.676
- Hot shield/plenum	0.424	0.036	--	0.016
- Shield	0.004	0.007	0.016	0.092
B. Outboard				
- First wall	1.481	1.479	1.479	0.737
- Blanket	10.401	10.988	10.988	11.490
- Plenum	0.835	0.235	0.235	0.135
- Shield	0.024	0.028	0.028	0.466
C. System total	17.905	17.808	17.802	17.915
D. Energy multiplication/14.06 MeV	1.273	1.267	1.267	1.274

In order to carry out three-dimensional analyses, the toroidal reactor system shown in Fig. IX.3-45 has been modeled by a Monte Carlo transport code, MORSE-CG (Ref. 15). The spatial dependence of the source neutron distribution is assumed to be

$$f(r) = [1 - (r/r_m)^{C_1}]^{C_2},$$

taking into account the plasma shift due to the high beta MHD equilibrium. The r and r_m stand, respectively, for the source point distance and the plasma boundary distance, both measured from the shifted plasma center, $R_m + S_m$. Here R_m is the magnetic axis major radius (7.0 m), and S_m is the plasma shifting distance (0.79 m). The plasma boundary contour is determined by parametric equations:

$$z_b = a \kappa \sin t;$$

and

$$\rho_b = R_m + a \cos(t + d \sin t),$$

where z_b is the boundary contour height in the direction perpendicular to the reactor midplane, and ρ_b is the major radius of the boundary contour. The values used for the three constants, a (plasma minor radius), κ (plasma elongation factor), and d (plasma triangularity), are 1.94 m, 1.6, and 0.5, respectively. The coefficients C_1 and C_2 appearing in the distribution function are the source shaping factors used for the neutronic modeling of the 14-MeV neutron source distribution. In the present study, both coefficients are assumed to be 2.0.

The limiter system modeled composes of three regions LIM-A, -B, and -C, and their material compositions assumed are:

LIM-A: 62% Ta-5W + 38% coolant;

LIM-B: 84% Ta-5W + 16% coolant; and

LIM-C: 10% Ta-5W + 8% coolant + 75% void.

The RF-waveguide modeled in the present study has a opening area of 77 cm (vertical) $\times$ 68 cm (horizontal) and a length of 70 cm. The uniform material composition assumed is 53% PCA + 8% coolant + 39% void. The RF waveguide is located at every 30 deg, i.e., 12 RFs in total. These definitions of the limiter and RF designs have been taken from the STARFIRE study and the readers are referred to Ref. 3 for more detailed geometrical descriptions.

Table IX.3-30 compares the tritium breeding performance for three $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket designs:

Case A: a full breeding blanket.

Case B: blanket with the limiter in Sector-5 and the RF in Sector-6; and

Case C: the same blanket as Case B above but the outboard BANK-4 thickness of 48.1 cm instead of 33.1 cm.

TABLE IX.3-30. TRITIUM BREEDING PERFORMANCE IN $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET DESIGNS*

Case:	A	B	C
Inboard breeding zone (cm):	28.8	28.8	28.8
Outboard breeding zone (cm):	33.1	33.1	48.1 ^a
System: TBR	Full breeding	With limiter + RF	With limiter + RF
Sector-1	0.192 ($\pm 1.0\%$) ^b	0.191 ($\pm 1.0\%$)	0.191 ($\pm 1.1\%$)
2	0.050 ($\pm 2.1\%$)	0.052 ($\pm 2.3\%$)	0.053 ($\pm 2.1\%$)
3	0.074 ($\pm 1.6\%$)	0.071 ($\pm 1.7\%$)	0.075 ($\pm 1.7\%$)
4	0.246 ($\pm 0.9\%$)	0.242 ($\pm 1.0\%$)	0.259 ($\pm 0.9\%$)
5	0.233 ($\pm 1.0\%$)	0.179 ($\pm 1.2\%$)	0.195 ($\pm 1.1\%$)
6	0.243 ($\pm 0.9\%$)	0.239 ($\pm 1.0\%$)	0.247 ($\pm 0.9\%$)
7	0.069 ($\pm 1.8\%$)	0.071 ($\pm 1.7\%$)	0.073 ($\pm 1.6\%$)
8	0.051 ($\pm 2.1\%$)	0.048 ($\pm 2.2\%$)	0.054 ($\pm 2.1\%$)
Total T_6	0.855 ($\pm 0.2\%$)	0.810 ($\pm 0.2\%$)	0.857 ($\pm 0.2\%$)
Total T_7	0.302 ($\pm 0.4\%$)	0.283 ($\pm 0.5\%$)	0.290 ($\pm 0.5\%$)
Total ($T_6 + T_7$)	1.157 ($\pm 0.1\%$)	1.092 ($\pm 0.2\%$)	1.147 ($\pm 0.2\%$)
Total ($T_6 + 0.85 T_7$)	1.112 ($\pm 0.1\%$)	1.050 ($\pm 0.2\%$)	1.104 ($\pm 0.2\%$)

*MORSE-CG with 40,000 neutron histories.

Li_2O : natural lithium; 85% of T.D.

^aExtension only in BANK-4 region (see Fig. IX.3-51 and Table IX.3-16).

^bStandard deviation estimated by MORSE.

In all cases the inboard breeding zone is assumed to be 28.8 cm. The coolant for both limiter and RF is assumed to be light water. The computations were

carried out with 40,000 14-MeV source neutron histories. The full tritium breeding ratio without inclusion of the limiter/RF-waveguide results in ~ 1.167 without the correction for the ${}^7\text{Li}(n,n'\alpha)t$ reaction (T_7), and 1.11 with the T_7 correction. These breeding ratios are estimated with a standard deviation of $\sim \pm 0.1\%$ and show a very good agreement with the one-dimensional results shown earlier, based on the major radius model (see Table IX.3-26). Upon implementation of the limiter and RF, the TBR drops to ~ 1.05 (with the T_7 correction) indicating a breeding loss of ~ 0.06 due to the penetration. The breakdown of the loss is estimated to be ~ 0.04 in Sector-5 and ~ 0.01 in Sector-6 and the remaining ~ 0.01 loss is contributed from the rest of the blanket. The indication is that the limiter design is of greater importance from the tritium breeding standpoint. As addressed earlier, the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket design studied here has a relatively thin breeding zone (particularly in the outboard region) in comparison with the helium-cooled design. Case C of Table IX.3-30 examines the effect of a thicker outboard blanket design on TBR, extending the BANK-4 region size to 48.1 cm, leading to a total outboard thickness Δ_{WB}^0 of 85 cm instead of 70 cm. The 48.1-cm thick outboard breeding zone is approximately the same as the case of the helium-cooled design. This enlarged blanket design yields a TBR of ~ 1.10 (with the T_7 correction) including the limiter and RF; i.e., a breeding enhancement of ~ 0.05 in comparison with Case B. It appears that the breeding zone thicknesses of 28.8 cm/33.1 cm are not large enough to recover a sufficient number of neutrons (especially slow neutrons) by the lithium reactions in the deep blanket regions. The breeding enhancement shown by Case C indicates a strong incentive for a thicker blanket design for $\text{Li}_2\text{O}/\text{H}_2\text{O}$ systems. Particular emphasis should be placed on the fact that most of the enhancement takes place in the outer regions (Sector 4, 5, and 6) where the space constraint in terms of reactor configuration seems minimal. A detailed trade-off study remains to be carried out in order to identify the impact of such a thicker blanket design on the outboard radiation shielding.

Table IX.3-31 studies the tritium breeding performance for three different helium-cooled blanket designs:

- Case A: a full breeding blanket;
- Case B: a blanket with helium-cooled limiter and RF; and
- Case C: a blanket with water-cooled limiter and RF.

TABLE IX.3-31. TRITIUM BREEDING PERFORMANCE IN Li_2O /HELIUM BLANKET DESIGNS*

Case:	A	B	C
Inboard breeding zone (cm):	56.8	56.8	56.8
Outboard breeding zone (cm):	47.8	47.8	47.8
System: TBR	Full breeding	With limiter + RF both cooled by helium	With limiter + RF both cooled by light water
Sector-1	0.211 ($\pm 0.9\%$) ^a	0.216 ($\pm 1.0\%$)	0.204 ($\pm 0.9\%$)
2	0.055 ($\pm 1.9\%$)	0.057 ($\pm 2.0\%$)	0.055 ($\pm 1.9\%$)
3	0.077 ($\pm 1.6\%$)	0.076 ($\pm 1.6\%$)	0.074 ($\pm 1.6\%$)
4	0.261 ($\pm 0.7\%$)	0.262 ($\pm 0.9\%$)	0.255 ($\pm 0.9\%$)
5	0.249 ($\pm 1.0\%$)	0.205 ($\pm 1.0\%$)	0.184 ($\pm 1.0\%$)
6	0.259 ($\pm 0.8\%$)	0.249 ($\pm 0.8\%$)	0.244 ($\pm 0.9\%$)
7	0.079 ($\pm 1.4\%$)	0.079 ($\pm 1.5\%$)	0.075 ($\pm 1.7\%$)
8	0.057 ($\pm 1.9\%$)	0.055 ($\pm 1.9\%$)	0.055 ($\pm 2.0\%$)
Total T_6	0.833 ($\pm 0.1\%$)	0.809 ($\pm 0.2\%$)	0.762 ($\pm 0.2\%$)
Total T_7	0.415 ($\pm 0.4\%$)	0.391 ($\pm 0.5\%$)	0.384 ($\pm 0.5\%$)
Total ($T_6 + T_7$)	1.248 ($\pm 0.1\%$)	1.200 ($\pm 0.2\%$)	1.146 ($\pm 0.2\%$)
Total ($T_6 + 0.85 T_7$)	1.186 ($\pm 0.1\%$)	1.141 ($\pm 0.2\%$)	1.088 ($\pm 0.2\%$)

*MORSE-CG with 40,000 neutron histories
 Li_2O : natural lithium; 85% of T.D.

^aStandard deviation estimated by MORSE.

All the computations were performed with 40,000 neutron histories as for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ cases. The full breeding blanket case results in a total breeding ratio of 1.248 (without the T_7 correction) or 1.186 (after the correction) which is compared to ~ 1.187 obtained by the one-dimensional major radius model. The impact on TBR of having the helium-cooled limiter/RF system is a total breeding loss of ~ 0.045 (with the T_7 correction) which is more or less consistent with what was observed in the previous $\text{Li}_2\text{O}/\text{H}_2\text{O}$ design cases. When the coolant for the limiter and RF is changed to light water, the breeding performance appears to be significantly altered and the breeding loss (relative to no penetration case) amounts to ~ 0.1 , as presented in Case C of Table IX.3-31. By comparing Case C to Case B, one finds that the largest breeding

degradation takes place near the reactor midplane (Sector 4, 5, and 6) as well as in the inboard blanket (Sector 1). This reflects the large neutron loss in the limiter, RF regions and their neighbor, because of the parasitic neutron capture by the PCA structure due to the localized spectrum softening caused by the water coolant. In fact, the breeding loss occurs in almost the entire depth of the blanket penetrated by the limiter and RF. For example, the breeding losses in the first, second, and third blanket regions of 14 cm each amount to ~ 0.026 , ~ 0.016 , and ~ 0.011 , respectively. The TBR of 1.088 in Case C is compared to the corresponding value of ~ 1.104 of the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ design (Case C of Table IX.3-30). The indication is that on the basis of the same design concept for the in-vessel components, the breeding performance of the solid Li_2O blanket designs is not significantly affected by the blanket coolant choice. However, it should be emphasized again that the design definition of the in-vessel components themselves (regarding especially the coolant selection) does notably affect the breeding performance. It is of importance to note that the single design uncertainty concerning the coolant selection leads to a breeding uncertainty by more than ~ 0.05 in the overall TBR. It is also noted that a further study remains to be carried out as to the impact of limiter location (e.g. at the bottom instead of at the midplane) on tritium breeding performance.

Finally, Fig. IX.3-52 shows a possible range of steady-state tritium inventory in the reactor, $I_R(\infty)$, for the Li_2O blanket designs studied here. The hatched area in Fig. IX.3-52 indicates a feasible domain as a function of required doubling time, assuming that the Li_2O blankets (both helium- and water-cooled) can yield a net BR of 1.1 (including the limiter and RF penetrations) without any uncertainties associated with the neutronics evaluation. When one needs to account for $\sim 5\%$ uncertainty for the neutronics evaluation as addressed in Sec. III.2.3.1, the feasible domain for the Li_2O designs is reduced by the dotted area. The maximum allowable $I_R(\infty)$'s for $t_d = 1, 5$, and 10 yr become ~ 9 , ~ 30 , and ~ 50 kg, respectively.

Figure IX.3-52. Possible range of steady-state tritium inventory in reactor for Li_2O breeder blanket designs.

IX.3.6 Comparison of Blanket Design Approaches

The selection of the overall best blanket design approach was based on the results of the analyses and evaluations previously discussed plus the recommendations of the Tritium Recovery Issues Group (Chapter VIII).

Several points should be noted before discussing the comparison. First, the conclusions and judgments are considered to be preliminary, since they are based on preliminary results. Second, the points noted in the following discussion relate in general only to the differences anticipated among the approaches for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ concept. There are many areas where no significant differences exist, e.g. power conversion efficiency, resource limitations, first wall erosion lifetime. Third, the viability of any of the approaches is largely dependent on the feasibility of the Li_2O breeder itself, principally from the standpoints of fabricability and of radiation damage effects on thermophysical properties and tritium release characteristics.

The preliminary conclusions that most strongly affect the choice of design approach for the reference design blanket are listed in Table IX.3-32. The recognition of the high probability of cracking of Li_2O sintered product when subjected to thermal gradients typical for power reactor blankets, is the factor primarily responsible for the strong emphasis thus far on examination of approaches to blanket design and breeder fabrication that are fundamentally different from those adopted by recent studies of water-cooled solid breeder blankets.

The BIT approach is presently considered not acceptable for use with high-pressure water coolant, on the basis of insufficient tritium breeding without the use of a neutron multiplier. The BIT approach is expected to be inferior to the other two approaches in tritium breeding and in overall attractiveness even if a neutron multiplier is used for all three.

The primary results of the comparison of the BOT and layered approaches are shown in Table IX.3-33 and are discussed below.

Tritium Breeding - This is one of the two most important engineering feasibility evaluation criteria (see Chapter III). The neutronics analyses of Section IX.3.5 shows the BOT approach to yield roughly the same tritium breeding ratio as the layered approach. The tritium breeding results shown for the BOT and layered approaches in Sec. IX.3.5 are sensitive to the

TABLE IX.3-32. PRELIMINARY STUDY CONCLUSIONS FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$
BLANKET CONCEPT

- o Large-scale breeder cracking, if present, will probably result in unacceptably large uncertainties in blanket performance.
- o The Breeder-in-Tube (BIT) approach results in an unacceptable TBR. With a neutron multiplier, TBR is perhaps acceptable but will be high-risk.
- o Tritium breeding ratios for the BOT and layered approaches are estimated as follows:

<u>Approach</u>	<u>Without Multiplier</u>	<u>With Multiplier</u>
BOT	$\sim 1.10 - 1.15$	$>> 1.20$
Layered	$\sim 1.10 - 1.15$	$>> 1.20$

- o Coolant panels with rectangular cross section channels are feasible for first wall and breeder zone applications, and meet non-irradiated stress criteria.
- o The sphere-pac breeder fabrication method is attractive for the BOT and layered blanket design approaches. The sintered product breeder fabrication approach is less attractive for the layered approach, and leads to major design difficulties for the BOT approach in assuring desired breeder/structure interface characteristics.

TABLE IX.3-33. COMPARISON OF THE ANTICIPATED POTENTIAL FOR THE
 $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET CONCEPT DESIGN APPROACHES^a

Area	BOT	Layered
o Tritium Breeding - Without Multiplier - With Multiplier	o $\sim 1.10 - 1.15$ o $>> 1.20$	o $\sim 1.10 - 1.15$ o $>> 1.20$
o Reliability	o Expected to be very good with proper design details - Two separate welds, each end - Double wall tubes	o Panels can be high reliability but seal welds in breeder zone are of concern
o Safety in Accidents	o Dual coolant loops can be incorporated, for afterheat removal	o Dual loops not feasible
o Blanket fabrication ^b	o Less complex overall than layered	o Main complications are plenum/panel connections and panel fabrication
o Design flexibility - Breeder fabrication - Beryllium neutron multiplier (slab)	o Major change in details and complexity for sintered product o Moderate detail changes and increase in complexity if located immediately behind first wall	o Accommodation of sintered product is relatively simple o Little change to incorporate multiplier
o Primary areas of concern or development risk	<p style="text-align: center;"><u>(BOTH)</u></p> <ul style="list-style-type: none"> o First wall irradiation effects o Sphere-pac: <ul style="list-style-type: none"> - Li_2O fabrication - Long-term effects of irradiation o Reliability level of water-cooled designs 	
	<u>BOT</u>	<u>Layered</u>
	o Incorporation of sintered product breeder (if needed)	o Plenum-to-panel seal weld reliability

^a Assumes BIT approach is not acceptable due to insufficient breeding.

^b Assumes sphere-pac breeder fabrication approach.

assumptions made for design details such as purge gas path (e.g., metallic felt), breeder confinement structure (if any), and tube wall thickness. Thus the relative differences in breeding ratios indicated for the approaches will vary according to design detail choices. The same relative results may also be expected if a beryllium neutron multiplier is added.

Reliability - For water-cooled blankets, reliability against leaks that result in reactor shutdown is expected to receive major emphasis as the design is developed further. Statistical data on tube failures in fission reactors tends to indicate that, for blanket systems with tens of thousands of coolant tubes, the failure (leak) rate could be ≥ 10 per year, clearly unacceptable in terms of reactor availability. It is presently thought that a large majority of the failures would occur at pressure boundary welds (transitions, ends, etc.) rather than in the virgin seamless tubes. The coolant panels can be designed with double skins on each panel face so that two independent through-cracks must occur at any one specific coolant channel before water can leak into the breeder. However, similarly providing two barriers at the welded panel/plenum joints could be very difficult, and thus a single through-crack in any seal would permit water/breeder contact, resulting in shutdown. For the BOT approach, double wall tubes can be used inside the breeder zone, with the ends independently welded at or behind the rear wall such that both pressure boundaries at any given tube must be breached before water/breeder contact can occur. The resultant increase in calculated reliability could be very large.

Safety in Accidents - The major difference here is that the BOT approach permits the incorporation of dual independent coolant loops (as in STARFIRE⁽³⁾) for the first wall and blanket, to enable safe removal of afterheat from all blanket sectors in the event of a shutdown due to a failure in only one loop. To incorporate two loops into the layered design would be very difficult; an emergency cooling system would likely be the preferred option.

Blanket Fabrication - The fabrication complexities for the BOT approach are judged to be less than for the layered approach, mainly because the end plenums and their connections to the first wall and blanket panels are eliminated. Fabrication of the double wall coolant tubes should also be less costly than the welded panels. The principal complication for the coolant

tubes is the welding of the ends of each inner tube to the manifold. However, the complexity of this plumbing is typical of that encountered in large steam generators and heat exchanger equipment. Also, making butt weld joints in thin-wall stainless steel tubing using automatic equipment is state-of-the-art.⁽³⁾ The use of the sphere-pac breeder fabrication approach will greatly simplify the final blanket assembly for either approach.

Design Flexibility - Preferably, the selected approach should be able to accommodate changes in the blanket concept itself or in design details without requiring major modification. The two most important possible changes to the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket reference design would be (1) use of the sintered product breeder instead of the sphere-pac approach, and (2) the use of a slab-type beryllium neutron multiplier. The layered approach can accept either or both changes with relatively small impact. It should be noted that the re-adoption of the sintered product breeder fabrication approach for this concept would likely require major changes to the BOT approach, to assure adequate breeder confinement and acceptable breeder/tube interface heat transfer characteristics.

Areas of Concern/Development Risk - In terms of engineering feasibility, the principal risk for the BOT approach is considered to be the difficulties involved with incorporation of the sintered product breeder, if the sphere-pac approach to Li_2O fabrication does not prove to be workable. For the layered approach, development of highly-reliable plenum/panel seals and weld methods is the area of most concern. For both approaches, however, the following areas of significant concern presently exist.

- o Accommodation of irradiation effects (creep, swelling, loss of ductility) in the PCA stainless steel first wall and in the first few centimeters depthwise of blanket structure.
- o Development of sphere-pac fabrication methods for Li_2O .
- o Long-term irradiation effects on the physical behavior of sphere-pac Li_2O
- o Attaining high reliability in the high-pressure water coolant system within the reactor envelope.

IX.3.7 Reference Design Description

The preliminary reference design for the $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket concept is

illustrated schematically in Fig. IX.3-53. Key features, materials selections and design option selections are listed in Table IX.3-34. Major parameters are listed in Table IX.3-35.

The blanket design uses individual first-wall/blanket modules configured as parallelepipeds. These modules are assembled into sectors, ten of which would comprise the reactor first-wall/blanket system.

The first wall is an actively cooled ribbed panel made of PCA austenitic stainless steel. Three millimeters of steel are added to the plasma-facing side of the panel as an erosion allowance, permitting a 3-year life on the basis of erosion at the presently-assumed 1 mm/yr first wall erosion rate. (This is approximately the same as the 3-year life anticipated for the PCA first wall on the basis of radiation-induced swelling. More erosion thickness could be added without violating limits.) The first wall panel is continuous down the module ends to the rear of the breeding zone, where its inlet and outlet ends terminate in headers at the rear of the breeder zone. The first wall is mechanically and structurally integrated with the blanket. Actively cooled support frames within the breeding zone are welded to the back side of the first wall and to the rear wall of the breeding zone to provide structural rigidity to the module. (Alternatively, the modules can be made 25-30 cm wide poloidally with no internal frames.) The module sides in the toroidal plane are also actively cooled ribbed panels.

The BOT (breeder-out-of-tube) approach was selected for the breeder zone arrangement, and the sphere-pac method was selected for fabrication of Li_2O breeder on the basis of the arguments presented in this chapter and in Chap. VIII. The Li_2O breeder is contained by the six sides of the module. The toroidally-oriented coolant tubes in the breeder zone are arrayed in banks; each tube makes a single pass through the breeder. Each tube is fabricated from seamless PCA tubing in the cold worked (CW) condition; inside diameter is 10 mm for each tube, and wall thickness is 0.75 mm. Tube spacing radially and poloidally through the breeding zone is graded in proportion to the local nuclear heating rate, to maintain the Li_2O breeder within the design basis minimum and maximum temperature limits of 410°C and 800°C respectively at all points for normal operating conditions. For each tube, coolant temperature is increased from 280°C at the inlet to 320°C at the outlet by orificing at the tube entrance to produce the proper flow rate/flow velocity

Figure IX.3-53. $\text{Li}_2\text{O}/\text{H}_2\text{O}$ blanket concept preliminary reference design.

TABLE IX.3-34. KEY FEATURES OF PRELIMINARY REFERENCE DESIGN
FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET CONCEPT

- o First Wall: Coolant panel with rectangular-cross-section channels
 - Flow in toroidal direction
 - Cools part of Li_2O Breeder
- o Breeder: Li_2O fabricated using sphere-pac approach
 - Module's six walls provide breeder containment
- o Blanket Cooling: U-bend coolant tubes
 - Flow in toroidal direction, except radial near tube ends
 - Connect to manifold tubes behind rear wall
- o Tritium Removal: Via helium purge gas
 - Flow through sleeve of medium-density metallic felt which surrounds each coolant tube
- o Reflector/Manifold Zone
 - Tubular manifolds connect to U-bend tube ends
 - 4 inlet, 4 outlet manifold tubes per sector
 - Reflector located between inlet and outlet manifold regions--material and cooling TBD
- o Inboard blanket module
 - Vertical module; coolant tubes oriented poloidally
 - No manifolds; coolant tubes connect to headers behind adjacent modules at top and bottom of inboard vertical module

TABLE IX.3-35. MAJOR PARAMETERS FOR $\text{Li}_2\text{O}/\text{H}_2\text{O}$ BLANKET
CONCEPT PRELIMINARY REFERENCE DESIGN

PARAMETER	VALUE
Materials	
Coolant	H_2O
Breeder	Li_2O
Neutron Multiplier	(none)
Structure	PCA Aust. St. Stl.
Purge Gas	Helium
Reflector	PCA Aust. St. Stl.
Average Neutron Wall Load, MW/m^2	5.0
Average First Wall Surface Heat Flux, MW/m^2	0.5
Coolant	
Inlet/Outlet Temperature, $^{\circ}\text{C}$	280/320
Inlet/Outlet Pressure, MPa	15.2/15.0
Mass Flow Rate (FW + B), kg/s	17470 ^e
Pumping Power, % Thermal Output	< 1%
Breeder	
Minimum/Maximum Temperature, $^{\circ}\text{C}$	410/800
Maximum Pressure, MPa	0.1 (Purge gas)
Structure	
Maximum Temperature	550 $^{\circ}\text{C}^a$
Neutronics	
Tritium Breeding Ratio (3-D)	$\sim 1.10^d$
Energy Multiplication Factor	~ 1.27
Energy Lost to Shield, % Total Thermal	~ 0.2
Geometry	
Inboard Blanket Thickness, m	0.30
Outboard Blanket Thickness, m	0.70
Blanket Tritium Inventory, kg	---
First Wall Erosion	
Thickness Allowance, mm	3.0 ^c
Design Life, yr (1-mm/yr erosion)	3
Power Conversion System	
Type	Single wall tube steam generator, no intermediate loop
Gross Thermal Conversion Efficiency, %	34
Steam Inlet/Outlet Temperature, $^{\circ}\text{C}$	240/280

^aSurface of erosion material, beginning of life.

^b15% porosity.

^cMaximum allowable thickness is ~ 6 mm at $50 \text{ W}/\text{cm}^2$, ~ 4 mm at $100 \text{ W}/\text{cm}^2$

^dAssumes 15 cm added to breeding zone thickness for outboard blankets.

^e4000 MWth total FW+B nuclear heating.

combination. The breeder coolant tubes are U-shaped and terminate in the inlet and outlet manifolds located immediately behind the back wall. Figure IX.3-54 shows the proposed dual weld method for the tube ends, which is anticipated to sharply increase the weld reliability of the blanket against water leaks into the breeder.

The manifolds connect all modules within a sector. The present manifold scheme uses four inlet and four outlet manifolds, two of each for each of the two separate cooling circuits. The first wall and all even-numbered coolant tube banks are connected to the first circuit; the first coolant tube bank and all other odd-numbered banks are connected to the second circuit. For the inboard blanket, the large manifold tubes end in headers behind the modules above and below the vertical inboard module. The coolant tubes in this module extend through it vertically and terminate in the manifold headers. Thus there are no large-diameter manifold tubes in the inboard vertical module, sharply reducing the required inboard wall/blanket/shield thickness which results in very significant capital cost savings for the reactor.

Helium purge gas is used to remove tritium from the breeder zone. The present conceptual approach is to use the metallic felt sleeve surrounding each coolant tube as the pathway for the purge gas. The helium is introduced into the sleeve from a narrow inlet plenum at the rear of the blanket. The gas travels through the felt (~60-80% porosity) along the tube length and exits into a similar outlet plenum. Other design options for the purge flow system will be examined later in the study.

Figure IX.3-54. U-bend coolant tube: double weld end method to increase reliability against leaks causing shutdown.

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IX.4 Helium-Cooled, Li₂O Breeder Blanket Concept

IX.4.1 Introduction

The task of selecting and evaluating a helium-cooled, Li₂O breeder blanket design was performed in three phases. Phase 1 was to review existing blanket designs applicable to helium-cooled solid breeder blankets and to make design selections based upon design approaches and configurations. In phase 2, technical evaluations were performed on the designs selected in phase 1 by identifying and considering critical issues with the aim of selecting a reference blanket. Detailed conceptual designs of the selected blanket were then performed in phase 3 by considering first wall, mechanical, breeder element, thermal-hydraulics, and neutronics design aspects. Blanket tritium handling and safety-related issues were also considered.

IX.4.2 Blanket Design Approaches Evaluation and Selection

Twenty existing blanket designs were reviewed, all of which can be classified either as pressurized module or pressurized tube module approaches. The pressurized module designs can further be subdivided into three design approaches which are the tubular, lobular, and canister modules. Examples of each are given in Figs. IX.4-1 through IX.4-4. In general, the tubular and lobular modules are relatively large modules that can be arranged toroidally or poloidally around a tokamak reactor. The canister module is usually a smaller and symmetric module that is oriented radially toward the plasma as shown in Fig. IX.4-4. A typical example for the pressurized tube module is the STARFIRE design which is illustrated in Fig. IX.4-5.

An evaluation was made of the above design approaches by considering the areas of mechanical, neutronics and thermal-hydraulic design, and the design for the tritium extraction system. It was found that meaningful comparison of all approaches on all design issues is not possible at this stage of the blanket design. Therefore, each design area was further divided into distinct design issues where comparison can be made among the four approaches, and

Figure IX.4-1. Tubular blanket design [INTOR/NET⁽¹⁾ - breeder blanket with solid breeder].

Figure IX.4-2. Tubular blanket design (GA-DEMO-1)⁽²⁾.

Figure IX.4-3. Lobular blanket design (GA-EIR)(3).

Figure IX.4-4. Canister - radially oriented blanket designs.(2,4)

Figure IX.4-5. Pressurized - tube blanket module (STARFIRE)⁽⁵⁾.

nondistinct issues where comparison cannot be made because either (1) similar ranking resulted from the evaluation, or (2) design modifications can be made on the designs to reach similar rankings. Design evaluation based upon the identified issues was then made among the four design approaches. The conclusions of this design evaluation is summarized in Table IX.4-1.

It was noted that the design advantages for a canister design can be covered by the other blanket concepts and that the large number of joints and connections could be detrimental to helium-cooling design due to potential leakage problems. Therefore, the canister approach was not evaluated further.

Comparison was made between the lobular and tubular pressurized modules in terms of module configuration, heat transfer design approach, and expected neutronics performance. It was found that both designs can use similar mechanical and thermal-hydraulics design approaches. In order to cut down the effect of neutron streaming, a multi-tube bank will be needed for the tubular design. This would imply the requirement of tube sizes of 20 to 30 cm, thus compounding the mechanical design complexity of the tubular design. From the consideration of material fractions, it was found that the tubular designs will have higher structural fractions and lower breeder volume fractions than the lobular design. Thus, within the blanket thickness guideline of ≤ 70 cm, these differences in volume fractions would reduce the tritium breeding ratio for the tubular designs to approximately 1.1 as compared to 1.2. Because of design complexity and reduced neutronics performance, the tubular design was not selected for further evaluation. The remaining concepts to be evaluated were the lobular pressurized-module and pressurized-tube concepts. Due to the potential drawback of the large number of tubes, which may have an impact on availability, major efforts were devoted to the evaluation and design of the lobular pressurized-module concept and reduced efforts were given to pressurized-tube concepts.

TABLE IX.4-1 GAS-COOLED SOLID BREEDER BLANKET DESIGN APPROACH EVALUATION CONCLUSIONS.

<u>Design Option</u>	<u>Pro</u>	<u>Con</u>
1. Pressurized tubular module	<ul style="list-style-type: none"> • Simple configuration • No joints in chamber 	<ul style="list-style-type: none"> • Complex internal coolant routing • Complicated first wall cooling • Potentially high coolant ΔP • Complex internal fuel design • Potentially small dimension in order to become swelling tolerant
2. Pressurized lobular module	<ul style="list-style-type: none"> • Integrated first wall cooling • Swelling tolerance • Ease of handling contact resistance (plate design) 	<ul style="list-style-type: none"> • Complex internal design
3. Pressurized canister	<ul style="list-style-type: none"> • Simplicity in modularization 	<ul style="list-style-type: none"> • Large number of joints and connections • High structural volume fraction • Complex coolant connections
4. Pressurized tube in breeder	<u>Axial Flow</u>	
	<ul style="list-style-type: none"> • Low pressure module • Swelling tolerance 	<ul style="list-style-type: none"> • Requires separately cooled first wall • Contact resistance • Design complexity in fitting solid breeder among pressure tubes
	<u>Radial Flow</u>	
	<ul style="list-style-type: none"> • Low pressure module • Swelling tolerance • Integrated first wall cooling possible 	<ul style="list-style-type: none"> • Close tube spacing • Number of tubes (many joints) • Contact resistance

4.3 Pressurized Module Mechanical Design

4.3.1 Bellows First Wall Design

The primary mechanical design purpose of the first wall is to provide a boundary between the pressurized coolant and the vacuum of the plasma chamber. At the same time, it has to handle the transmission of thermal power through the wall to the high pressure helium coolant. There are two contributions to the heating of the wall; first, the volumetric power generation due to the neutron and material interaction, and second, a surface heat flux coming from the plasma side. In addition to these effects, the selected structural material has to handle the assumed effects of neutron-induced swelling and other radiation damage under high neutron fluence while being eroded away at a rate of ~ 1 mm/year.

The above task is severe by conventional standards. A service life of two years was selected for the reference design of the $\text{Li}_2\text{O}/\text{He}$ concept. Figure IX.4-6 illustrates the first wall which has resulted from our present studies, being an integral part of the blanket. The first wall itself is a semi-cylinder forming a lobe which is tied back to the structural region behind the blanket. The flat sides of the lobes are pressure balanced by adjacent lobes. Thus, the pressure is contained by pure tension in the wall. Thermal effects can be tolerated because of the comparative flexibility of this wall, as discussed below. To render the wall "soft" in the cylinder-axis direction so that it can tolerate disturbances in that direction, the wall is of corrugated (bellows) form. Dimensions of the cross section are delineated in Section 4.3.2. The bellows dimensions were decided after some iterations, but have yet to be optimized. Similarly, the radius of the lobed semi-cylinder has been set at 15 cm, based upon the trade-offs between first wall thickness and total structure volume fracture and can also be further optimized.

Manufacturing methods for such a wall are worth mentioning. The wall is grooved on both sides by gang milling, this operation being carried out while

Figure IX.4-6. Blanket module first wall arrangement.

the wall is a flat plate. The sides of the lobe are then thinned and the wall finally pressed into lobe form with the corrugations in place.

Various aspects of the performance of such a wall in its various functions are set out below. The pressure carrying capability is derived from a simple tension in the wall. Due to the erosion allowance, this tension causes less stress at the beginning of life than at the end. The stress is finally around 138 MPa (20,000 psi) and was set on the basis of the creep performance of the material for a two year life. Small pressure stresses exist in the bellows due to the convolutions and these are added into the analysis.

Erosion capability is built in by the corrugation being 2 mm thicker than structurally needed on the plasma side. This material is expected to diminish in thickness progressively during the wall life. Bellows side-wall erosion is expected. This could be accommodated by tapering the bellows side-wall thickness. The two-dimensional thermal-mechanical analyses of tapered side-wall design will need to be considered in the future. Only the uniform wall thickness bellows design was considered in this study.

It should be appreciated that thermal changes in dimension only result in stress if constrained. The functional constraints of this wall design do not impose constraints which affect the thermal expansion. The analysis below presents a fairly mature iteration in which, two dimensionally, the hottest material clearly has very low stress levels, as illustrated in the two-dimensional calculation. This manipulation is considered of major importance in this design and probably useable in many other blanket design concepts. Experience indicates that the synthesis and control of stress/thermal patterns for the bellows configuration is entirely feasible even when the section is changing due to erosion. These effects have in the main been investigated two-dimensionally. Due to the physical thickness of the wall caused by the erosion allowance, it has not proven feasible in the other direction to use lobe flexure to accommodate thermal changes in dimensions. The major thermal effect is to tend to cause shortening of the lobe radius, particularly at the point nearest the plasma, and in the simple semi-cylindrical design this gives

rise to considerable stresses. However, by changing the lobe shape from a simple radius to some other shape for just a few millimeters at variance from a radius, it may be possible to arrange moments which counter those which the temperature effects generated lending to a working situation where for practical purposes no temperature stresses exist. This can also be interpreted as pre-loading of the first wall. Indeed, if the simple cylindrical wall is left in this temperature/pressure condition long enough, it will creep to this equilibrium condition, however, in doing so it will "use up" some of its creep limit, which, if embrittlement occurs, may be very important, especially at shutdown when the heat flux is absent.

Irradiation swelling stress in the bellows direction can be fairly easily controlled assuming pessimistically that no creep occurs. It is considered that this should in any case be minimized so as not to deplete the creep budget (as in the thermal case). Swelling stresses in the lobe direction have not yet been analyzed and are more difficult to handle, although they can in some measure be dealt with as the thermal expansion is.

Some general observations on the proposed design can be made. First, the situation with regard to radiation stimulated dimension change under stress, i.e., radiation creep, it is essential to know whether this is damaging. Present postulates say that it may not be damaging, but that on cessation of the radiation, the material will be hardened. In this state, do hardness and brittleness arrive together? If so, if irradiation is stopped, restarting may call for very low subsequent strains due to low creep secondary capability. These questions need resolutions. What is emerging is that whatever their resolution it appears possible to accommodate the effect satisfactorily in this configuration at a reasonable life.

Further work on the first wall will be most productive if the material design criteria can be changed. However, the present design can further be optimized with present design criteria and, in particular, interaction of the lobe and bellows stress systems needs to be done to produce minimum material contents for a required life.

4.3.2 First Wall Thermal-Mechanical Analysis

The bellows first wall concept was proposed as a means of accommodating the temperatures; pressure, thermal, and neutron swelling stresses; and material erosion anticipated for tokamak fusion reactor first walls. The objective of the first wall thermal mechanical analysis was to iteratively manipulate the bellows first wall configuration in such a way as to control the associated temperatures and stresses, keeping both below specified design limits. To accomplish this objective, the two dimensional steady-state temperature distribution at the beginning and end of life was calculated for the bellows first wall, using TACO2D, a finite element heat transfer code. The temperature profile calculated by TACO2D was then coupled along with the helium pressure boundary condition into NIKE2D, an implicit, finite deformation, finite element stress code. The neutron swelling effects were not included in the stress analysis; they will be included in future iterations. The results of TACO2D and NIKE2D were graphically displayed using the post-processors POSTACO and THOR, respectively. Both of these codes and their accompanying post-processors are available on the magnetic fusion energy computer network.

Figure IX.4-7 presents the two dimensional bellows first wall configuration. This basic configuration was analyzed for two conditions: the beginning of life in which the 2 mm sacrificial layer on the plasma side of the wall has not eroded, and at the end of life in which the sacrificial layer has completely eroded. Bellows side-wall erosion could be accommodated by tapering of the wall thickness. This will need to be analyzed in the future. Table IX.4-2 lists the design guidelines used for the analysis. Three surface loading conditions were analyzed. The high surface loading condition is representative of the conditions expected for tokamaks. The lower loading conditions are typical of mirror confinement schemes. Table IX.4-3 summarizes the thermal and structural computer inputs.

The principal stresses calculated by NIKE2D for the bellows first wall were computed assuming a hydrostatic pressure on the helium side of the wall which was coupled with the temperature profile computed by TACO2D. The wall

Figure IX.4-7. Two dimensional bellows first wall configuration.

TABLE IX.4-2. DESIGN GUIDELINES FOR THE BELLOWS FIRST WALL

Material	20% cw PCA stainless steel
Neutron wall loading	5 MW/m ²
Surface wall loading	1 MW/m ²
Maximum sacrificial material temperature	550°C
Maximum structural material temperature	550°C
Maximum stress (primary plus secondary)	390 MPa
Erosion rate	1 mm/yr
Blanket lifetime	2 yr
Equivalent wall thickness ^a	5 mm

^aDetermined from hoop stress calculations for PCA at the end-of-life.

TABLE IX.4-3 THERMAL AND STRUCTURAL COMPUTER INPUTS
FOR THE BELLOWS FIRST WALL

	High Surface Loading	Intermediate Surface Loading	Low Surface Loading
<u>Thermal TACO2D Inputs</u>			
Maximum surface heat flux (MW/m^2) ^a	1.0	0.5	0.1
Volumetric neutron heating (MW/m^3)	55.0	55.0	55.0
Beginning of life helium channel convective heat transfer coefficient ($\text{W/m}^2\text{-K}$)	8599	8241	8058
End of life helium channel convective heat transfer coefficient ($\text{W/m}^2\text{-K}$)	8595	8238	8054
Bottom plate convective heat transfer coefficient ($\text{W/m}^2\text{-K}$) ^b	190	190	190
Beginning of life helium coolant temperature ($^{\circ}\text{C}$)	322	307	300
End of life helium coolant temperature ($^{\circ}\text{C}$)	319	305	297
<u>Structural NIKE2D Inputs</u>			
Helium side hydrostatic pressure, (MPa)	5.0	5.0	5.0
^a The surface heat flux at each nodal point on the plasma side is equal to the fraction of the total angle seen at that point times the maximum surface heat flux. ^b The bottom plate convective heat transfer coefficient is smaller than the helium channel convective heat transfer coefficient because heat flowing through the bottom plate must also flow through the end plate that holds the helium.			

was constrained from moving in the bellows direction along both bellows side interfaces and from moving up and down by a single point located at the wall's center of mass (see Fig. IX.4-7).

Figures IX.4-8 and IX.4-9 graphically display the bellows first wall temperature profiles calculated by TACO2D for a maximum surface heat flux of 1.0 MW/m^2 at the beginning and end of life, respectively. Figures IX.4-10 and IX.4-11 present the beginning and end of life principal stresses calculated by NIKE2D. These figures show that with the present bellows first wall design, the design guideline maximum sacrificial and structural material temperature (550°C) is exceeded. However, the design guideline maximum stress (390 MPa) is met. It is important to note that the maximum stresses occurs along the bellows' bottom plate; the hotter portion of the bellows must accommodate only very small stresses. These small stresses increase slightly from the beginning to the end of life owing to the decrease in the stiffness of the bellows when the sacrificial material is eroded. It should be emphasized that although the design guideline temperatures are exceeded for the highest surface heat flux case with the present design, further modifications to the design by varying bellows geometry are expected to improve the bellows first wall performance.

Figure IX.4-12 shows the effect of decreasing the surface heat flux on the temperature at the center of the top bellow surface and on the maximum tensile stress. As expected, the temperature of the bellows first wall decreases with a lower surface heat flux. The temperature at the beginning of life is higher than at the end of life due to the longer conductance path length. The maximum tensile stress at the end of life is also generally lower, but is about the same at 1.0 MW/m^2 maximum surface heat flux.

These results indicate that the present bellows first wall design meets the thermal and structural design guidelines for maximum surface heat fluxes below 0.5 MW/m^2 . Efforts are under way to modify the present design, especially in the lobe direction to further improve the performance of this bellows first-wall design.

Figure IX.4-8. Beginning of life bellows first wall temperature contours.

BELLOWS FIRST WALL TEMPERATURE CONTOURS (C)
ISOPLLOT AT TIME 1.0000E+00

Figure IX.4-9. End of life bellows first wall temperature contours.

UNERODED BELLOWS FIRST WALL THERMAL AND PRESSURE STRESS
CONTOURS OF MAXIMUM PRINCIPAL STRESS

TIME=1.00E+00
MIN(N)=-9.30E+07
MAX(X)= 2.64E+08

CONTOUR LEVELS

A = -5.73E+07
B = -2.17E+07
C = 0.
D = 4.96E+07
E = 8.53E+07
F = 1.21E+08
G = 1.57E+08
H = 1.92E+08
I = 2.28E+08

Figure IX.4-10. Beginning of life bellows first wall stress contours.

ERODED BELLOWS FIRST WALL THERMAL AND PRESSURE STRESS
CONTOURS OF MAXIMUM PRINCIPAL STRESS

TIME=1.00E+00
MIN(N)=-9.38E+07
MAX(X)= 2.65E+08

CONTOUR LEVELS

A = -5.79E+07
B = -2.20E+07
C = 0.
D = 4.98E+07
E = 8.57E+07
F = 1.22E+08
G = 1.57E+08
H = 1.93E+08
I = 2.29E+08

Figure IX.4-11. End of life bellows first wall stress contours.

Figure IX.4-12. Effect of surface heat flux of temperature and stress.

4.3.3 Structural Design Considerations

In view of the difficult function of the first wall, the emphasis in design of this structure is on relief of the first wall from any structural task other than pressure containment in its operating environment. This environment is such that the structural function of the lobe has been removed as far as possible from the first wall. The blanket structural tasks are to carry the weight of the blanket contents and those parts of the pressure containment not carried by the first wall. The back of the lobe consists entirely of a structural frame (see Fig. IX.4-13) with gas passages, and the lobe ends, which cantilever from the back to carry the end pressures, relieve the bellows first wall of end load constraints due either to pressure, swelling, or thermal effects.

The reasons for this design are first, relief of the first wall, second, protection of the structure from radiation, and third, the avoidance of structure in front of the blanket which would attenuate the neutron flux. Another advantage comes from this design in that requirements for highly refined designs can be avoided since excess metal merely acts as part of the shield, thus the plenum ducts are capable of being of close-packed rectangular form because comparatively inefficient structural forms, i.e., beams, do not intrude into the blanket function. In addition to the structure shown, the lobes are held against each other and supported by a dee-shaped frame to conform to the tokamak shape and take loads due to the noncircular form, gravity, and earthquake accelerations. This frame can be embedded in the shield and should be considered in detail in the future.

4.3.4 Tritium Purge Flow - Mechanical Design

It is necessary to have a purging gas stream flow through the plate assemblies to sweep out bred tritium for the reactor fuel supply. A stream entirely separate from the coolant, 1 atm pressure, has been chosen.

Figure IX.4-13. Lobe structure without first wall.

A circuit form which allowed some of the coolant to permeate the plates and be drawn off for tritium recovery was considered, but rejected on the grounds that should the helium coolant become water contaminated due to steam generator leaks, lithium hydride formation can potentially be a problem. One of the reasons for considering this system was that the connection between the main coolant space and the plate interior was good, thus in the event of sudden depressurization of the blanket the plates would not be damaged by the unbalanced internal pressure. This was also the reason for not selecting a system with the plate interior at the coolant pressure.

The selected system has the purge flow at a low pressure, about 1 atm. Thus in a coolant pressure loss situation, the pressure in the plate will not be high enough to disrupt the clad. A further important advantage is that since the plate, during operation, is firmly clamped by the coolant pressure, the thin clad skin will be very efficiently stabilized against buckling, giving good cooling reliability and a very low structural material fraction. In a high nuclear heating rate situation when the Li_2O plate is thin in order to maintain the temperature limits, the plate clad may constitute as much as 6% of the solid material, this is an important consideration.

It will be necessary with the system chosen to show a high degree of integrity against leakage since the coolant will inevitably tend to permeate into the plates. Both experiment and experience will be necessary to set acceptable standards for this leakage. A comparatively simple low cost addition to the tritium purge system would be to make it have dual pressure capability, thus in normal operation, the plate interior would be at low pressure, but if a wet situation arose, the tritium purge system could be temporarily balanced to the coolant pressure, giving an acceptable short-term risk until the coolant was dried out again. In the high pressure mode, the tritium process stream need not be pressurized, but the cleaned-up helium stream would require pressurization before reinsertion and the outflow would require regulation to atmospheric pressure.

4.3.5 Plenum Design

Two plenum designs are shown in Fig. IX.4-14. The thicknesses shown represent the actual dimensions required for flow around the torus cross section. A practical design based upon this layout will have two areas of departure from these idealized dimensions.

Some small differences can be expected in that the idealized gas space is allocated between out and return. Inevitably the detail at any point of the flow from the out to the return line through the blanket must contain space for the distribution across the blanket (i.e., toroidally). A few centimeters will probably cover this.

In addition, the practical details of the structure will involve occupation of some space at the rear of the blanket by structural members with consequent increase of plenum thickness.

The consequence is that the realistic figures for plenum thickness should, when all factors are considered, be increased by about 3 to 5 cm from those shown in the diagram. An optimization is conceivable in which the rear structure penetrates the lithium zone, simultaneously losing mass as it does so, but decreasing the TBR by a small amount. This penetration would allow a thicker blanket to compensate for the TBR loss.

4.4. Fuel Element Design

The lithium oxide-based breeding element design consists basically of radially oriented plates closely stacked together and mounted at their outer corners to the module strongback structure. The plates will be 1 to 2 cm thick, depending on the wall loading, with 1 mm gaps to pass the coolant.

The plates will consist of lithium oxide tesserae, the characteristic dimensions of which will be decided on the basis of manufacturability and tritium permeation. Figure IX.4-15 illustrates the arrangement. These will be clad in 0.25 mm thick stainless steel sheet welded closed or in a series of rectangular tubes assembled to form a plate. Special attention is paid to

THRU STREAMS

STREAM LOCATION THICKNESS cm

1	A	10
1	B	10
2	A	18
2	C	18

MIXED STREAMS

STREAM LOCATION THICKNESS cm

1	A	10
1	B	0
2	A	18
2	D	18
3	B	0
3	C	10

Figure IX.4-14. Coolant-based plenum sizes.

PURGE FLOW SYSTEM

Figure IX.4-15. Purge flow system.

swelling since the thin plates used are prone to buckling. The plates are wire wrapped to maintain the 1 mm gap dimension. Details of the plate bundle and how they are held in place inside the module will need to be designed in the future.

The plates are held together by the tritium purge attachment points. A sleeve is installed in four places in the outer corner of each plate, the clad being drilled where this sleeve is fitted. A tie tube with suitable perforations in its sides is tightened through the bank of plates by threads at its ends. Between the plates, a soft crush washer is fitted and correct tensioning of the tie tube results in the tie tube being sealed to each plate. Since this constitutes a substantial member, it was decided to use it as the principal mounting point for the plates. The position of the connections at the near corners gives the shortest possible tritium purge system and allows the swelling of the plates due to irradiation to take place with minimal restraint, thus causing low swelling stress levels.

4.5 Thermal-Hydraulics Design

4.5.1 General Considerations

The thermal-hydraulic design of a gas-cooled reactor system should have high thermal efficiency and low pressure losses. The high efficiency requirement dictates a high coolant outlet temperature, restricted by the maximum operating temperature limits of the reactor materials. The low pressure loss requirement leads to high system operating pressure to obtain high coolant density, a large coolant inlet-to-outlet temperature differential, and restricts the velocities of the coolant in various sections of the coolant loop. On the other hand, the restrictions on material operating temperature limits lead to high coolant velocities to maintain high heat transfer coefficients. During the course of the blanket thermal-hydraulics design, close interaction was maintained with the mechanical design, neutronics analysis and material selection efforts.

4.5.2 Temperature and Pressure Drop Limits

The temperature limits of different materials crucial to the design of the blanket must be established. At the same time, the pressure drop limits for different regions of the blanket will need to be defined to establish a basis for the thermal-hydraulic design and analysis. In the blanket region, the crucial materials are the structural material and the fuel materials. In the blanket locations where the material strength is important and radiation levels are high, PCA was recommended to be the structural material which has a maximum allowable temperature of 550°C. Typically, the reactor first wall would experience the highest flux of high energy neutrons, thus this temperature limit would be applicable to the heat transfer analysis of the first wall. The temperature limits for Li₂O as specified by the solid breeders task group are $T_{\min} = 410^{\circ}\text{C}$ and $T_{\max} = 800^{\circ}\text{C}$ (1000°C for design approaches with helium purge gas flow directed only to the cold region of the breeder).

Low coolant pressure drop in a power producing machine is important in reducing the power necessary to circulate the helium coolant. The acceptable pressure-drop depends on the over-all optimization of the reactor economics. At this stage of the conceptual design, two pressure drop limits can be used. A value of approximately 5% of the thermal power can be used for the pumping power for the complete coolant circuit, which includes the heat exchangers, the blanket, pumps, and piping. The second limit is imposed by the pressure differential capability of the helium circulator. Using a single-stage circulator, a value of $(\Delta P)/(P) < 4.3\%$ can be used for the entire coolant loop.

Experience with helium-cooled nuclear power systems indicates that a helium pressure of 40 to 80 atm will be needed for a good thermal-hydraulic design. Steam-generator design conditions dictate a minimum coolant inlet temperature of about 275°C and a minimum coolant temperature rise of above 100°C. Based on this information, a helium operating pressure of 5.1 MPa (50 atm) and an inlet temperature of 275°C were selected. For the Li₂O blanket, an outlet temperature of 500°C was selected to give a thermal cycle efficiency of 39%.

4.5.3 First Wall Cooling

Thermal-hydraulics calculations were performed on the first wall, that section of the reactor which is closest to the plasma and which has the highest volumetric heat generation and surface heat flux. In order to design for a neutron wall loading of 5 MW/m^2 and a surface heat loading in the range of 0.1 to 1 MW/m^2 , the bellows first wall configuration, as illustrated in Fig. IX.4-6 was used for the analysis. This is necessary in order to increase the heat transfer surface area and the heat transfer coefficient. This configuration is also conveniently coupled with the mechanical design requirements as presented in Section IX.4.3.1. The key results of this analysis are the bellows detailed dimension, coolant heat transfer coefficient, and temperature distribution. These were then used as inputs for the two-dimensional thermal-mechanical analyses as presented in Section IX.4.3.2. The equations used to calculate the one-dimensional temperature distribution in the wall were presented in Ref. 6. The first wall coolant is separated from the module internals by a closely attached metal wall in order to maintain the high coolant velocity in the first wall.

Table IX.4-4 gives the parameters for the selected bellows first wall design.

4.5.4 Fuel-Plate Thermal-Hydraulic Design

Thermal-hydraulic calculations were performed for the plate design of the helium-cooled Li_2O breeder blanket. Figure IX.4-16 shows a schematic of the breeder plate design with coolant flowing through the 1 mm breeder plate gaps from the bellows first wall distribution plenum to the outlet manifold. The equations used in this analysis were presented in Ref. 7. The plate design characteristics are summarized in Table IX.4-5. The temperature distribution through the breeder plate is given in Fig. IX.4-17. It can be noted that the structural and breeder plate material temperature limits are met with this design. Even with consideration of the hot spot factors, only a small portion of the plates ($< 5 \text{ cm}$) exceed the temperature limit of 1000°C .

TABLE IX.4-4. PARAMETERS OF THE BELLOWS FIRST WALL DESIGN.

Parameter	Values - Units
Neutron wall loading	5 MW/m ²
Volumetric heat generation	50 MW/m ³
Surface heat flux range	0.1 to 1 MW/m ²
Equivalent wall thickness	6.3 cm (BOL) ^a , 5 mm (EOL)
Inside (coolant) groove width	1.2 mm
Outside (plasma side) groove width	2.4 mm
Bellows wall thickness	1.6 mm
Inside groove height	7.2 mm
Outside groove height	9.2 mm (BOL) 7.2 mm (EOL)
Helium pressure	1.5 MPa (50 atm)
The following are for surface wall loading of 1 MW/m ²	
Helium coolant maximum temperature, °C	322°C
Reynolds number	16,377
Coolant velocity	54 m/sec
Pressure drop	9.2 kPa (1.34 psi)

^aAt the beginning of life (BOL), a 2 mm thick layer is added on for erosion requirements.

Figure IX.4-16. Helium coolant flow path in the blanket.

TABLE IX.4-5. Li_2O PLATE DESIGN CHARACTERISTICS.

● Selected Inputs		
Contact heat transfer coefficient, $\text{W/m}^2\text{-K}$ ($\text{Li}_2\text{O}/\text{He}/\text{PCA}$)		3000
Thermal conductivities, W/m-K ($\text{Li}_2\text{O}/\text{PCA}$)		3.4/19
● Reference Design Geometry		
Plate thickness, cm		1.7
Clad thickness, mm		0.25
Coolant flow gap width, mm		1.0
Plate length, cm		52
Number of plates/module		16
● Breeder plate temperatures		
Centerline Li_2O , max		986 (1167) ^a
Surface Li_2O , min/max		508/508
Cladding, PCA		505

^aHot spot factors included.

Figure IX.4-17. Li_2O plate temperature distribution.

4.5.5 Coolant Pressure Drop

Calculations were performed to estimate the total pressure drop of the whole blanket cooling circuit including the steam generators. The pressure losses due to friction, acceleration of flow from density change as a function of temperature, joints, turns, expansions, and contractions are all taken into consideration. Table IX.4-6 summarizes the friction pressure drop from different blanket sections.

The total pressure drops in the blanket and in the primary coolant are given in Table IX.4-7.

It should be noted that both $\Delta P/P = 3.4\%$ and pumping power fraction of 4% are within the design limits of 4.3% and 5%, respectively, as stated in Section 4.5.2.

TABLE IX.4-6. FRICTION PRESSURE DROP FOR THE DIFFERENT BLANKET SECTIONS.

Section	Flow Velocity V (m/s)	Pressure Drop ΔP (kPa)
1. Inlet manifold	55	9.1
2. Distribution channel	32	1.3
3. Side flow path	38	14.7
4. Grooved first wall	54	9.2
5. Breeder plate gaps	33	16.3
6. Collection channel	16	1.9
7. Outlet manifold	77	12.0
Total		64.5

TABLE IX.4-7. TOTAL PRESSURE DROP IN THE BLANKET.

Pressure Drop	ΔP (kPa)	$\Delta P/P$ (%)
Friction	64.5	1.1
Turning, joining, and dividing	45.6	0.9
Expansion/contraction	11.6	0.2
Total	121.7	2.2
Total Pressure Drop in the Primary Coolant Loop		
Blanket	121.7	2.2
Sector lines, 22.3	61.1	1.2
Ring ducts, 4.3		
Steam generator piping, 7.1		
Steam generator, 27.4		
Total	182.8	3.4
Total pumping power fraction		
= loop pumping power/reactor thermal power $\times 100\%$ = 4%		

4.6 Neutronics Design

The neutronic calculations were performed for the solid breeder helium cooled blankets using the same techniques and models described in Section VII.2.1.6. The breeding zone of the solid breeder blanket consists of 6% PCA, 85% Li_2O , and balance of helium. The density factor of the Li_2O pellets used in all calculations is 80%.

Figure IX.4-18 shows the tritium breeding ratio, ${}^6\text{Li}(n,t)(T_6)$ and ${}^7\text{Li}(n,n't)(T_7)$ for the helium-cooled solid breeder (Li_2O) blankets as a function of breeding zone thickness at several ${}^6\text{Li}$ enrichment values in lithium. This figure reveals that at natural ${}^6\text{Li}$ enrichment (7.4% ${}^6\text{Li}$ in lithium), the breeding ratio becomes greater than 1.0 when the breeding zone thickness exceeds about 0.23 m. It then becomes attractive (greater than 1.2) when the breeding zone thickness is more than 0.39 m thick. The enrichment of ${}^6\text{Li}$ in lithium does not increase the tritium breeding at all when the breeding ratio exceeds 1.2 in the blanket, as also shown in Fig. IX.4-18 This is primarily due to the tradeoff between T_6 and T_7 .

Table IX.4-8 gives the tritium breeding ratios and blanket nuclear heating rates for several 0.7 m blanket designs varying the ${}^6\text{Li}$ enrichment, reflector type (stainless steel or graphite) and thickness, and shield type (stainless steel or graphite). It shows in this table that the tritium breeding ratio is about 1.22 when there is no reflector in the blanket and it is not sensitive to the type of shield, stainless steel or graphite. The blanket energy multiplication is greater than 1.2 for all designs. Although the graphite shield design may offer slightly higher blanket energy multiplication, it may require a thicker shield to protect the superconducting magnet.

The inboard and outboard blanket thickness tradeoff study was performed using the one-dimensional major radius torus model described in Section VII.2.1.6. The results are shown in Table IX.4-9. As shown in this table, with an inboard blanket thickness of 0.5 m, the tritium breeding ratio of 1.22 can be obtained if the outboard blanket is 0.9 m thick.

Figure IX.4-18. Tritium breeding ratio, $^7\text{Li}(n,n'\text{t})(\text{T}_7)$ and $^6\text{Li}(n,\text{t})(\text{T}_6)$ for solid breeder (Li_2O) helium-cooled blankets as a function of breeding zone thickness at 7.4%, 15%, and 20% ^6Li in lithium.

TABLE IX.4-8. TRITIUM BREEDING RATIOS AND NUCLEAR HEATING RATES IN HELIUM-COOLED SOLID BREEDER (Li₂O) FUSION BLANKETS.

	Option												
	1			2			3			4		5	
First Wall (PCA)	60 mm (6.6% dense)			60 mm (6.6% dense)			60 mm (6.6% dense)			60 mm (6.6% dense)		60 mm (6.6% dense)	
Li ₂ O zone	0.42 m			0.3 m			0.3 m			0.3 m		0.42 m	
Reflector	None			0.12 m Stainless Steel			0.12 m Graphite			0.12 m Graphite		None	
PCA plenum	0.22 m (10% dense)			0.22 m (10% dense)			0.22 m (10% dense)			0.22 m (10% dense)		0.22 m (10% dense)	
First 0.3 m shield	Stainless Steel			Stainless Steel			Stainless Steel			Graphite		Graphite	
⁶ Li enrichment (% in Li)	7.42	15	20	7.42	15	20	7.42	15	20	7.42		7.42	
⁶ Li(n,α)	0.829	0.868	0.880	0.75	0.803	0.822	0.772	0.816	0.831	0.763		0.816	
⁷ Li(n,n'-α) ^a	0.390	0.355	0.333	0.37	0.338	0.316	0.371	0.339	0.317	0.371		0.414	
TBR	1.219	1.223	1.213	1.12	1.141	1.138	1.143	1.154	1.148	1.134		1.230	
Blanket nuclear heating	7.16	17.20	17.22	17.67	17.56	17.53	17.04	17.05	17.05	17.10		17.56	
(E/14.1 Mev)	(1.22)	(1.22)	(1.22)	(1.25)	(1.25)	(1.24)	(1.21)	(1.21)	(1.21)	(1.21)		(1.25)	

^a10% reduction in the ⁷Li(n,n'α) reaction rate was taken into account.

TABLE IX.4-9. CALCULATED RESULTS OF THREE DESIGNS VARYING INBOARD AND OUTBOARD BREEDING ZONE THICKNESSES.

	Design A	Design B	Design C
Inboard breeding zone thickness	0.42 m	0.22 m	0.22 m
Outboard breeding zone thickness	0.42 m	0.42 m	0.62 m
Tritium breeding (T/neutron)			
${}^6\text{Li}(n, \alpha)$	0.839	0.801	0.839
${}^7\text{Li}(n, n'\alpha)^a$	0.378	0.373	0.378
TBR	1.217	1.174	1.217
Nuclear heating (MeV/neutron)			
Blanket	17.06	16.77	16.98
Total	18.02	18.19	18.06

^a10% reduction of the ${}^7\text{Li}(n, n'\alpha)$ reaction rate was taken into account.

It is of interest to explore the tritium breeding potential of the Li_2O breeder when a neutron multiplier is incorporated in the blanket. We performed neutronic calculations for the Li_2O blankets employing beryllium as the neutron multiplier for the purpose of tritium breeding investigation. Figure IX.4-19 shows the one-dimensional schematic of the neutronic model. In this design, the beryllium zone consists of 4% PCA, 52.8% beryllium (80% dense), and balance of helium, all by volume. Figure IX.4-20 depicts the tritium breeding ratios and blanket nuclear heating rates for blanket design with 0.5, 0.6, and 0.7 m total blanket thicknesses and as a function of beryllium zone thickness. Note that only naturally enriched Li_2O is considered in this study.

	4 cm	1.5 mm	X cm	1 cm	Y cm	22 cm	30 cm
PLASMA AND VACUUM	PCA	PCA	4% PCA 52.8% Be ^a 43.2% He	PCA	6% PCA 85% Li ₂ O ^a 9% He	PCA PLENUM	STAINLESS STEEL
DENSITY FACTOR	0.066	0.6		0.024		0.1	1.0

^a80% dense beryllium balls (rods, or plates)/Li₂O plates.

^bTotal blanket thickness = X + Y + 27 (cm).

Figure IX.4-19. Neutronic model (one-dimensional) of a helium-cooled beryllium-multiplied Li₂O blanket.

Figure IX.4-20. Tritium breeding ratio and blanket nuclear heating for beryllium multiplier/Li₂O helium-cooled blanket designs as a function of beryllium zone thickness at 0.5, 0.6, and 0.7 m blanket thickness.

From Fig. IX.4-20 we see that the maximum tritium breeding ratios can be obtained at about 0.1 m beryllium zone thickness. The tritium breeding ratio decreases when the beryllium zone thickness becomes thicker than 0.1 m because of excessive neutron parasitic absorption in the structure of the beryllium zone. As noted from Fig. IX.4-20, the maximum tritium breeding ratios are 1.5 and 1.38, respectively, when the total blanket thicknesses are 0.7 and 0.6 m. The corresponding blanket nuclear heating rates are 19.2 ($M_B = 1.36$) and 18.3 ($M_B = 1.30$) MeV per D-T neutron, respectively.

4.7 Blanket Tritium Inventory and Migration

For the helium-cooled Li_2O breeder blanket design, the bred tritium is extracted by a purge stream of helium through the solid breeder plates. This tritium extraction option allows positive control of tritium and impurity levels in the purge helium stream. Both of these have direct impact on blanket tritium migration and inventory. The purge flow loop was selected to operate at a helium pressure of 1 atm versus the main helium coolant pressure of 50 atm to reduce the effect of clad-to-breeder contact resistance and to prevent distortion of the breeder cladding in the case of blanket module depressurization. This section presents the description of the TRIT4 code which was used for the calculation of tritium inventory and migration in the Li_2O solid breeder blanket.

The release of tritium generated in the solid breeder to the extraction system (purge gas) is a critical concern for tritium recovery and inventory. The TRIT4 code developed at GA Technologies models the migration and inventories of tritium in the breeder, purge helium loop, coolant loop, and steam generator loop at equilibrium.

The model assumed in the breeder is as follows:

1. Bulk diffusion from the solid breeder grains (100% theoretical density) to the grain boundaries.

2. Release from grain boundaries to open pores in the breeder pellet occurs much faster than the mean bulk diffusion time.
3. Porous diffusion in the pellet out to the surface of the pellet into the purge stream. (Breeder in tube concept.) Only the effect of concentration diffusion was used in the porous diffusion calculation. The temperature effect on porous diffusion was found to be negligible and the pressure gradient effect on diffusion was neglected as a conservative measure. The release of T_2O from the grains and porous diffusion through the purge gas in the pores is included in the calculation. The appropriate mass transport properties are calculated allowing for Knudsen, transition, or ordinary diffusion.
4. Release from the breeder pellet to the purge gas stream.

The model calculates the temperature profile in the breeder pellet and the pressure and concentration distributions of T_2O in the pores of the breeder. The temperature and pressure distributions are used in the calculation of solubility and bulk diffusion inventories. Experimentally determined activity coefficients⁽⁸⁾ were used in the determination of breeder solubility inventory of $LiOT$ in Li_2O as a function of temperature. The pellet is assumed representative of the entire breeder and therefore, an average volumetric heating rate and an average volumetric tritium generation rate are used in the calculation.

The inventories and tritium permeation release rates for the coolant and purge gas loops are calculated using appropriate permeation constants and the total surface area seen by each loop.

The input data are available from the neutronics calculations performed for the helium-cooled, Li_2O breeder blanket design. The key input/output parameters for the TRIT4 codes are presented in Table IX.4-10. It can be noted that the blanket total tritium inventory is calculated to be

1.82 kg/GW_{th} and the tritium leakage into the steam generator for a 4000 MW_{th} reactor is 0.7 Ci/day with an assumed effectiveness of 100 for the oxide barrier on the clad walls and an oxygen partial pressure of 0.1 torr into the purge stream. Details of the T₂, T₂O conversion in the vicinity of metallic clads still need to be considered. Both the tritium inventory and leakage rate are acceptable and within the design limits of the BCSS.

TABLE IX.4-10. SELECTED INPUTS/OUTPUTS FOR THE Li₂O BLANKET TRITIUM MIGRATION AND INVENTORY

Inputs:		
Fuel geometry		Plate (1.7 cm thick)
Purge helium pressure		0.1 MPa (1 atm)
Li ₂ O grain size (100% dense)		100 μ m
Pellet packing fraction		85%
Volume flow rate fraction of the main coolant		0.1%
Oxide barrier effectiveness		100
Tritium extraction efficiency		99%
Outputs:		
Purge stream T ₂ O pressure		91.2×10^{-6} atm
Total breeder tritium inventory		1.8 kg/GW _{th}
Total blanket tritium inventory		1.82 kg/GW _{th}
Coolant heat exchanger permeation rate		0.7 Ci/day for 4000 MW _{th} reactor

4.8 Safety Considerations

The objectives of this section are to identify and highlight those aspects of the Li_2O , helium-cooled blanket which are of safety interest, and to indicate how safety is satisfactorily addressed by the present design. Since the structure and breeder source term, risk, occupational exposure, and waste disposal concerns will be addressed as part of future efforts, and tritium inventories and leakage rates were presented in the previous section, this section presents a discussion of the fault tolerance of the present design. Events considered include submodule depressurization, leakage of helium into the breeder plates, loss of coolant (LOCA), loss of flow (LOFA), and loss of site power (LOSP).

The major accident concern in the present blanket is depressurization of a submodule lobe, possibly as a result of external forces or near-blanket system interaction. The major uncertainties are the survivability of the configuration in a depressurization event, and the subsequent thermal response and cooling concerns. The analysis of a first-wall, burst scenario has been directed at bracketing the extent of the problem. Consider first the coolant pressure communication of adjacent lobes. Figure IX.4-21 presents a lobe interconnection diagram representing the volumes and distances available for flow in one meter of adjacent submodule lobes. It can be shown that pressure balancing between the lobes will be rapid, on the order of milliseconds, and thus no domino effect will occur. If it is assumed, however, that the worst case scenario applies and no communication between the lobes exists, the lobe contents (i.e., plates) are designed to take the full pressure loading from adjoining modules with less than one millimeter distortion in the lobe membranes. Thus, no cascading of the failure is foreseen for the present blanket. More detailed analysis will need to be performed in the future.

Minor leakage of the primary helium into the Li_2O breeder would not result in any appreciable level of breeder-coolant interaction due to the low levels of impurities in the helium.

Figure IX.4-21. Lobe connection diagram presenting the volumes and distances for 1 m of two adjacent lobes.

The remaining safety concerns addressed here are the survivability of the modules with respect to LOCA, LOFA, and LOSP. Though no transient thermal analyses were performed for the specific blanket configuration presented in this section, valuable insight into the expected temporal temperature behavior can be obtained from recent analyses⁽⁹⁾ performed for a similar helium-cooled blanket⁽¹⁰⁾ with the same first wall configuration, but with a $^{17}\text{Li}^{83}\text{Pb}$ breeder tube bank and a helium coolant outlet temperature of 700°C . The blanket of Ref. 9 additionally incorporates a nonstructural, SiC high-temperature zone which operates in the 1200°C to 1500°C range. Detailed two-dimensional, finite difference thermal analysis of LOCA, LOFA, and LOSP using temperature-dependent correlations for all critical physical properties were performed for that blanket and are summarized below.

In the event of primary coolant system depressurization, one of the major advantages of helium cooling is that pumping can still be provided by the main circulators or auxiliary circulators operating at atmospheric condition. The net effect of depressurized cooling is illustrated in Fig. IX.4-22 which shows that the first wall and breeder structure remain below $\sim 800^{\circ}\text{C}$ and may be able to maintain structural integrity. Note that the breeder region in the figure was initially at a maximum temperature of $\sim 800^{\circ}\text{C}$, whereas the maximum temperature in the present blanket's breeder zone structure is $\sim 500^{\circ}\text{C}$. Note also that the maximum sidewall temperature in the figure is driven by the high-temperature zone, which is not present in the current design.

Under the pressurized conditions that would exist during a LOFA, it has been shown that natural circulation of the pressurized helium can provide adequate passive cooling of the blanket in an HTGR-type of geometry.⁽¹¹⁾ This advantage should be made use of in the future detailed design of the module safety system. It is noted that minimal diversity and redundancy need to be incorporated into the primary pumping circuitry to minimize the probability of this event.

Figure IX.4-22. Fault tolerance to LOCA: blanket temperature response with depressurized cooling (reproduced from Ref. 9).

In the event of LOSP, natural circulation of the pressurized helium may provide adequate cooling as described above. If it is further postulated that the primary system depressurizes, it is estimated that the blanket can tolerate total absence of cooling for ~4 h without melting. This estimate is based on thermal analysis performed for the Li_2O blanket of Ref. 12 shown in Fig. IX.4-23. Note that a very small supply of stand-by auxiliary on-site power would provide the circulation requirements under depressurized conditions, further lowering the probabilities of total absence of cooling.

Figure IX.4-23. Fault tolerance to LOSP: centerline material temperature versus time for the Li_2O blanket of Ref. 12. No cooling of blanket.

4.9 Conclusions and Recommendations

In the reference helium-cooled Li_2O -breeder pressurized module design, an integral first wall is used with full flow of the cool inlet helium directed to the first wall. The helium then turns and flows radially outward through the Li_2O fuel elements. This arrangement allows all the material temperature limits to be met while still achieving a high outlet temperature (500°C) and high net power conversion efficiency (36.5%).

With the relatively optimistic erosion rate of 1 mm/hr, and the restrictive temperature limit of 550°C for PCA (even when used in a nonstructural model), the bellows first wall can satisfy a surface loading of 0.6 MW/m^2 with a corresponding neutron wall loading of 5 MW/m^2 . For the present design, a sacrificial layer of 2 mm of PCA is included on the plasma side of the first wall to give a blanket life-time of two years. The bellows configuration of the first wall is designed to accommodate neutron-induced swelling without creating large stresses, while maintaining a thin structural member with extended heat transfer surfaces. The first wall can be the most critical issue for tokamak blanket designs. Further quantification of the first wall erosion rate and the acceptable range of surface temperature and wall loading at the corresponding neutron wall loading are needed. In the area of thermal-mechanical analysis, the bellows geometry will need to be optimized and the effects of thermal and radiation-induced creep included. The bellows side wall erosion must be quantified. The erodable layer may need to be grooved in the lobe direction in order to reduce thermal stresses. The manufacturing techniques for the bellows first wall will need to be demonstrated.

To achieve the maximum Li_2O density in the blanket, a flat plate fuel element geometry was adopted. Li_2O sintered pellets are clad in 20% CW, PCA to form plates. Plate spacing is maintained by a 1 mm diameter wire wrap around the plates. The coolant flows through the 1 mm coolant gaps between breeder plates and maintains the Li_2O temperature distribution within its

specified temperature limits. The fuel clad design has three functions. First, the clad is used to contain the solid breeder and to minimize the potential effects of fuel cracking due to thermal stresses or irradiation. Second, it is used to separate the main coolant from the purge helium extraction flow in order to maintain positive control of coolant impurities. Third, it can provide a pressure differential between the purge and main coolant streams in order to minimize the effect of thermal contact resistance between the clad and the breeder. The question of flow gap nonuniformity due to irradiation-induced swelling or sintering of the Li_2O needs to be addressed. The quantification of thermal contact resistance between the Li_2O and the clad will also need to be verified.

Thermal-hydraulics concerns for the concept center mainly on the breeding zone and manifolding. The low volumetric heat capacity of helium makes analyses of sensitivity to changes in flow conditions (e.g., flow gap sizes) especially important. The requirements for expansion/contraction zone depths at top and bottom of the breeder plate region will directly affect the blanket thickness requirements. The manifold flow pattern selected for the sector and the manifold flow passage requirements will also directly affect the blanket thickness. Because of the importance of the inboard blanket thickness in a tokamak, the present flow passage requirement calculation should be verified and optimized.

A blanket tritium code that accounts for the solid breeder tritium inventories from different contributions (e.g., bulk diffusion, solubility, porous diffusion, etc.) and from different blanket zones (e.g., purge stream, main coolant stream, and claddings and pipings) was used to calculate the tritium inventory. Using the experimentally determined activity coefficient of T_2O in Li_2O , the normalized tritium inventory with 90 appm T_2O in the purge flow helium stream was calculated to be $1.8 \text{ kg/GW}_{\text{th}}$. This inventory can be reduced by increasing the purge flow volume flow rate.

Based upon three-dimensional neutronics calculations, the tritium breeding ratio of the Li_2O -helium-cooled blanket was calculated to be 1.09 and

1.14 when the limiter was cooled by water and helium, respectively. With the selected coolant inlet/outlet temperatures of 275°/500°C and using helium at 50 atm, after accounting for all the pressure losses in the helium coolant circuit, including the steam generator, the net blanket power conversion efficiency was calculated to be 36.5%.

The present design offers many inherent safety features. In a depressurization event, the design allows for rapid communication between submodules and can withstand the maximum forces expected without failure propagation to adjacent units. With gas cooling, the capability exists to circulate the depressurized coolant for heat removal during accident conditions and thus prevent module failure. Under complete loss of cooling events, the blanket offers large heat capacity for handling afterheat. More detailed calculations of the blanket safety system behavior are needed for further quantification of the safety characteristics.

In summary, the helium-cooled Li_2O blanket design appears to be quite attractive. The basic design criteria can be satisfied and the blanket tritium breeding and thermal performance appear reasonable. Additional design, analysis, and optimization are needed in several areas to allow the evaluation indices to be determined. The most severe critical issues for this design appear to be the understanding, quantification, and accommodation of plasma erosion effects to the first wall, and materials irradiation effects to the structural materials and the Li_2O solid breeder.

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IX.5 Water Cooled, Lead Multiplier, Lithium Aluminate Breeder Blanket

IX.5.1 Introduction

The combination of a lithium aluminate breeder, lead multiplier, and water coolant has been chosen for study because of the individual advantages of each of these three materials relative to their alternatives. Ternary oxide tritium breeders are characterized by chemical stability and low tritium holdup. Lithium aluminate has been chosen because it has good tritium release and a wide operating temperature range in comparison with the other ternary oxides (see Section VIII and Table 1). Its disadvantage, a lower lithium fraction, is useful as a bounding case because a concept that will breed sufficient tritium with lithium aluminate will work with other ternary oxides. (The design considered here is not neutronicly sensitive to the choice of ternary oxide). All ternary oxides require neutron multipliers to obtain adequate tritium breeding ratios. Beryllium is the best element for neutron multiplication in fusion reactor blankets; however, it is a limited resource and thus may not be suitable for long term service in a fusion economy. Therefore, the second best fusion neutron multiplier, lead, has been chosen for examination. An additional objective of this design is to determine if lead is a viable multiplier for fusion. The primary advantages of water coolant are its excellent heat transfer properties and the current extensive development of water cooled technology. Other advantages and disadvantages of lithium aluminate, lead, and water cooling will be discussed throughout the rest of this section.

The purpose of this design feasibility study is to attempt to determine if water cooled, lead multiplier, ternary oxide breeder fusion blankets are technologically attractive and, if so, to provide enough design definition to adequately compare the resulting blanket with other candidates. The approach has been to address the major issues and attempt to create a design that resolves all of them as nearly as possible, with the intent of providing a basis for comparison with other blanket concepts and judging the desirability (if not the feasibility) of this blanket. Major design goals are: adequate tritium breeding (full coverage TBR > 1.2), good thermal to electrical conversion (compatible with LWR power cycles), a credible mechanical design, and safe containment of high pressure (2200 psi) water.

TABLE IX.5-1 CANDIDATE TERNARY OXIDE SOLID BREEDER MATERIALS

Breeder	MP °C	Li Atom Density, g/cm ³	Temperature Range, °C	Tritium Dissolution	Thermal Conductivity, W/m°C
LiAlO ₂	1610	0.27	300 - 1200	Moderate	2.2
Li ₅ AlO ₄	1047	0.61	350 - 780	High	2.3
Li ₂ SiO ₃	1200	0.36	410 - 1000	High	1.5
Li ₄ SiO ₄	1250	0.54	320 - 950	Moderate	1.5
Li ₂ ZrO ₃	1615	0.33	400 - 1400	Moderate	1.3
Li ₈ ZrO ₆	1295	0.68	340 - 980	Moderate	1.5
Li ₂ TiO ₃	1550	0.33	400 - 1185	Moderate	2.0

IX.5.2 Configuration

Constraints are imposed on the possible configurations of this blanket by the choice of coolant, breeder, and multiplier. Water and steel fractions must be minimized because of the marginal tritium breeding performance of ternary oxide breeders with a lead multiplier. Water can slow high energy neutrons below the lead n,2n reaction threshold, and steel (iron) competes with Li⁶ for thermal neutron capture. High pressure (~ 2200 psi) water must be contained by vessels in hoop stress to minimize the structural fraction. Containing pressure with vessels in hoop stress implies tube or pod configurations. (A water coolant panel concept that does not rely on hoop stress is discussed in Section IX.1 for use with beryllium multiplier and lithium oxide breeder.) Since the water coolant, lithium aluminate breeder, and lead multiplier must be separated from one another three possible combinations exist which contain the coolant in tubes or pods:

1. coolant in tubes, multiplier out of tubes, and breeder in tubes or plates,
2. coolant out of tubes (in pods), and multiplier and breeder in tubes or plates,

3. and coolant in tubes, multiplier in tubes or plates, and breeder out of tubes.

Case 3, with the solid breeder surrounding the water coolant tubes and lead multiplier tubes or plates, is not feasible because the high temperature drop through the breeder and poor thermal contact between the breeder and tubes would cause the lead to operate at high temperatures, aggravating corrosion.

Case 2, water-in-pods, introduces pressure differential problems because of the high pressure water surrounding the solid breeder and lead multiplier tubes or plates, and may require higher water fraction ($\sim 15\%$) than case 1. The 300°C lower temperature limit for tritium release from lithium aluminate is uncomfortably close to the water operating temperature (which can range from $\sim 280^{\circ}\text{C}$ to 350°C), making it undesirable to have breeder tubes directly cooled with water. Case 2 also makes it difficult to provide a second barrier for pressure containment if a pod fails. However, a water-in-pods design will require approximately 2 orders of magnitude fewer pressure vessels than the water-in-tubes design.

Case 1, with the coolant and breeder contained in tubes surrounded by the liquid lead multiplier, was chosen over Case 2 because it has excellent thermal control, it minimizes water and structural fractions, and it may have safety advantages. Further study may show that Case 2 is equally feasible, but Case 1 has been pursued here since the configuration is adequate to address the major issues.

The basic configuration of the ternary oxide blanket shown in Fig. IX.5-1 and IX.5-2 consists of steel pods filled with liquid lead, cooling tubes, and tubes containing the lithium aluminate tritium breeder. The front of the pods is screened from the plasma by a row of cooling tubes which are brazed together comprising the first wall. The first wall and the half sector configuration shown schematically in Fig. IX.5-2 applies to the tokamak geometry. Upper and lower half sectors as in Fig. IX.5-2 make up each 36° sector. In a tandem mirror, the first wall tubes can be eliminated because of the lower surface heat fluxes, and the sectors are much smaller and cylindrical in shape.

Figure IX.5-1. Blanket Configuration Schematic

Figure IX.5-2. Poloidally Oriented Pods. Lower Half of Blanket with First Wall Tubes Removed

The purpose of the pods is to contain the pressure in the event of a coolant tube break or leak. A structure designed to contain only the static pressure of the lead (~ 1 MPa) would fail if a tube (~ 15 MPa) leaked (see Ref. 1), damaging adjacent structures and possibly ejecting missiles which would damage other parts of the structure. A single point failure that could potentially cause failures in other sectors would be economically disastrous. Since high pressure tube failures are expected to occur fairly frequently (as discussed below), catastrophic consequences must be eliminated.

The pods are similar to those conceived for the helium cooled blankets and discussed in Section IX.3 and by R. Creedon in Ref. 2. They are constructed of 3 mm thick PCA and are 10 cm wide and 70 cm deep. The length varies around the sector from about 1.6 m on the inboard side to 3.5 m on the outboard side. The rounded pod ends form the sector boundaries as indicated in Fig. IX.5-3. The front portion of the pods is corrugated to relieve bending stresses caused by thermal and swelling gradients. Because of these corrugations, extra reinforcement as shown in Fig. IX.5-4 is required at the ends of the pods to contain the pressure and prevent an accordion-like expansion.

The U-shaped cooling tubes are 1 cm in diameter, 0.5 mm thick, and vary in length from about 3 m to 8 m depending on their location. The breeder tubes are 1.5 cm in diameter. One cm breeder tubes may be required for the row nearest the first wall. An integral, water-cooled steel reflector provides structural support at the back of the blanket.

IX.5.3 Neutronics

One and two dimensional neutronics analyses have been performed to establish the tritium breeding ratio (TBR) of this blanket. M. Abdou, in Ref. 3, had previously analyzed a water cooled, ternary oxide lead multiplier blanket with separate multiplier and breeder zones. Abdou found the TBR to be marginal (maximum full coverage TBR of about 1.19) in the separate multiplier design, and established its sensitivity to the thickness of the multiplier zone at the front of the blanket. The design described here intersperses the lead with the breeder tubes and allows a varying fraction of breeder with depth in the blanket, which appears to result in a higher maximum TBR. Neutronics analyses were performed iteratively with thermal analyses and

Figure IX.5-3. Pod Ends Serve as Sector Boundaries

Figure IX.5-4. Pod End Support Concept

mechanical design to converge toward a feasible blanket. Our results, TBR greater than 1.22 with full coverage, confirms that lead is a viable multiplier and that a water cooled, ternary oxide lead blanket can breed adequately.

One dimensional neutronics analyses were performed using ONEDANT⁽⁴⁾, and two dimensional heterogeneous calculations used the TARTNP⁽⁵⁾ Monte Carlo code. Further optimization of the spatial distribution of the breeder tubes to maximize TBR is possible, but has not been pursued because adequate values were obtained. One dimensional homogeneous parameterizations show that the TBR is sensitive to water fraction, less sensitive to structural fraction, and that most of the tritium breeding neutrons are at epithermal or higher energies. (See Table IX.5-2) A two-dimensional calculation was performed using TARTNP, and compared with a one dimensional homogeneous calculation with the same code to determine the importance of heterogeneous effects. Heterogeneity reduced the TBR by less than 4.3% and changed the spatial distribution of reactions slightly by self-shielding effects.

TABLE IX.5-2 TRITIUM BREEDING ENERGY AND SPATIAL DEPENDENCE,
FRACTION OF T BRED IN ENERGY BANDS

ENERGY (MeV)		ZONE NUMBER					TOTAL
		7	8	9	10	11	
10.0	- 15.0	.00037	.0012	.0017	.0011	.0004	.00102
2.865	- 10.0	.00061	.0022	.0035	.0032	.0016	.00246
0.823	- 2.865	.00645	.0237	.0371	.0391	.0253	.0286
0.184	- 0.823	.0306	.116	.190	.233	.194	.162
0.0091	- 0.184	.0250	.100	.181	.262	.260	.173
4.54E-4	- .0091	.0512	.180	.265	.299	.337	.225
2.26E-5	- 4.54E-4	.124	.310	.258	.144	.162	.183
4.14E-7	- 2.26E-5	.308	.262	.0619	.0177	.0196	.120
Thermal	.453	.0054	.0013	.0004	.0004	.104	

Schematics of the one dimensional neutronics model and the actual blanket geometry are shown in Fig. IX.5-5. Volume fractions are given in Table IX.5-3. Fifty intervals were taken in the 9 zones. A benchmark calculation

TABLE IX.5-3. MATERIALS VOLUME FRACTIONS BY ZONE

ZONE #	ZONE THICKNESS (cm)	DESCRIPTION	H ₂ O	STEEL	VOID	LEAD	BREEDER
3	1.0	First wall	.65	.15	.20		
4	1.4	Pod wall	.32	.36		.32	
5	2.0	Multiplier	.11	.087		.803	
6	1.6	Multiplier	.09	.081		.829	
7	2.0	Breeder	.14	.092		.568	.20
8	2.0	Breeder	.09	.081		.529	.30
9	5.0	Breeder	.08	.079		.4205	.4205
10	25.0	Breeder	.05	.07		.44	.44
11	30.0	Breeder	.05	.07		.44	.44

with added intervals in the breeder regions showed good agreement (TBR within 0.03%). Coolant tubes spacings have been determined from a thermal analysis discussed below, based on heat generation rates from previous iterations of the neutronics and thermal analyses. As will be shown, the iterative thermal-neutronic analysis has essentially converged. The 30 neutron groups and 12 gamma groups shown in Table IX.5-4 were used for the calculations. Global TBR and n_{2n} results for lithium aluminate and lithium orthosilicate breeders with the tube bank first wall, and lithium aluminate breeder without the first wall are given in Table IX.5-5. As mentioned earlier, the tandem mirror reactor has much lower first wall heat fluxes and erosion rates than the tokamak and will not require the separate first wall. With the first wall tube bank

Figure IX.5-5. Ternary Oxide Blanket One-Dimensional Neutronics Model Schematic

TABLE IX.5-4. NEUTRON AND GAMMA ENERGY GROUP STRUCTURE

GROUP NUMBER	UPPER BOUND (MeV)	
	Neutron	Gamma
1	17.0	10.0
2	15.0	9.0
3	13.5	8.0
4	12.0	7.0
5	10.0	6.0
6	7.79	5.0
7	6.07	4.0
8	3.68	3.0
9	2.865	2.0
10	2.232	1.0
11	1.738	0.5
12	1.353	0.1
13	0.823	
14	0.5	
15	0.303	
16	0.184	
17	6.76E-2	
18	2.48E-2	
19	9.12E-3	
20	3.35E-3	
21	1.235E-3	
22	4.54E-4	
23	1.67E-4	
24	6.14E-5	
25	2.26E-5	
26	8.32E-6	
27	3.06E-6	
28	1.13E-6	
29	4.14E-7	
30	1.52E-8	

XI.5-5. GLOBAL NEUTRONICS RESULTS

CONFIGURATION	TBR	ENERGY MULTIPLICATION	LEAD (n,2n)
Baseline	1.28	1.25	.456
Li_4SiO_4	1.33	1.23	.454
No separate first wall (TMR)	1.39	1.23	.459

removed the TBR increased from 1.28 to 1.39. This indicates the sensitivity of the design to the first wall layers. The effect on TBR of adding a sacrificial steel layer of various thicknesses is shown in Fig. IX.5-6. Approximately 8 mm of sacrificial material can be added while still maintaining an adequate TBR of 1.2 at beginning of life.

A schematic of the homogeneous and heterogeneous models used in the TARTNP Monte Carlo calculations is shown in Fig. IX.5-7. Specular reflecting boundaries form the sides of the wedge to minimize computer time. The plasma is modelled as a cylindrical homogeneous source with a 300 cm outer radius. The first three centimeters of blanket including the first wall, the corrugated pod wall, and the first centimeter of the water cooled lead zone were homogenized in both cases. The next 17 centimeters, in which over half the tritium breeding takes place, was modelled both heterogeneously and homogeneously, and the remaining 60 centimeters of blanket was homogenized. Minimizing the number of tubes (and therefore zones) in the heterogeneous model has led to small differences in configuration; Table IX.5-6 compares the material fractions by zones for the TARTNP and ONEDANT models. These differences, and more importantly, differences in cross-section libraries (TARTNP uses ENDL and ONEDANT uses ENDF V), account for the increase in TBR from 1.28 in the ONEDANT calculation to 1.39 in the homogenized TARTNP calculation. The capture cross-section for lead in ENDL is known to be low. The ONEDANT results are believed to be the better estimate. The heterogeneous TARTNP calculation yields a TBR of 1.33, 4.3% lower than the homogeneous case. Assuming linearity, this 4.3% reduction corresponds to a minimum full

Figure IX.5-6. Effect of Sacrificial First Wall Erosion Layer on TBR

MATERIALS MIX, BY VOLUME

**TABLE IX.5-6. MATERIALS FRACTIONS COMPARISON BY ZONE FOR HOMOGENEOUS (ONEDANT)
AND HETEROGENEOUS (TARTNP) NEUTRONICS MODELS**

ONEDANT Zone	TARTNP Zone	ONEDANT %Fe	TARTNP %Fe	ONEDANT %H ₂ O	TARTNP %H ₂ O	ONEDANT %Pb	TARTNP %Pb	ONEDANT %LiAlO ₂ **	TARTNP %LiAlO ₂
3	1	15	15	65	65	0.0	20	0.0	0.0
4	2*	36	50	32	0.0	32	30	0.0	0.0
5	3*	8.7	10	11	40	80	50	0.0	0.0
6	4*	8.1	1.0	9.0	11	83	88	0.0	0.0
7	5	9.2	3.0	14	17	57	61	20	19
8	6	8.1	2.0	9.0	8.0	53	71	30	19
9	7	7.9	3.0	8.0	0.0	42	55	42	42
9	8	7.9	3.0	8.0	23	42	74	42	0.0
9	9	7.9	3.0	8.0	0.0	42	55	42	42
10	10	7.0	4.0	5.0	10	44	42	44	44
10	11	7.0	3.0	5.0	0.0	44	50	44	47
10	12	7.0	4.0	5.0	5.0	44	47	44	44
11	13*	7.0	7.0	5.0	5.0	44	44	44	44

*Zone boundaries are not identical

**80% dense

coverage TBR of 1.22 when applied to the ONEDANT results. The 2 mm sacrificial first wall layer specified in the design guidelines will reduce the TBR to about 1.21.

IX.5.4 Heat Transfer and Thermal Hydraulics

Heat transfer and thermal hydraulics analysis have been performed to determine tube spacing, film drop temperatures, and maximum and minimum temperatures in the lead, lithium aluminate, and structure. Heat generation rates averaged over zones are given in Table IX.5-7. Local heat generation rates in the lithium aluminate are considerably higher than the zone averages that are shown; however, the lithium aluminate can operate at much higher temperatures than the lead and structure and the heat flux at the cooling tube wall should be fairly close to the average value. The maximum heat generation rate in the lithium aluminate is 170 W/cm^3 , and the resulting maximum centerline temperature is 755°C .

Coolant tube spacing is determined by finding the radius (R_0 from Fig. IX.5-8) corresponding to a 100°C temperature rise from the tube wall in liquid lead at a constant heat generation rate. The 100°C temperature rise was chosen for the calculations as an approximate maximum figure. A higher temperature rise would allow larger tube spacing, and therefore lower steel and water fractions. The limiting factor is the maximum PCA/lead interface temperature which is approximately 450°C with the 100°C rise. The 1-D cylindrical conduction equation was solved for constant properties. The distance between tubes is found by assuming that the R_0 , or $+100^\circ\text{C}$ boundaries from adjacent tubes would overlap enough so that the total area cooled by each tube equals the area of the circle formed by R_0 . This results in small areas that are overcooled where the circles overlap and small areas that are undercooled between the circles. The undercooled areas will be within 2°C of the intended value due to this approximation.

Table IX.5-7 gives the tube spacings and resulting fractions of steel and water in the blanket. These are compared with the values used in the neutronics model to find the heat generation rates, and show that the process has converged fairly well. For example, Zone 5 (see Fig. IX.5-5) has 0.8% more

TABLE IX.5-7. TERNARY OXIDE BLANKET TUBE SPACING AND HEAT FLUXES BY ZONE

ZONE	HEAT DEPOSITION (W/cm ³)	R _o ^(a) (cm)	TUBE SPACING ^(b) (cm)	HEAT FLUX AT TUBE WALL (W/cm ³)	%H ₂ O REQUIRED	%H ₂ O IN NEUTRONICS MODEL	%Fe ^(e) REQUIRED	%Fe ^(e) IN MODEL
3(first wall)	29.4	—	—	50 – 100	65	65	15	15
4	44.7	0.796	0.52	28.3	32 ^(c)	32	36 ^(c)	36 ^(c)
5	34.3	1.31	1.50	58.9	11.8	11.0	2.7	2.7
6	23.3	1.51	1.89	53.1	8.9	9.0	2.1	2.1
7	43.2	1.25	1.38	67.5	13.0	14.0	3.0	3.2
8	26.7	1.44	1.74	55.4	9.7	9.0	2.3	2.1
9	20.0	1.51	1.88	45.6	8.9	8.0	2.1	1.9
10	6.57	2.01	2.83	26.5	5.0 ^(d)	5.0	1.2	1.0
11	0.558	2.01	2.83	2.25	5.0 ^(d)	5.0	1.2	1.0

a) Outer radius of zone cooled by tube

b) Wall to wall, assuming equal area cooled as 1-D cylindrical model

c) Increased number of tubes to cool corrugations

d) Increased for temperature control of breeder tubes

e) Considering cooling tubes only (podwalls are included in neutronics model)

Figure IX.5-8. Coolant Tube Spacing Model

water required than used in the neutronics model, and Zone 7 has 1% less water required than used. The differences will tend to cancel and are expected to be small compared to errors from other sources, such as the homogeneous model, cross-section data errors, and constant thermal properties. All the heat deposited in an area cooled by a tube is assumed to be deposited outside the tube in determining the heat fluxes at the tube walls (also given in Table IX.5-7), resulting in conservative values. A temperature summary is given in Table IX.5-8.

TABLE IX.5-8. BLANKET OPERATING TEMPERATURES

	°C
Coolant Inlet/Outlet	300/320
Structure Min/Max	300/450
Breeder Min/Max	320/755
Multiplier Min/Max	300/450

The cooling tubes are routed in toward the first wall from the back of the blanket, along the forward portion of the blanket for a distance of 1 to 2 m and back out of the blanket, or along a back portion of the blanket and then out, depending on the heat deposition near the particular tube. Assuming an average coolant temperature rise of 20°C, the maximum water velocity is 8.4 m/s. The maximum film drop temperature is 14°C. The pumping power at the 8.4 m/s operating velocity is approximately 0.5% of the blanket energy. A higher fluid ΔT (currently 20°C) would allow proportionally longer tubes or slower velocity and lower pumping power, but difficulties with freezing lead would become more severe and no great benefits are apparent. Lead freeze-up must be prevented by the operator during shutdowns to avoid damage to the blanket.

Possible water coolant inlet and outlet temperatures are restricted by thermal to electric conversion efficiency, the lead melting point (327°C) and the critical point of water (374°C). Two possible inlet/outlet temperature

combinations will be discussed to illustrate the problems: 330-350°C and 300-320°C. The higher temperature (330-350°C) operating point has the advantage of keeping all the lead in the blanket molten during operation since the 330°C inlet temperature is above the 327°C melting point of lead. The higher temperature could be used for steam superheating to boost the thermal to electric conversion efficiency. The major disadvantages are higher temperature (480°C steel/lead interface), and much higher pressure (21.3 MPa vs. 14.4 MPa for the 320°C outlet temperature). The 0.5 mm thick tubes will carry the higher pressure at an acceptable 31% of yield strength, but the higher pressure could affect tube failure rates. Thicker pods are required to contain the higher pressure in the event of a tube failure. The 21.3 MPa (3100 psi) operating pressure is high compared to current LWR operating pressures. For example, Oconee Unit 1⁽⁶⁾ operates at 15.1 MPa (2200 psi). However, the design pressure for this currently operating plant is 17 MPa (2500 psi), and extrapolating to 21.3 MPa (3100 psi) in the next century seems credible. A 1200 MWe LWR proposed for construction in Ref. 6 has a design coolant outlet temperature of 354°C.

The lower temperature operating point (300-320°C) is very similar to current LWR temperatures (Oconee Unit 1 is 290-318°C), and can use the same power cycle. The major advantages of the lower pressure are thinner pods and lower stresses in the cooling tubes. (Thinning the cooling tubes below the current 0.5 mm does not appear feasible because of manufacturing difficulties). During normal, full power operation lead freeze up is prevented by the heat deposited in the blanket. A 1 cm layer of solid lead will surround the inlet coolant tubes at the back of the blanket if they are spaced approximately 3 cm apart. Some portion of the tubes will be in contact with molten lead in the front portion of the blanket because of the higher heat fluxes and film drop temperatures. Partial power operation will require that the coolant inlet temperature be raised to prevent the lead from solidifying at the back of the blanket and damaging tubes. During shutdown, the water temperature will have to be raised to prevent freeze-up. The operating pressure is high enough to prevent boiling up to 340°C to allow for hot spots during operation. Hot spots will be much less severe (or non-existent) at shutdown, allowing the coolant temperature to be raised to ~ 330°C to keep the lead molten.

Operational difficulties, caused by the necessity of preventing solidification of the lead, are generic to the combination of lead and water. The higher temperature operating point (330-350°C) avoids the necessity of raising the coolant temperature at shutdown, but operator action is required to make sure that flow rates are reduced at the steam generators or the coolant temperature will drop rapidly. We have chosen the lower temperature operating point as the baseline because of the advantages of lower pressure. Changing to the higher temperature would not require a major perturbation of the design.

Major issues (liquid metal embrittlement, corrosion of the structure by the molten lead, and constraints on the choice of structural material) arise from the operating temperatures possible with liquid water coolant. Water outlet temperatures below 300°C result in poor thermal to electric conversion efficiency, and temperatures above 350°C require very high pressures and are too close to the 374°C critical point of water. These problems are discussed in the issues section below.

IX.5.5 Mechanical Design

Mechanical design of the pods is similar to the gas cooled blanket design described in Section IX.5.3. The water cooled pods have a smaller radius because the water coolant pressure (to be contained in an accident) is higher than for helium. The smaller water cooled pods have the option of containing the pressure at the pod ends mainly in membrane stresses in cylindrical end caps (see Figs. IX.5.3 and IX.5.4). This is impractical with helium because of voids between the pod ends. In the water cooled blanket the voids are smaller, and could be partially filled with the inlet and outlet portion of the first wall tubes. The outer end of the pods requires extra support (see Fig. IX.5.4) because the corrugations provide none against an accordian-like expansion, but the pod side walls are adequate to support the ends out to the corrugated section. Hoop and thermal stresses in the pods (the maximum during a tube break) and tubes are given in Table IX.5-9. The static lead pressure is appreciable, 1 atm per meter of head (or < 1 MPa total), but is very low compared to the design pressure (14.4 MPa). The pods see only the static lead pressure unless a tube failure occurs, and are not intended for reuse after a tube failure. It may be feasible to design thinner pods that will yield and/

TABLE IX.5-9. MAXIMUM STRUCTURAL HOOP AND THERMAL STRESSES

H ₂ O OUTLET TEMPERATURE (°C)	OPERATING ^(a) PRESSURE (MPa)	TUBE ^(b) HOOP STRESS (MPa) 155 MPa MAX	POD ^(c) HOOP STRESS (MPa)	TUBE ^(d) THERMAL STRESS (MPa)	FIRST WALL ^(d) THERMAL STRESS (MPa) (no erosion layer)
350	21.3	192	372	36	51
320	14.4	130	240	36	51
300	11.0	99	183	36	51
280	8.7	78	145	36	51

a) 20°C boiling point margin

b) 1 cm O.D., 0.5 mm wall

c) Maximum in failure condition, including 1 MPa static lead head

d) $b = E\alpha \Delta T / 2 (1-\nu)$

or go to 80 to 90% of ultimate strength under these conditions, thus precluding a catastrophic result of a tube failure with the minimum structure. At the 14.4 MPa water operating pressure corresponding to the 320°C outlet temperature the maximum primary pod stress during a tube failure could reach 240 MPa, which appears to be below the yield strength of the irradiated material (see Section IV.1.3). P. Roth and J. Herring's analysis⁽¹⁾ of coolant tube failures in the STARFIRE lithium aluminate filled blanket module shows that the first wall pressure never reaches the water operating pressure during a tube failure due to venting through the packed bed. Liquid lead will behave quite differently from the packed bed analyzed by Roth and Herring, but a pressure spike higher than the water operating pressure is unlikely and venting may provide some relief as it does for the packed bed. The pods are connected by lead manifolds so that the forces due to the static lead pressure are balanced across adjacent pod walls. The blanket contains approximately 60,000 separate high pressure tubes.

The first wall design for this blanket has not been emphasized. A water cooled first wall is a difficult design effort in itself, and the blanket described here could be easily adapted to accept various types of water cooled first walls. The major problem with the tube bank concept presented here is that separate tubes may tend to chafe against one another due to vibrations induced by the high velocity (~ 8 m/s) water in them. Brazing the tubes to one another may eliminate this problem, but has not been studied in detail. If a sacrificial first wall layer is required in tokamaks, it can be provided by drawing the first wall tubes with one thickened side.

IX.5.6 Issues

IX.5.6.1 Tube Failure

Extensive experience in the nuclear industry with water cooling indicates that tube failure rates may be the biggest problem confronting water cooled fusion blankets. The tubes-in-pod design presented here is an attempt to show that catastrophic results due to failure of a single tube can be prevented, but each failure will still require changing out a blanket sector. Estimates of blanket change out time are speculative because of the level of detail of

conceptual reactor designs. STARFIRE⁽⁷⁾ estimates 11 days, DEMO⁽⁸⁾ estimates 11 days, and MARS^(9,10) estimates 37 days (for the high temperature blanket) of total downtime for changing out a blanket sector. The DEMO reactor is the closest in maintenance concept to the reactor envisioned here. A fusion reactor containing 60,000 high pressure water tubes will require lower tube failure rates than those currently achieved in nuclear power reactors. LWR steam generators and sodium/water experience for the LMFBR indicate failure rates corresponding to 1 - 2.7 failures per year in the ternary-lead blanket discussed here (see Table IX.5-10). The LWR industry lives with high failure rates in steam generators by tolerating some leakage, by frequent inspection

TABLE IX.5-10. TUBE FAILURE DATA APPLIED TO BLANKET DESIGN
(Based on 60,000 Tubes, 80% Availability)

	RAW DATA	PER 60,000 TUBES PER CALENDAR YEAR
Westinghouse (Na/H ₂ O) ^a	5 x 10 ⁻⁹ /tube/hr + 1 x 10 ⁻⁹ /ft ² hr	2.1 + .6
NREP (LWR) ^b	2.3 x 10 ⁻⁹ /12 ft/hr	1.0
LWR steam tubes ^c	2.9 x 10 ⁻⁵ /tube/FPY	2.2 "events per year", for average of 90 failures per event (1.5 up to 1000 FPD's, 2.8 thereafter).

- a) Westinghouse, "Steam Generator Concept Selection Study", WARD-4210T2, 1971.
- b) Oswald, et.al., "Generic Data Base for Data and Models Chapter of the (NREP) Guide", EGG-EA-5887, June 1982.
- c) Tatone and Pathania, "Steam Generator Tube Performance: World Experience with Water-Cooled Nuclear Power Reactors during 1979", Nuclear Safety, September-October 1981.

and preventive maintenance (mainly tube plugging) during scheduled downtime, and by shutting down and plugging tubes that leak. Tube plugging is a very difficult maintenance option for fusion blankets; a thermal margin must be provided by adding extra tubes in the reactor, thereby increasing steel and water fractions, and all tubes must terminate at tube sheets located near the outside of the reactor for access, necessitating long tubes and requiring extra space. Allowing some leakage during operation also presents difficult design problems. Breeder materials tend to react chemically with water, and pressure containment can be a major problem.

If fusion reactors that tolerate leaks and/or allow tube plugging cannot be designed, current tube failure rates must be reduced by roughly an order of magnitude (to approximately one failure every four to ten years) for reliable power plant operation. Such a reduction in tube failure rates is being sought after today (so far unsuccessfully) by nuclear and other power producing industries. It is possible that fusion blankets can employ design solutions that are too expensive for steam generators, such as special coatings or thickened tube ends to reduce failures at welds. Double walled tubes are currently under investigation and may prove feasible if common cause failures are eliminated. Tube failure rate is the major outstanding issue for water tube cooled fusion blankets and may require a considerable development effort.

IX.5.6.2 Liquid Metal Embrittlement

Liquid metal embrittlement of the PCA structure by lead is a major issue in this design. Water temperatures must be in the vicinity of the 327°C melting point of lead. The 374°C critical point of water limits the upper temperature to ~ 350°C to maintain good heat transfer properties in the boundary layer, and thermal to electrical conversion efficiencies limit the outlet temperature to above 300°C. The point of minimum ductility in steels subject to LME occurs near the melting point of the liquid metal (see Fig. IX.5.9). The uniform elongation of 25% cold worked PCA not exposed to lead is only about 1.2% at the melting point of lead (see Fig. IV.9). Experiments are required to determine whether PCA is significantly embrittled by lead, and if so at what temperatures. If LME is a problem with PCA, several possible remedies exist. Recent work at the Colorado School of Mines has shown that lead embrittles 2-1/4 Cr-1Mo, and that embrittlement of this

Figure IX.5-9. Effect of Heat Treatment and Cold Work on Fracture Strength of 4145 Steel Wetted with Lead

material can be prevented by heat treatment. Heat treatment and cold work have also been shown to significantly affect LME in 4145 steel. (See Fig. IX.5-9 and Ref. 10 and 11). However, irradiation effects on LME are not well known and may dominate effects of cold work and heat treatment. An oxide coating on the PCA would prevent wetting and thus eliminate LME because lead could not diffuse to crack tips. If PCA is shown to embrittle in lead, oxide or other coatings may be an effective way of eliminating embrittlement.

IX.5.6.3 Corrosion

Corrosion of PCA by liquid lead at the 450°C maximum interface temperature may require corrosion inhibitors in the lead or coatings on the steel. Ferritics such as HT-9 are more corrosion resistant, but the ductile to brittle transition temperature of irradiated ferritic steels may exceed the operating temperature at the 300°C water inlet temperature. Vanadium alloys should be evaluated as structural materials for this concept.

IX.5.7 Conclusions

The ternary oxide-lead water cooled blanket is neutronicallly, thermally and mechanically viable. Our calculations show that lead is an adequate multiplier and that temperatures can be adequately controlled with a molten or partially molten lead multiplier, breeder-in-tube configuration. We believe that each issue can be solved. However, more issues exist with this concept than with other blanket concepts, most of which arise from the lead-water combination. The 374°C critical point of water is uncomfortably close to the 327°C lead melting point, making difficulties with LME and lead freeze up (which can damage tubes) inevitable. Water also limits the usefulness of lead as a neutron multiplier because it is a strong neutron energy moderator. Using another coolant, such as HTS or helium would eliminate problems with LME, freezing, and high pressures, and would result in a higher TBR. Corrosion of the structure by liquid lead may limit the upper temperatures regardless of the coolant, but we believe that corrosion is controllable in this static system.

Water cooled fusion blankets face two generic feasibility issues. These are the consequences and the rate of occurrence of high pressure water leaks in the blanket. A good water cooled blanket concept addresses the first, but

leaves the second open because of the large number of tubes. (We have not seen a concept that resolves both.) Shutdowns due to tube failures and tritium removal from the water due to permeation through the first wall will tend to drive up costs compared to blankets using other coolants. The possibility of lead freezing creates operational difficulties. For these reasons, we recommend that this design be pursued further only if no other water cooled solid breeder design looks better and the study chooses to retain a water cooled option.

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X. ALTERNATE BLANKET CONCEPT SCREENING

X.1 The Concept Screening Approach

Although the Blanket Comparison and Selection Study (BCSS) is already studying diverse combinations of breeders, coolants, and multipliers, some effort must be expended to ensure other potentially attractive concepts are recognized. This chapter summarizes an effort to review the literature, identify concepts which are not in the mainline study, and evaluate them against screening criteria. Those concepts which pass the screening criteria are studied further, and recommendations concerning their status in the study are presented.

For the first step of the alternate concept screening task a bibliography of existing fusion electric blankets designed for, or adaptable to, tokamak and mirror fusion machines was compiled. This bibliography is presented in Section X.2. The coolant, breeder, structure, and multiplier are listed for each design. Theta pinch and liquid metal protected inertial confinement fusion blankets were intentionally excluded because they are not adaptable to the machines of interest, whereas the SOLASE design was included because only minor changes would be required. Studies which did not include enough detail to be adequately evaluated were also excluded. Some recent aqueous slurry concepts fall into this latter category.

The second step was to determine which blankets should be classified as alternate concepts. The blankets designated as alternate concepts are identified by the breeder or coolant used: those using Li_7Pb_2 , $62\text{Li}-38\text{Pb}$, or FLIBE ($\text{LiF}-\text{BeF}_2$ salts) as the breeder, and those using a molten salt or flowing solid pebble coolant. Where possible, the design parameters quoted in the blanket studies are compared with the screening criteria established for this study. These comparisons are also presented in Section X.2.

Only two of twelve alternate concepts were carried beyond the screening criteria comparison step. The next step was to modify the remaining concepts to improve their engineering feasibility, mechanical design, and breeding performance. In addition, a variety of specific technical issues were explored to determine the development needed to further advance the designs.

These issues are discussed in Section X.3 and X.4. Suggestions for further study and recommendations as to the concept status of the modified designs are given in Section X.5.

X.2 A Fusion Blanket Concept Review

Forty-nine suitable fusion blanket designs from eighteen different organizations were identified for the blanket bibliography. Of these, thirty-seven are classified as mainline concepts (i.e., already covered in Chapters VII and IX) and twelve as alternate concepts. Table X.2-1 lists the mainline concepts by coolant and breeder along with the reference number, year of publication, organization, structural material used, and neutron multiplier. Table X.2-2 lists the twelve alternate concepts. Some technical features of each alternate concept are discussed below and, where possible, each concept is compared to the screening criteria. A summary of the screening process is given in Table X.2-4.

X.2.1 Blankets with a FLIBE Breeder and Gas Coolant

Only one alternate concept using FLIBE as the primary breeding material has been identified. The blanket was designed by Princeton Plasma Physics Laboratory and is shown in Fig. X.2-1. Molten FLIBE (46.9 mole % LiF, 53.1 mole % BeF₂) contained within the structural troughs absorbs neutron energy and generates tritium. Helium is used to cool the FLIBE and structure. Tritium is extracted from the slowly circulating FLIBE by cold traps. Because of copious tritium permeation into the helium, it is also extracted from the coolant as T₂O.

Because the Princeton effort was a fairly complete study, the blanket parameters could be compared with the screening criteria. This comparison, shown in Table X.2-3, indicates the design is deficient in tritium breeding as well as in neutron wall loading. The latter deficiency should not be taken too seriously because the design could easily be modified to accommodate a higher wall loading. However, the former deficiency is catastrophic to this design because when three dimensional effects are included the breeding ratio will be less than unity. This shortcoming can be alleviated by the addition of a neutron multiplier. A modified version of this concept employing a multiplier is described in Section X.4.

TABLE X.2-1 BIBLIOGRAPHY OF EXISTING BLANKET DESIGN CONCEPTS

<u>BREEDER/COOLANT AND REFERENCE NUMBER</u>	<u>YEAR</u>	<u>ORGANIZATION</u>	<u>STRUCTURE</u>	<u>SEPARATE MULTIPLIER</u>
<u>Li/Li Blankets</u>				
1	72	ORNL	Nb-1%Zr	--
2	72	CULHAM	Unspecified	--
3	72	LLNL	Nb	--
4	74	UW (UWMAK-I)	316 SS	--
5	76	UW (UWMAK-III)	TZM	--
6	77	ANL	316 SS	--
7	78	ORNL	316 SS	--
8	79	ANL	Vanadium	--
<u>^{17}Li-^{83}Pb/^{17}Li-^{83}Pb Blankets</u>				
9	80	UW (WITAMIR)	HT-9	--
10	82	UW (MARS)	HT-9	--
<u>^{17}Li-^{83}Pb/H_2O Blankets</u>				
11	82	UW (UWTOR-M)	HT-9	--
<u>Li/He Blankets</u>				
12	72	CULHAM	Nb-5Zr	--
13	76	ORNL (EPR)	316 SS	--
14	77	LLNL	Inconel or SS	--
15	77	ORNL	316 SS	--
16	78	ORNL (EBT)	316 SS	--
17	78	ISPRA (FINTOR-D)	Austenitic SS	--
18	79	WESTINGHOUSE	316 SS	--

TABLE X.2-1 BIBLIOGRAPHY OF EXISTING BLANKET DESIGN CONCEPTS (CONTINUED)

<u>BREEDER/COOLANT AND REFERENCE NUMBER</u>	<u>YEAR</u>	<u>ORGANIZATION</u>	<u>STRUCTURE</u>	<u>SEPARATE MULTIPLIER</u>
<u>Li₂O/He Blankets</u>				
19	76	MIT (BRSR)	Nb-1%Zr	--
20	77	ORNL (DEMO)	SS	--
21	79	LLNL	Inconel 718	--
22	80	GA	Al	--
23	80	GA	Inconel 718	--
24	81	GA	Inconel 718	--
25	81	GA	SiC	--
26	81	GA	SiC	--
27	82	ANL	PCA	--
10	83	GAC	Vanadium	--
<u>Li₂O/H₂O Blankets</u>				
28	79	LANL	SS and Cu	--
29	?	JAERI	Mo	--
30	82	FED/INTOR	HT-9	Pb
<u>Ternary Oxide/He Blankets</u>				
31	74	BNL	Al	Be or Li ₇ Pb ₂
32	75	UW (UWMAK-II)	316 SS	Be
33	76	BNL	Al	Be or Li ₇ Pb ₂
34	78	LLNL (Standard Mirror)	TZM	--
<u>Ternary Oxide/H₂O Blankets</u>				
35	80	ANL (STARFIRE)	Austenitic SS	Be or Zr ₅ Pb ₃
36	81	LANL	Austenitic SS	Be or Zr ₅ Pb ₃

TABLE X.2-2 BIBLIOGRAPHY OF ALTERNATE CONCEPTS

<u>REFERENCE</u>	<u>YEAR</u>	<u>ORGANIZATION</u>	<u>STRUCTURE</u>	<u>BREEDER</u>	<u>COOLANT</u>	<u>SEPARATE MULTIPLIER</u>
37	74	PPPL	SS	FLIBE	He	--
38	77	UW (SOLASE)	Graphite	Li ₂ O/Ar	Li ₂ O/Ar	--
39	77	ORNL	316 SS	Li	HTS	--
40	77	GA (DPR-1)	Inconel 718	Li ₇ Pb ₂	He	--
41	78	GA (DPR-2)	Inconel 718	Li ₇ Pb ₂	He	--
42	78	MIT (HFCTR)	TZM	Li	FLIBE	--
43	79	UW (NUWMAK)	Ti-Al Alloys	62Li-38 ^{ru}	H ₂ O	--
8	79	ANL	SS	Li ₇ Pb ₂	He	--
44	79	MSNW	SS	Li ₇ Pb ₂	H ₂ O	--
8	79	ANL	SS	Li ₇ Pb ₂	H ₂ O	--
8	79	ANL	SS	Li ₇ Pb ₂	HTS	--
45	82	FRG	Not specified	Li ₇ Pb ₂	He	--

TABLE X.2-3 COMPARISON OF ALTERNATE BLANKET CONCEPTS WITH THE SELECTION CRITERIA

<u>PARAMETER</u>	<u>CRITERION</u>	<u>PPPL</u>	<u>SOLASE</u>	<u>NUWMAK</u>
Breeding ratio 1-D	> 1.2	1.07	1.33	1.54 (2-D)
3-D	> 1.05	N/A	N/A	N/A
Thermal efficiency (%)	> 25	41	42	32
T Blanket inventory, g/MWth	< 10	<10	0.009	1.95
Lifetime, MW-yr/m ²	> 5	8.25 (estimate)	5	10 (estimate)
Tritium loss rate, Ci/d	< 100	2 (estimate)	4-6	10
Material compatibility	yes	yes	yes	no
Total wall loading, MW/m ²	> 3	1.65	5	4.34

Figure X.2-1. The Helium Cooled FLIBE Blanket Designed by Princeton Plasma Physics Laboratory (37)

TABLE X.2.4 SUMMARY OF SCREENING PROCESS

Breeder/Coolant	Applications of Criteria		Suggestions
	Pass	Fail	
FLIBE/He	X (with modification)		Add a Pb multiplier and modify configuration
Li/HTS		X	Fails chemical compatibility criteria
Li ₇ Pb ₂ /HTS		X	Fails chemical compatibility criteria
Li/FLIBE		X	Fails limitations on structure temperature
Li ₂ O/Li ₂ O	X (with modification)		Could be modified with a steel pressure boundary and possibly SiC as an alternative to graphite
62Li-38Pb/H ₂ O		X	Fails chemical compatibility criteria
Li ₇ Pb ₂ /H ₂ O		X	Fails chemical compatibility criteria
Li ₇ Pb ₂ /He		X	Doubtful ability to maintain adequate tritium diffusion rate

X.2.2 Molten Salt Cooled Blankets

Three molten salt cooled blankets were identified in the literature: a heat transfer salt (HTS) cooled lithium breeder design, an HTS cooled Li₇Pb₂ breeder design, and a FLIBE cooled lithium breeder design. The first two designs violate the compatibility criteria because HTS reacts vigorously with lithium and Li₇Pb₂.⁽⁸⁾ However, the HTS coolant itself has some very attractive properties and is better matched to other breeders. Modified HTS cooled designs are presented in Section X.3.

The FLIBE cooled lithium breeder design is shown in Fig. X.2-2. This design satisfies the screening criteria listed in Table X.2-3, but exceeds the temperature limitations for steel structural materials set by this study. The

Figure X.2-2. Section Diagram of the Outboard Blanket and Shield for the MIT FLIBE Cooled Lithium Breeder Design ⁽⁴²⁾

salt used for this design (66 mole percent LiF and 34 mole percent BeF_2) is a eutectic with a melting temperature of 460°C . To assure melting and avoid coolant channel blockage it is desirable to operate the coolant at least 50°C above the melting point. Considering also that the film drop will be large for a coolant with such low conductivity, it is doubtful the temperature can be maintained below the limit set for steel in contact with static lithium. The limit is 550°C for austenitic and ferritic steel. Indeed, this is the very reason TZM was chosen as the structural material.

The other candidate material which is consistent with the temperature window of a FLIBE cooled liquid metal breeder blanket is vanadium. But without a strong oxygen getter in the vicinity of vanadium, such as lithium, oxidation of the structure and pursuant loss of strength may be of concern. It is possible oxygen getters could be implemented in conjunction with vanadium structure, but at this point in time FLIBE cooled blankets are not recommended for future study.

There is another FLIBE eutectic with a composition of 47 mole percent LiF and 53 mole percent BeF_2 which has a melting point of 363°C . This salt could probably be used with structural steel in the presence of static lithium or lead lithium. However, the viscosity of this salt is one to two orders of magnitude higher than that of the eutectic discussed above, resulting in a prohibitive blanket pressure and a high recirculating power fraction. Hence, this alternate eutectic is not recommended as a blanket coolant.

The temperature limitations for steel in contact with solid breeders are less restrictive than those for steel in contact with liquid breeders, and there is no reason in principle why a FLIBE cooled solid breeder blanket with steel structure should not be considered. But because HTS has a lower viscosity and wider temperature window for operation than FLIBE, HTS is the only molten salt coolant recommended for further study. If HTS does not provide to be a feasible coolant, FLIBE cooled solid breeder blankets may be reconsidered.

X.2.3 Solid Coolant Breeders

The only fusion blanket designed specifically for electricity production which uses a pebble bed coolant is the University of Wisconsin's SOLASE concept, shown in Fig. X.2-3. In this design Li_2O particles 0.1 mm in

Figure X.2-3. A Schematic of the University of Wisconsin SOLASE Cavity(38)

diameter move through the blanket and act as both coolant and breeder. Argon gas fills the gaps between particles to improve heat transfer. Tritium removal from the Li_2O is effected by slowing the flow rate of the outer layer of particles enough to heat them to 800°C . Tritium then diffuses out of the particles and into a purge gas. The structure chosen for this design is high density graphite.

One unique advantage which was perceived for the SOLASE blanket was that low induced radioactivity in the graphite structure might allow the possibility of limited-hands on maintenance. This advantage might also be possible had SiC been chosen, and since the SOLASE work was completed data has emerged showing SiC may be at least as good a choice of structural material. The reason graphite was chosen in the study was primarily that more irradiation damage data is available, making a more thorough design analysis possible. If the graphite structure of SOLASE is replaced with one of the candidate metals being considered for this study, the possibility of hands on maintenance no longer exists. In any case, because it will be necessary to add a structural first wall to the SOLASE blanket to adapt it to magnetic confinement machines, the advantage of low induced radioactivity will be difficult to retain.

The blanket parameters for SOLASE are compared to the screening criteria in Table X.2-3. Although the SOLASE design passes the screening criteria, solid coolant breeder reactors are not being recommended for further study. The principal reason is that there is not sufficient industrial experience with solid coolants to show they are erosior resistant or will provide adequate thermal control for this application. Considering the novel effects the radiation environment may have on Li_2O , it is unlikely sufficient experience can be obtained without a long term development program.

X.2.4 Phase Change Blankets

The only blanket utilizing a breeder which changes in phase is the University of Wisconsin's NUWMAK design, shown in Fig. X.2-4. This blanket utilizes ^{62}Li - ^{38}Pb as the breeder and boiling water as the coolant. The breeder is always solid around the coolant tubes and liquid between them. Between fusion power pulses (NUWMAK was a pulsed tokamak) the melt layer shifts in position as latent heat is added or subtracted, thus avoiding

Figure X.2-4. The University of Wisconsin
NUWMAK Blanket Design (43)

troublesome temperature cycles at the coolant tubes. The coolant tubes are multilayered to prevent tritium from reaching the working fluid and passing into the environment. The tubes are primarily made of titanium, with nitride coatings to act as a tritium barrier and zirconium coatings to act as a getter. A lead layer between concentric tubes serves as a heat transfer medium.

The relevant parameters for the NUWMAK design are compared to the screening criteria in Table X.2-3. This design appears to satisfy the criteria, but the lifetime estimate lacks adequate justification, and the compatibility anticipated in the study between water and ^{62}Li - ^{38}Pb was not verified by subsequent experiments.⁽⁴⁶⁾ The use of ^{62}Li - ^{38}Pb as a thermal capacitor provides an innovative method of operating in the narrow temperature window of titanium even with pulsed power machines. But if titanium is not used as the structure, there is no strong motivation for using this eutectic. Because titanium alloys are not a candidate structural material in the Blanket Comparison and Selection Study, the NUWMAK concept cannot be recommended for further study. Another reason for not recommending further study of this design is that there is no industrial experience with nitride coated titanium, and thus, the tritium containment mechanism may prove to be unreliable.

X.2.5 Li_7Pb_2 Breeding Blankets

Of the alternate blanket concepts using Li_7Pb_2 as the breeding material, two are water cooled and five are helium cooled. None of the studies associated with these blankets are complete enough to compare with the screening criteria. The two water cooled blankets are shown in Fig. X.2-5. This combination of breeder and coolant has two attractive features: good heat transfer characteristics, and attractive neutronics. The heat transfer characteristics are good because Li_7Pb_2 has a relatively high thermal conductivity, and water has a relatively high convective heat transfer coefficient. The neutronics are particularly attractive for this breeder and coolant because the hydrogen in water rapidly moderates the neutrons down to energies where leakage becomes small and the ^6Li capture cross section becomes large. Thus, the blanket can be made thinner with water as the coolant.

(a)

(b)

Figure X.2-5. Water Cooled Li_7Pb_2 Breeding Blankets
Designed by (a) Math Sciences Northwest (44),
(b) Argonne National Laboratory (8)

(a) (40)

(b) (41)

Figure X.2-6. Helium Cooled Blanket Designs Using Li_7Pb_2 as the Breeder Material

(c) (8)

(d) (45)

Despite the advantages of Li_7Pb_2 breeder/ H_2O cooled blankets, chemical incompatibility between the coolant and breeder make this combination highly undesirable. The reaction

is very exothermic, and can proceed rapidly at the temperatures of interest.⁽⁴⁶⁾ There is even the possibility of explosion should the evolved H_2 come into contact with O_2 and an ignition source. Because of the high risk associated with this coolant and breeder combination, it fails the screening criteria and is not recommended for further study.

The use of helium coolant significantly reduces the possibility of energy release from Li_7Pb_2 -water reactions. But even then, an intermediate loop is desirable to better isolate the breeder from water in the working cycle. The designs for $\text{Li}_7\text{Pb}_2/\text{He}$ blankets are shown in Fig. X.2-6. In designs (a) through (c), tritium is recovered by allowing it to diffuse into the coolant. Despite the chemical inertness of helium, there is still a possibility of troublesome interactions between impurities in the coolant and unclad Li_7Pb_2 . The oxidation potential of Li_7Pb_2 probably does not differ substantially from that of pure lithium, and hence the oxygen concentration in the coolant must be kept very low to prevent oxide barriers from building up and inhibiting tritium release. Maintaining a low oxygen concentration is easier in systems which use helium as a low pressure purge gas rather than a high pressure coolant. With helium coolant, it is likely extreme measures will be required to maintain an oxygen partial pressure low enough to avoid building up oxide barriers or unclad Li_7Pb_2 . Because of the need for low oxygen partial pressure, the tritium partial pressure will be relatively high once tritium enters the helium coolant stream. To maintain a low O_2 concentration tritium must be recovered from the helium by getters rather than using O_2 to convert T_2 to T_2O .

The blanket design shown in Fig. X.2-6(d) recognizes some of these concerns, and includes a getter directly within the blanket. Tritium is recovered by batch processing in this design. Blankets requiring batch processing are not recommended for further study because they either have a high tritium inventory, or low plant availability due to frequent changeout.

Another concern with Li_7Pb_2 which discourages its use is its propensity to separate into two phases.⁽⁴⁷⁾ This separation has been shown to decrease the tritium diffusion coefficient enough that the thickness of a breeding slab should be no greater than fractions of a millimeter to maintain an acceptably low tritium inventory. The only blanket design satisfying this constraint is that in Fig. X.2-6(c). Because the breeder must be divided into such small pieces, it is virtually impossible to use cladding to avoid direct contact of Li_7Pb_2 with the helium coolant.

Considering the difficulty of using Li_7Pb_2 with any coolant, designs implementing this compound fail the screening criteria and are not being recommended for further study. It is concluded the advantages of using Li_7Pb_2 over the other solid breeders, such as Li_2O , are outweighed by the disadvantages.

X.3 Modified HTS Cooled Blanket Designs

Some characteristics of the optimum fusion blanket coolant are that it will operate at low pressure, have a high heat transfer coefficient, exhibit little parasitic neutron absorption, and be compatible with many candidate breeders over a wide temperature range. No other coolant appears to approach these characteristics to the degree the nitrate-nitrite salt known as HTS (or HITEC) does. Despite the tempting features of HTS, it also has some characteristics which detract from its appeal. The following paragraphs and the discussion in Appendix E attempt to put these advantages and disadvantages in perspective to enable HTS to be compared equitably with other coolants.

X.3.1 Selection of an HTS Cooled Blanket Design

X.3.1.1 Chemical Compatibility of Breeders with HTS

The first step towards identifying a viable HTS cooled blanket design is to determine which breeders are chemically compatible with HTS. Although no experiments have ever been conducted to determine the compatibility of HTS with either lithium or lithium compounds, some tentative conclusions can be drawn by examining the thermodynamics of reactions between HTS and various breeders. Vigorous reactions between the sodium nitrate in HTS and lithium in Li_7Pb_2 and $62\text{Li}-38\text{Pb}$ are thermodynamically possible. If the reaction kinetics between these breeders and HTS is analogous to that observed during experi-

ments with these breeders and water, then they should not be used with HTS. This last assumption needs to be verified by experiment, but will be assumed true for this study.

The chemical activity of lithium in ^{17}Li - ^{83}Pb at 932 K is three orders of magnitude less than the activity of pure lithium.⁽⁴⁸⁾ This accounts for the very modest reaction rate observed between ^{17}Li - ^{83}Pb and water during experiments.⁽⁴⁶⁾ It is anticipated this compound will also react very slowly with HTS. Hence, HTS and ^{17}Li - ^{83}Pb are expected to satisfy the compatibility criteria. To better resolve this issue, experiments need to be conducted with this breeder-coolant combination. Also, further thought must be given to determine the allowable reaction rate for a fusion blanket.

No thermodynamically possible chemical reactions between HTS and Li_2O or LiAlO_2 have been identified. Hence, these breeders are recommended for solid breeder blankets. If a multiplier is required, HTS and Pb are compatible. The compatibility of HTS with beryllium has yet to be investigated.

X.3.1.2 Thermal Hydraulics of HTS

Some general properties of HTS are listed in Table X.3-1 along with some properties relevant to thermal hydraulics. The convective heat transfer coefficient of HTS has been shown to behave like that of a normal fluid, and can be calculated using standard correlations.⁽⁴⁹⁾ For comparative purposes, the ratio of the heat transfer coefficient of HTS at 400°C to that of water at 300°C has been computed for pipes with equivalent dimensions. It was found the heat transfer coefficient of HTS is about half that of water at the same speed. Hence, to maintain the same heat removal rate in a blanket, either the speed of HTS must be increased to increase the heat transfer coefficient, or more coolant tubes must be used. If the speed is increased to make the heat transfer coefficient equivalent to that of water then the pumping power, which goes as the speed to the 3.7 power, increases by a factor of 40. Such a large increase would have a noticeable influence on the recirculating power fraction and should be avoided by increasing the coolant fraction in the blanket.

In computing the heat transfer coefficient and pumping power, the effect of the magnetic field was ignored. However, evaluation of the Hartmann number for typical reactor parameters shows viscous stresses in HTS are negligible compared to those induced by magnetic forces.⁽⁵⁰⁾ Hence, turbulence in the

TABLE X.3-1 PROPERTIES OF HTS AND A COMPARISON OF THE THERMAL HYDRAULIC CHARACTERISTICS WITH WATER

<u>PROPERTY</u>	<u>VALUES</u>	<u>COMMENTS</u>
Chemical composition	$\text{NaNO}_2(49\%) \text{ NaNO}_3(7\%) \text{ KNO}_3(44\%)$	
Melting temperature	140°C	Increases with decomposition
Thermal decomposition temp.	$460 - 500^\circ\text{C}$	$5\text{NaNO}_2 + 3\text{NaNO}_3 + \text{Na}_2\text{O} + \text{N}_2$
Density	1.76 gm/cm^3	@ 400°C
Viscosity	1.1 cp	@ 400°C
Thermal conductivity	$0.0057 \text{ W/cm}^\circ\text{C}$	@ 400°C
Specific heat	$1.55 \text{ J/g}^\circ\text{C}$	@ 400°C
$h_{\text{HTS}}/h_{\text{H}_2\text{O}}$	0.45	Assuming same geometry, coolant speed and H_2O at 300°C
$\frac{(\text{pumping power/length})_{\text{HTS}}}{(\text{pumping power/length})_{\text{H}_2\text{O}}}$	42	Increasing coolant speed to retain same heat transfer (fully turbulent) and pipe diameter
Electrical conductivity	0.59 mho/cm	Nominal value for molten HTS

fluid will be more strongly damped, and the boundary layer thickness will be less than without the presence of the field. Because of this effect, the heat transfer coefficient of HTS in Table X.3-1 may be overestimated and the pressure drop per unit length underestimated. But without experiments it is difficult to determine what corrections should be applied, because the flow field of HTS is determined by a combination of both inertial and magnetic forces. As a result, HTS does not act like a liquid metal in a magnetic field. In the latter case, both inertial and viscous forces are negligible compared to magnetic forces. Until more information is available on how the magnetic field influences the thermal hydraulic properties of HTS, the standard techniques applied to non-conducting fluids must suffice to compute the pressure drop.

X.3.1.3 Neutronics Aspects of HTS

To study the sensitivity of neutronics to the presence of HTS, some preliminary blanket designs were considered. All the neutronics studies were done in two dimensional cylindrical geometry. The first study was done with the HTS cooled ^{17}Li - ^{83}Pb breeding blanket shown in Fig. X.3-1(a). The dimensions and volume fractions are shown in Fig. X.3-2. A sensitivity study of tritium breeding to blanket thickness shows excessively thick blankets are required to limit leakage if natural lithium is used in the ^{17}Li - ^{83}Pb . This sensitivity is shown in Fig. X.3-3. Nearly all the breeding occurs in the ^6Li . Increasing the ^6Li enrichment for a 50 cm thick blanket, it becomes evident about 50% ^6Li enrichment is required to reach a breeding ratio of 1.2 in a 50 cm thick blanket.

To study the sensitivity of tritium breeding to the HTS volume fraction, the HTS was replaced with helium coolant. For a blanket relying heavily on ^6Li for breeding, the helium is neutronically like a void. It was found the tritium breeding ratio of the 50 cm thick blanket with natural lithium in the ^{17}Li - ^{83}Pb decreases only slightly. From this result, it is concluded that interactions of the neutrons with HTS do not significantly affect breeding in this blanket.

It is also interesting to compare how breeding in this same blanket increases to 1.4 when the HTS volume fraction is replaced with water. The breeding ratio increases because the hydrogen in water moderates the neutron

Figure X.3-1. Two Specific HTS Cooled Designs Proposed for the Mainline Study: a) A Liquid Breeder Concept; b) A Solid Breeder Concept

Figure X.3-2. The Zoning Scheme Used for the HTS Cooled Strawman Blanket

Figure X.3-3. The Tritium Breeding Ratio of the HTS Cooled Strawman Blanket

energies rapidly enough that the neutrons are captured in ${}^6\text{Li}$ before they can be absorbed in the steel or leak out.

The second preliminary blanket with which neutronics studies were conducted (shown in Fig. X.3-1(b)) is identical to the water cooled ternary oxide blanket discussed in Section IX.5, except the water is replaced with HTS. The tritium breeding ratio of the water cooled blanket was calculated to be 1.28. When water is replaced with HTS, the breeding ratio rises to 1.5. In both cases the breeding contribution from ${}^7\text{Li}$ is negligible. The reason for the increase in breeding when water is replaced with HTS is that HTS does not moderate neutrons nearly as much, and allows them to easily pass through the front of the blanket to mid-blanket regions where the atom fraction of breeding material is higher. In effect, the water cooled blanket is over moderated and does not breed as efficiently as less moderating coolants. On the other hand, an HTS cooled blanket will tend to have higher leakage, which could lead to thicker blankets.

Both preliminary blankets described above contain lead, which provides neutron multiplication via $(n,2n)$ reactions, but prevents ${}^7\text{Li}$ from contributing significantly to tritium breeding. For blankets without multipliers, such as an HTS cooled Li_2O breeder design, the contribution from ${}^7\text{Li}$ must be significant if breeding is to be acceptable. The presence of HTS may have a detrimental effect in such a blanket if scattering with HTS atomic components moderates many neutrons below the 2.5 MeV threshold of ${}^7\text{Li}$. Further neutronics studies should be conducted to better quantify the effects of using HTS in such blankets.

X.3.1.4 Chemical Compatibility of HTS with Structures

The chemical compatibility of various candidate structural materials with HTS has also been considered. A preliminary materials choice can be made based on short term (a few hundred hours) compatibility test results published by the HTS vendor,⁽⁵¹⁾ as well as the experience of the chemical industry. Bohlmann provides a good review of industrial experience.⁽⁵²⁾ It is clear from the short term tests that chrome steels and inconel are much more corrosion resistant than carbon steels, aluminum, magnesium, and copper. However, corrosion can occur even in chrome steels if cathodic conditions prevent protective oxide coatings from forming.⁽⁵³⁾ Penetration in these

latter metals tends to follow the classic $(\text{time})^{1/2}$ behavior. Molybdenum is also expected to be the corrosion resistant. The corrosion characteristics of vanadium with HTS needs to be investigated. Graphite reacts strongly with HTS and should be avoided. Water and HTS are very compatible and are often used together to fill HTS loops at temperatures lower than the HTS melting temperatures. The HTS is dried by extracting the water as steam.

Only within the last decade has a long term testing program begun to enable a more refined choice of structural material to be made. Emphasis has focused primarily on chrome steels. It has been found corrosion decreases exponentially as the chrome content increases up to 25%.⁽⁵³⁾ Although these results are promising, forced convection loop experiments are needed to better quantify corrosion rates. The corrosion mechanisms appear to be identical to those of steels in air, but the corrosion rates are slightly more rapid with HTS. It is important that impurities such a NaOH be prevented from forming, as they aggravate corrosion by intergranular attack. The possible mechanisms for forming NaOH need to be better identified.

An additional corrosion mechanism arises from the electrical potential between the channel walls and coolant formed by flowing the coolant across a magnetic field. The magnitude of the potential, given by Ohms law, is the product of the coolant speed, the perpendicular magnetic field, and the coolant channel diameter. It is estimated a potential over 0.5 V could electrolyze the components of HTS and make it corrosive to tube walls.⁽⁵⁴⁾ Assuming a magnetic field of 8 T, the product of coolant speed and tube diameter is then limited to $620 \text{ cm}^2/\text{s}$ to stay below the breakdown potential. Hence, for a 1 cm diameter tube the coolant speed is limited to 6.2 m/s, which is certainly not very restrictive.

The product of coolant speed times tube diameter is also limited by the Reynolds number required for turbulent heat transfer to occur. It is not known what this Reynolds number will be because, as was previously discussed, the flow is dominated by a combination of both magnetic and inertial forces. The fact that the boundary layer thickness is dominated by magnetic stress rather than viscous stress should be considered in computing corrosion and mass transfer rates when these rates are boundary layer dependent.

X.3.1.5 Tritium Confinement Characteristics of HTS

One of the very positive features of HTS is its tritium confinement characteristics. These characteristics are favorable for two reasons. First, the oxide coating which HTS deposits on steel pipes acts as a tritium barrier, discouraging tritium leakage out of the breeder region and into the coolant. Once tritium does permeate into the coolant, the oxide layer inhibits it from leaking out through the heat exchanger and into the working fluid. This barrier has been estimated to reduce the diffusion rate by a factor of approximately 10^2 . The second favorable characteristic of HTS is that the partial pressure of T_2O is over a factor of 10^{10} greater than the partial pressure of T_2 ,⁽⁵²⁾ so nearly all the tritium which does manage to leak into the coolant is in the form of steam rather than free tritium. Unlike free tritium, steam does not permeate easily through heat exchanger walls and into the working fluid. Because of these characteristics, HTS cooled blanket designs will not require an intermediate coolant loop.

To illustrate how well HTS prevents tritium from reaching the environment the tritium cycle of an HTS cooled ^{17}Li - ^{83}Pb tokamak blanket was considered. The blanket was assumed to have a major radius of 6 m, a first wall radius of 3.5 m, and a breeding blanket thickness of 50 cm. The volume fraction of HTS in the blanket was assumed to be 10%, with the remainder being breeding material. A schematic of the tritium cycle is shown in Fig. X.3-4. For a neutron wall loading of 5 MW/m^2 , the blanket tritium inventory is about 1 kg if the breeding material is vacuum degassed daily. With this inventory, and assuming a Sievert's constant of $0.05 \text{ wppm/Torr}^{1/2}$, the tritium partial pressure in the ^{17}Li - ^{83}Pb is about 15 Torr. About 10^3 Ci/day permeates back into the plasma if a 1 mm thick stainless steel first wall is used. If the HTS cooling tubes in the blanket are 2.5 mm thick and the tritium permeability is reduced by only an order of magnitude by the oxide coating on the HTS side, the permeation rate into the primary loop is 10^4 Ci/day . If the tritium inventory in the primary loop is allowed to build up for one day, it will amount to 1 gm. Only one part in 10^{10} will exist as free tritium, the rest being in the form of steam. Provided the reaction kinetics of free tritium conversion to T_2O is rapid, the partial pressure of free tritium in the primary loop is then very low, and the permeation rate through the heat exchanger walls and into the working fluid will be much less than the limit of

Figure X.3-4. The Tritium Cycle for an HTS Cooled Breeding Blanket

100 Ci/day set by the screening criteria. The T_2O can easily be extracted out of the salt for reprocessing.

X.3.1.6 Radiolytic Decomposition of HTS

Perhaps the greatest concern with using HTS as a fusion reactor coolant is its stability under neutron and gamma irradiation. If large chemical reprocessing facilities are required to reconvert alkali and gas formed by radiolytic decomposition back to the original constituency, then this is a significant disadvantage. The earliest investigation was reported by O. Sisman at ORNL sometime prior to 1960:

"Three samples of HTS were irradiated at this laboratory by O. Sisman to establish the nuclear radiation stability of the salt. The samples received a thermal dose of 3.3×10^{18} neutrons/cm² and an epithermal dose of somewhat less than half the thermal dose. Examination of the irradiated samples indicated that the salt is rendered more hygroscopic by radiation and that it also undergoes some breakdown, yielding, in part, gaseous decomposition products."⁽⁴⁹⁾

Unfortunately, the experiment does not seem to have been formally reported. The reasons for the breakdown or the amount of breakdown observed are not discussed.

Experiments were also conducted at Argonne National Laboratory to investigate the radiolytic stability of HTS to gamma radiation. The investigators came to a more optimistic conclusion:

"The salts were exposed to 0.8×10^9 rads of gamma radiation in the ⁶⁰Co facility at the Argonne National Laboratory and simultaneously heated to temperatures in excess of 530°C. A helium gas stream circulated over the salts was analyzed for decomposition products. It was found that there was no observable thermal or radiolytic decomposition of either salt. Although the exposure was equivalent to only about one minute in a controlled thermonuclear reactor, the results were very encouraging and suggest that further experimentation on molten nitrate-based salts is warranted."⁽⁵⁵⁾

Further experiments with the salt showed it did indeed undergo breakdown, but the breakdown was due to thermal decomposition rather than radiolytic

decomposition.⁽⁵⁶⁾ A maximum operating temperature of $<530^{\circ}\text{C}$ was recommended to avoid thermal decomposition.

Considering the sparsity of data concerning the radiolytic stability of HTS, it is impossible to draw definitive conclusions about its feasibility as a fusion reactor coolant. Without further experimental evidence, the effect on emission control and reactor economics cannot be thoroughly evaluated. But unless it can be shown the radiolytic decomposition rate of HTS is clearly untenable, it is recommended this coolant be retained as a candidate for fusion reactor applications.

X.3.1.7 Emissions Control with HTS

One of the unattractive features of HTS is its propensity to become highly radioactive in the radiation environment of a fusion reactor. The levels of radioactivity expected and the emission control needs are described in Appendix E. It is concluded in Appendix E that adequate emission control is potentially feasible. But it is also pointed out the emission control requirements could entail significant economic penalties should high rates of radiolytic decomposition lead to high rates of chemical reprocessing. Thus, the emission control requirements cannot be evaluated in detail until the radiolytic decomposition rates are known.

X.3.2 Blanket Parameters for HTS Cooled Solid and Liquid Breeder Blankets

To focus on some specific HTS cooled blanket designs, the liquid breeder concept and the solid breeder concept shown in Fig. X.3-1 were chosen. The solid breeder concept is similar in design to the water cooled ternary oxide blanket described in Section IX.5, except the water is replaced with HTS. The liquid breeder design is also similar to the water cooled ternary oxide blanket, except the tubes for the solid breeder are removed, the lead is replaced with $^{17}\text{Li}-^{83}\text{Pb}$, and the water is replaced with HTS. The first wall concept is only conceptual in these designs, and no attempt at detailed engineering was made.

A parameter description for each blanket is given in Table X.3-2. For the liquid breeder design, it was found either PCA or ferritic steel could be used as the structure. The parameters listed are for a blanket using PCA as the structure. Liquid metal embrittlement is assumed to impose a minimum

TABLE X.3-2 BLANKET PARAMETERS FOR TWO BREEDER CONCEPTS
RECOMMENDED AS CANDIDATES FOR THE MAINLINE STUDY

GENERAL DESCRIPTION	UNITS	LIQUID BREEDER	SOLID BREEDER
Materials			
Coolant		HTS	HTS
Breeder		$^{17}\text{Li}-^{83}\text{Pb}$	LiAlO_2
Neutron multiplier		---	Pb
Structure		PCA	Ferritic
Major design parameters			
Avg. neutron wall load	MW/m^2	5.0	5.0
Avg. FW surface heat flux	MW/m^2	0.5	0.5
Coolant			
Inlet/outlet temperature	$^{\circ}\text{C}/^{\circ}\text{C}$	270/380	370/430
Maximum pressure	MPa	.1	.1
Breeder			
Minimum/maximum temperature	$^{\circ}\text{C}/^{\circ}\text{C}$	290/430	430/750
Maximum pressure	MPa	.1	---
Structure			
FW/B maximum temperature	$^{\circ}\text{C}$	450	450
Minimum/maximum temperature			
Coolant interface	$^{\circ}\text{C}/^{\circ}\text{C}$	250/400	390/450
Breeder interface	$^{\circ}\text{C}/^{\circ}\text{C}$	290/430	420/480
Neutronics			
tritium breeding ratio			
1-D, 100% coverage	---	1.2 - 1.4 (50%-100% ^6Li)	1.2 - 1.4 (50%-100% ^6Li)
Net (3-D with all geometrical details and penetrations)	---	TBD	TBD
Maximum nuclear heating rates			
Breeder	W/cm^3	36	78
Coolant	W/cm^3 (estimate)	5.26	5.26 (estimate)
Neutron multiplier	W/cm^3	---	74
Structure	W/cm^3	37	37
Energy lost to shield	% total	<< 1	<< 1

TABLE X.3-2 (CONTINUED)

GENERAL DESCRIPTION	UNITS	LIQUID BREEDER	SOLID BREEDER
Breeding & power fraction (Tokamak)			
Inboard/outboard breeding	%	~ 30/70	~ 30/70
Tritium removal from breeder method	--- degassing	Vacuum	He purge
Power loss (thermal)	% total	< 1%	< 1%
Steady-state breeding tritium inventory	g	< 10 ³	TBD
Purge gas (if applicable)			
Material	---	---	He
Temperature	°C	---	TBD
Pressure	MPa	---	Low
Mass flow rate	kg/s	---	TBD
FW/B design description			
Inboard first wall/blanket			
Thickness (inc. manifolds)	m	0.7	0.7
Manifold thickness	m	0.2	0.2
% STR/% CLNT in manifold region	%/%	5/95	5/95
Coolant Δ P (total)	MPa	≤ 0.1	≤ 0.1
Coolant pumping power	% total	≤ 1	≤ 1
Thermal efficiency			
$\left[\eta = \frac{(MW_e)_{out} - (MW_e)_{pump}}{MW_{th\ TOTAL}} \right]$	%	~ 36%	~ 39%
Steady state tritium losses	Ci/d	<< 100	<< 100

breeder to structure interface temperature of 290°C. The maximum breeder to structure interface temperature is 430°C, as imposed by the project guidelines for the interface temperature between PCA and ^{17}Li - ^{83}Pb . It was estimated that with these temperature limitations, the inlet to outlet coolant temperatures would be 270 to 380°C. Coolant speeds of 1 m/s were assumed in this analysis.

Only ferritic steel (or certain non-steel structures) can be used as the structure for the solid breeder design. Liquid metal embrittlement was assumed to impose a minimum lead to structure interface temperature of 380°C, which is about 50°C above the melting point of lead. Hence, the temperature window for PCA, which has an upper limit of 430°C, is virtually closed. With ferritic steel the maximum temperatures are fixed by the decomposition limit of HTS, which for this study was conservatively assumed to be 450°C. The inlet to outlet coolant temperatures are estimated to be 370 to 430°C. Coolant speeds of 1 m/s were assumed in this analysis.

Neutronics studies for both blankets indicate ^6Li enrichment will be required to reach the breeding goal. When ^6Li is enriched between 50 and 100%, one dimensional calculations showed breeding ratio varies from 1.2 to 1.4 for both blankets.

The method of tritium removal suggested for the solid breeder is by helium purge. The details of this method are described in the chapters on solid breeders. The method of tritium removal for the liquid breeder is vacuum degassing. This technique is described in other blanket designs using ^{17}Li - ^{83}Pb .⁽¹⁰⁾

Although these coolant/structure/breeder combinations are recommended for further consideration, it is recognized the proposed mechanical designs are far from optimal. In the future, it is recommended a pod configuration which takes advantage of the low coolant pressure be considered. Once the mechanical design is fixed, the blanket design can be optimized.

X.4 Helium Cooled FLIBE Blanket Design Concept

The advantages of FLIBE as a breeder in fusion-electric blankets have motivated several conceptual blanket designs over the past ten years.^(37,42,57) Those advantages most often cited include excellent irradiation stability, in-situ tritium breeding, low pressure operation, and various chemical compatibility and safety advantages. Unfortunately, the disadvantages of FLIBE as a coolant have, in many cases, precluded the selection of FLIBE blankets for further consideration. The most important disadvantages of FLIBE use are its low lithium density and high fluorine density. Together, these have prevented existing FLIBE designs from providing a tritium breeding ratio which is high enough to overcome breeding losses due to leakage and parasitic absorption.

Other disadvantages associated with the use of FLIBE as a coolant include its poor thermal conductivity and its relatively high viscosity (see Table X.4-1).⁽⁸⁾ These can lead to hot spots and/or high pressure drops and pumping powers--especially considering the high surface power densities ($\sim 50 \text{ W/cm}^2$) currently being considered for tokamaks. The high melting point of the 66 LiF-34 BeF₂ FLIBE (460°C) has required the specification of exotic structural alloys such as TZM. Although the 47LiF - 53BeF₂ FLIBE has a lower melting point (363°C), its much higher viscosity can lead to unacceptable pressure drops and pumping powers at reasonable neutron wall loadings.⁽⁵⁷⁾

Two additional issues associated with the use of FLIBE in fusion blankets are the possibility of salt breakdown due to the strong magnetic field and TF (hydrofluoric acid) formation due to lithium transmutation followed by recombination. In the latter case, TF formation can be eliminated in a practical sense by the use of a reducing agent in the salt (e.g., excess beryllium). But such use will result in the formation of T₂--the tritium form most difficult to contain in the fusion environment.

In an effort to preserve the advantages of FLIBE while eliminating many of the disadvantages, a new blanket concept based upon the use of FLIBE as a breeder but helium as the coolant and lead as the neutron multiplier, is being proposed.

Table X.4-1 Physical Properties of FLIBE Salts

COMPOSITION (MOLE %) Li:Be		47 – 53	66–34
MELTING POINT (°C)		363	460
DENSITY, ρ (g/cm ³)	TEMP (°C)		
	400	2.05	2.05
	500	2.01	2.00
	600	1.97	1.96
	700	1.93	1.92
VISCOSITY, η (cp)	TEMP (°C)		
	400	505.8	—
	500	119.5	14.9
	600	39.3	8.6
	700	16.2	5.5
SPECIFIC CONDUCTANCE K (Ω^{-1} cm ⁻¹)	TEMP (°C)		
	400	0.27	—
	500	0.61	1.55
	600	1.02	2.14
	700	1.49	2.73
HEAT CAPACITY, c_p (W-sec/g°C)		2.35	2.38
THERMAL CONDUCTIVITY, λ (W/cm°C)		0.0080	0.0100

X.4.1 Concept Overview

A schematic of the helium cooled FLIBE blanket concept is shown in Fig. X.4-1. The proposed configuration is similar to other gas cooled designs being investigated in conjunction with the Blanket Comparison Selection Study. Specifically, the structural design of the high pressure (50 atm) gas cooled pod is essentially the same as that being investigated by GA Technologies for the helium cooled lead-lithium multiplier/breeder design. However, in the FLIBE design concept the lead-lithium is replaced by a two zone design consisting of several rows of horizontally oriented tubes containing liquid lead followed by several rows of tubes consisting of FLIBE (47 LiF- 53 BeF₂). A ferritic steel structure was chosen to avoid concerns regarding the compatibility of stainless steel and liquid lead (assuming that the corrosive properties of lead are similar to those of lead-lithium).

In comparison with other gas cooled tube designs, two features of the FLIBE breeder design are worthy of note. First, the breeder tubes are each closed. Although possible, no provision for venting or purging the tubes is included. In contrast with the purged breeders, this design has eliminated the added complexity of the thousands of interconnections. Potentially, this design philosophy can result in increased safety by isolating the tritium in each tube, and increased reliability (by eliminating a major reactor subsystem). In the proposed concept, the bred T₂ would diffuse through the tube walls, become oxygenated to T₂O at the outer tubes surface, and be collected in high efficiency molecular sieves by use of a slip stream.⁽¹⁰⁾ Since it is quite likely a substantial amount of tritium will diffuse or leak into the main coolant stream of any helium cooled design, the proposed concept exacerbates the situation in only a qualitative sense (i.e., a larger slip stream for tritium removal). Preliminary calculations (below) indicate this approach has a tolerable cost and effect on plant efficiency.

Another advantageous feature of the design is the energy partitioning which results from breeding in the back of the blanket. Specifically, in a helium cooled lead-lithium breeder/multiplier blanket, the heat generation in the first row of tubes is higher than in the lead multiplier proposed here. This results because ⁶Li(n, α) neutron capture energy would be included in the lead-lithium. Because the coolant film drop will be lower for lower energy deposition, the proposed design could relax concerns relating to the

Configuration - high pressure pod with inertial breeder and multiplier in separate tubes

Coolant - helium

Structural materials - ferritic steel

Breeder - FLIBE: 47 LiF-53 BeF₂ (m.p. = 360°C)

Multiplier - lead

Figure X.4-1. Schematic of the Helium Cooled FLIBE Blanket Concept

structure/multiplier interface. However, it should be noted that a two zone design with lead in the first zone and lead-lithium in the second zone would accomplish the same objective.

Another important feature of the proposed design is that both the lead and the salt are molten during operation regardless of the blanket power level (due to the 325°C coolant inlet temperature and the 500°C outlet). During shutdown, both fluids can freeze within the individual pipes and can re-expand upon restart. Adequate void and partitioning would be provided in each tube to prevent bursting upon melting.

To assure T_2 formation rather than TF formation, each FLIBE tube would contain a small piece of beryllium such that the fluid is supersaturated and any excess fluorine forms BeF_2 .^(57,58) This goal can be achieved with very small quantities of beryllium. Only about 500 kg of beryllium is needed to replace all of the tritium bred in a 1 GWe power plant in three years.

A principal goal of the helium cooled FLIBE blanket concept is to minimize tube reliability issues. Although the overall number of tubes in a 1 GWe power plant is on the order of 2×10^5 , each tube is an individual structural component. If a tube leaks or bursts, but remains attached at the module ends and retains its configurational stability, it is expected that the fluid (either lead or FLIBE) would be transported into the primary loop gas flow where it could be separated from the gas in a low velocity chamber or cold trap designed for this purpose. Since the gas inlet temperature (375°C) is above the FLIBE melting point (363°C), tube plugging due to freezing is not a concern. Also, because the tubes do not provide blanket cooling, a tube burst is not expected to lead to an unacceptable heat transfer situation in the blanket. As a result, the helium cooled FLIBE blanket could be expected to continue operation despite the rupture or leakage of up to a few percent of the lead and FLIBE tubes. In the remainder of this section, several potential feasibility issues for the proposed concept are discussed.

X.4.2 Key Feasibility Issues

Thermal Design Issues

Several thermal design constraints have been identified for the blanket design concept. These include the following:

- Ferritic steel operation above $\sim 350^{\circ}\text{C}$ at first wall to minimize the change in ductile to brittle transition temperature;
- Ferritic steel operation below $\sim 500^{\circ}\text{C}$ to minimize thermal creep;
- Lead/steel interface $> 375^{\circ}\text{C}$ to avoid liquid metal embrittlement and $< 500^{\circ}\text{C}$ for compatibility
- Helium inlet above 363°C FLIBE melting point to prevent primary loop plugging and outlet below $\sim 600^{\circ}\text{C}$ for compatibility.

As a result of these design constraints, a 375°C helium inlet temperature, a 500°C helium outlet temperature, and a helium coolant pressure of 50 atm were selected. Although the required inlet temperature exceeds that specified in several other helium cooled designs, a neutron wall loading of 3 MW/m^2 appears reasonable for adequate first wall heat transfer. At 3 MW/m^2 the maximum lead/steel interface temperature, the key remaining concern, is predicted to be $< 500^{\circ}\text{C}$ if a 2 cm o.d. tube size is chosen. Because the heat flux transferred to the coolant from the lead and FLIBE tubes scales as the tube diameter, thermal control in the blanket can be controlled in this manner without further reductions in the neutron wall loading.

X.4.2.1 Mechanical Design Issues

The high pressure gas pod concept proposed for the helium cooled FLIBE breeder design is similar to other gas cooled first wall/blanket concepts (e.g., the helium cooled lead-lithium and lithium oxide designs) pursued as part of the Blanket Comparison and Selection Study. The issues associated with the gas cooled pod were covered earlier in this report (see Sections VII.2.1 and IX.4) and are not discussed in this section. As mentioned above, the principal difference between the helium cooled FLIBE blanket and other helium cooled blankets are the higher coolant inlet temperature of 375°C , which is compensated by reducing the neutron wall loading to 3 MW/m^2 , and the

concept of individual breeder tubes which are not purged to remove tritium.

Key mechanical design issues for the helium cooled FLIBE blanket involve the design of the lead and FLIBE tubes to accommodate freeze/melt cycles without draining, and the accumulation of helium gas in the FLIBE tubes as tritium is produced (via the neutron + ${}^6\text{Li} \rightarrow \alpha + \text{tritium}$ reaction) and diffuses through the tube wall into the primary coolant stream.

The principal issue for the lead tubes is accommodation of the freeze/melt cycles. Because lead expands about several percent by volume upon melting, a local void fraction must be provided in each tube. The suggested approach is to orient the lead and FLIBE tubes horizontally and to provide about 20% void in each lead tube when the lead is frozen (13.6% void when melted). To balance the coolant pressure, the tube must be internally pressurized to 50 atmospheres when hot. Therefore, the fabrication procedure for each individual tube is to orient the tube vertically, fill it to 86.4% capacity, pressurize the remainder with helium to 50 atm and seal the tube under pressure. When cold, the tube internal pressure would be 34 atm, but the tube would be designed to accommodate the full 50 atm with full depressurization on the outside. For a 2 cm o.d. tube and a 20 ksi design stress (based upon HT-9 ferritic steel thermal creep), the minimum tube wall thickness is 0.37 mm. A minimum tube wall thickness specification of 0.50 mm for fabricability and weldability will limit the design stress to 15 ksi and will require a 10% steel fraction in the lead tubes.

For the FLIBE breeder tubes, expansion upon melting is not an issue because the volume fraction increases upon melting is negligible.⁽⁵⁸⁾ More importantly, a larger void fraction must be provided to accommodate the pressure rise due to helium generation. To address this issue, the following model for the tube pressure increase was developed.

$$\Delta P(\text{atm}) = \frac{.418 T \beta P_f^{1/2} C_f Y P_{op}/300}{f_t \left(\left(\frac{1}{2\pi} \left[\frac{.8P}{\alpha \Gamma} f \right]^{1/2} + \Delta X_{1,0} + \Delta X_{2,0} \left(\frac{1-f}{1-f_y} \right)^2 - \left(\frac{1}{2\pi} \left[\frac{.8P}{\alpha \Gamma} f \right]^{1/2} + \Delta X_{1,0} \right)^2 \right) f_y \left[\frac{.8\alpha}{\Gamma} \right]^{1/2}}$$

where:

P_{op}	=	operating temperature ($^{\circ}K$)
T	=	tritium breeding ratio (typical value - 1.1)
β	=	peak/average tritium breeding factor (2)
P_f	=	fusion power (3000 MW)
C_f	=	average plant capacity factor (0.7)
Y	=	blanket life (3 calendar years)
α	=	tokamak aspect ratio - major radius/minor radius (3)
Γ	=	average neutron wall loading (3 MW/m ²)
$\Delta x_{1,0}$	=	average distance between plasma edge and beginning of FLIBE zone (e.g., scrape-off + first wall + multiplier thickness (0.45 m)
Δ	=	reference breeder zone thickness (including coolant void between pipes) corresponding to a reference void fraction $f_{y,0}$. Both quantities derive from neutronics. (0.35)
$f_{y,0}$	=	reference helium void fraction in FLIBE pipes as above (0.4)
f_y	=	helium void fraction in pipes (this case)
f_t	=	tube fraction in breeder (0.85)

Results for this model are shown in Fig. X.4-2. These results indicate a 3 calendar year (2.1 full power year) blanket life at a neutron wall loading of 3 MW/m² can be achieved if a 50% average void fraction is provided in the FLIBE breeder tubes and the tubes are designed for 100 atm pressure (i.e., 50 atm initial pressure plus 50 atm buildup at end-of-life).

Recognizing that the tritium breeding ratio per unit volume of the FLIBE breeder is peaked toward the front of the FLIBE tube zone, where the neutron flux is higher by a peaking factor of about 2, it follows that to achieve the average void fraction it is necessary to compensate for this effect by having a greater void fraction than 50% in the front of the breeder zone and a fraction lower than 50% in the back of the zone. Although the details of such compensation have not been determined at this stage of the design, such a determination is straightforward and will not drastically perturb the

Figure X.4-2. Typical Helium Buildup Results

design. For example, if a 50% void fraction in the first row of FLIBE tubes is shown to produce a helium pressure increase of twice the desired level at end-of-life, use of a 66.6% void fraction will reduce the pressure increase by the required factor of two. Similarly, the last FLIBE tube might produce half the desired pressure increase with a 50% void. In this case, the void fraction would be reduced to 33.3%.

As shown in Fig. X.4-3, for an end-of-life (EOL) pressure of 100 atm the thickness of the 2 cm o.d. FLIBE tubes is 0.74 mm. This results in a tube structure fraction of 15.4%.

X.4.2.2 Nuclear Design Issues

In contrast with previous FLIBE blanket designs, the helium cooled FLIBE blanket proposed here has the potential to provide excellent tritium breeding. This results from the use of a very high volume fraction of lead (56%) in a front breeder zone where the number of $n,2n$ reactions leading to breeding enhancement can be maximized. In the back zone, a ^6Li enrichment of ~ 90% (and possibly lower in an optimized design) is sufficient to provide a large enough macroscopic absorption cross section to effectively limit parasitic absorption in the steel structure. Although ~ 700 kg of ^6Li is burned out over the blanket lifetime, the overall perturbation in the ^6Li enrichment is very small and the effect on breeding will be negligible.

The hard neutron energy spectrum in this blanket is advantageous in the sense that neutron diffusion paths will be long and heterogeneous effects will be minimal. But a problematic side effect is that the blanket must be quite thick to achieve its full potential tritium breeding ratio. The approach of using a thinner blanket on the inboard side of the tokamak and a thicker blanket on the outboard, top, and bottom sides is considered to be key issue.

To investigate the nuclear performance of the helium cooled FLIBE blanket, the TARTNP Monte Carlo code⁽⁵⁹⁾ and the ENDL cross section data base⁽⁶⁰⁾ were used in 1-D cylindrical geometries. A typical blanket/reflector geometry with material fractions is shown in Fig. X.4-4. As shown, a 15 cm lead multiplier zone with 81% tube packing, 5% added steel for pod walls and tube sheets, and 14% net void is assumed to follow a 1 cm thick (effective thickness) first wall. A FLIBE breeder zone of 55 cm and the same tube arrangement follows the multiplier zone and is followed by a 10 cm silicon

Figure X.4-3. FLIBE Tube Structure vs. EOL Pressure

MULTIPLIER TUBES (80.75% OF REGION VOLUME)

10% Fe 70% Pb 20% VOID

BREEDER TUBES (80.75% OF REGION VOLUME)

15.4% Fe 42.3% FLIBE 42.3% VOID

FLIBE COMPOSITION (LiFBeF₂)

47% LiF 53% BeF₂

Figure X.4-4. TARTNP Neutronics Model of the Helium Cooled FLIBE Blanket

carbide reflector (80% dense). This configuration with a breeding ratio of 1.29, a net leakage of 0.09, and a blanket energy multiplication of 1.2 (normalized to 14.1 MeV) resulted from several optimization studies of 80 cm blankets. Breeding results for blankets of variable thicknesses are shown in Table X.4-2. As shown, breeding ratios from 1.18 for a 70 cm blanket to 1.33 for a 110 cm blanket result.

Although these results continue to indicate the possibility of a thick blanket, two corollary observations are of interest. First, to recapture some of the leakage neutrons, the use of a water-cooled steel reflector was recognized to be more effective. Second, in other neutronics runs where the steel structure fraction in the breeder tubes was varied between 10 and 20% (15.4% nominal), the net tritium breeding was constant. This occurred because increasing the structure increased parasitic absorptions, but decreased the neutron leakage.

To better utilize the above observations, a water cooled steel reflector was added to the model and an attempt was made to combine a thin blanket on the inboard side with a thick blanket elsewhere. For this purpose a linear weighting model was used to approximate the breeding ratio. In this case, 1-D results for the inner blanket were given a 30% weighting factor and combined with a 70% weighting factor for the remainder of the blanket. This model is expected to provide conservative results. As shown in Table X.4-3, a thin 56 cm inner blanket, including 10 cm for helium manifolds, can breed 0.92 tritons per source neutron in the 1-D model. When combined with a thick outer blanket breeding ratio of 1.34, a net global breeding ratio of 1.2 is achieved.

X.4.2.3 Tritium Inventory Issues

As mentioned earlier, the tritium processing scheme for the helium cooled FLIBE blanket involves the permeation of tritium through the steel tubes and recovery from the primary coolant. In this section, issues involving the resulting inventory of tritium in the breeder tubes are discussed.

Beryllium seeding of the FLIBE tubes to form T_2 and BeF_2 instead of TF is expected to be very effective with only negligible quantities of tritium dissolved in the FLIBE.⁽⁵⁸⁾ Therefore, for the purpose of this analysis, dissolved tritium as a contributor to the blanket inventory has been ignored.

Table X.4-2 He Cooled FLIBE Blanket Performance at Various Blanket Thicknesses

CASE	REGION THICKNESSES (cm)*				LEAKAGE	TBR	M
	MULTIPLIER	BREEDER	REFLECTOR	TOTAL			
1	10	50	10	70	0.13	1.18	1.18
2	15	50	10	75	0.12	1.24	1.18
3 (NOM)	15	55	10	80	0.09	1.29	1.20
4	20	40	30	90	0.06	1.30	1.22
5	20	60	30	110	0.03	1.33	1.22

*NOT INCLUDING 1 cm FIRST WALL

Table X.4-3 Tritium Breeding Using a Thin Inner Blanket

	<u>INBOARD</u>	<u>OUTBOARD</u>
MULTIPLIER	5 cm	15 cm
BREEDER	30 cm	55 cm
REFLECTOR	5 cm	10 cm
MANIFOLDS	10 cm	20 cm
TOTAL	56 cm*	106 cm*
TBR	.92	1.34
WEIGHTING FACTOR	30%	70%

$$\begin{aligned}\text{AVERAGE TBR} &= .92 \times .3 + 1.34 \times .7 \\ &= 1.2\end{aligned}$$

*INCLUDES CORRECTIONS FOR POD GEOMETRY

The larger contributor to the inventory is the quantity of T_2 in the voids of the FLIBE tubes necessary to drive the steady state permeation of tritium into the primary coolant at the same rate it is produced. The model used to apply this analysis is described by:

$$\frac{dP_{T_2}}{dt} = \frac{RT}{V} \left[\beta - K \phi_T \frac{A}{X} \Delta P_{T_2}^{1/2} \right]$$

where:

- P = pressure (atm)
- R = gas constant (0.0821 l - atom/mole $^{\circ}$ K)
- t = time (seconds)
- T = temperature in $^{\circ}$ K
- V = void volume of reactor tube (770 cm³)
- β = production rate of T_2
- K = constant to convert permeability to proper units
- ϕ = permeability
- A = surface area across which permeation occurs (0.077 m²)
- X = thickness of tube wall (0.75 mm)

In this model, a tube void fraction of 50% was used to adjust the tube surface area available for permeation. The reactor was assumed to produce 168 kg/yr of tritium (3000 MW_f) in 180,000 individual tubes of 2 cm o.d. and 4.9 m length.

Because equilibrium tritium concentrations are reached very quickly (~ days) the above equation can be reduced to its steady state form:

$$P_{T_2} = \left[\frac{PX}{K \Phi_A} \right]^2$$

This equation was applied to the FLIBE tubes assuming both oxidized (which is expected) and unoxidized conditions. The tritium permeability value used for the un-oxidized ferritic steel was $1.146 \text{ mm}^3(\text{STP}).\text{mm}/\text{m}^2.\text{sec}.\text{Pa}^{1/2}$ at 450°C . For the oxidized tubes, the value was reduced 150 fold.

The results provided by this model indicate a total tritium inventory of 0.68 kg for the oxidized tubes, or 0.004 g per tube. These results are considered to be encouraging, especially when observing that a system wide tritium release appears to be impossible and the tritium activity accessible due to a single tube failure is only about 40 Curies. Because a breeder tube failure would release tritium only into the primary coolant tritium processing loop, it is difficult to imagine an accidental release of significant public consequence which could be initiated in the blanket.

X.4.2.4 Tritium Control Issues

Blanket designs which release the bulk of bred tritium into the primary coolant are legitimately suspect in two areas. First, the ability to adequately contain tritium and limit normal releases to 10-100 Ci/day requires investigation. Second, if the first issue can be resolved the cost and power flow implications must be acceptable. In this section, these issues are addressed via scoping calculations.

Concerning possible normal release paths, two are apparent: leakage to the secondary coolant loop and leakage from the reactor building atmosphere. The design philosophy used to address these issues for the helium cooled FLIBE breeder design is to provide a double walled stream generator or intermediate loop and to maintain low tritium inventories in both the primary loop and the building atmosphere. Tritium control in the primary loop would be accomplished by first oxygenating the T_2 as it permeates through the breeder tubes. This would be followed by the use of a process slip stream and molecular sieves to collect T_2O of the type studied in the MARS program.⁽¹⁰⁾ Tritium control in the reactor building would be accomplished using air dryers of the type studied in the FED/INTOR programs.⁽⁶¹⁾

The equation used to model the slip stream requirements is given by

$$f = \frac{R \rho_{\text{He}} C_p^{\text{He}} \Delta T}{Q \eta \rho_t}$$

where:

- f = slip stream fraction,
- R = tritium generation rate,
- ρ_{He} = helium density,
- C_p^{He} = helium specific heat,
- ΔT = Coolant ΔT ,
- Q = blanket power,
- η = tritium recovery efficiency,
- ρ_t = tritium concentration.

This model was applied using the primary loop specifications shown in Table X.4-4 with the result that a 1% slip stream would reduce the primary loop T_2O inventory to 1.1 grams. The primary loop T_2 inventory was estimated to be on the order of 0.04 g by assuming the oxygenation kinetics and mixing are sufficiently rapid that T_2 in the primary loop is fully reacted with oxygen after circulating five times through the loop. Although, by thermodynamics alone the equilibrium concentration of T_2 in the loop would be much smaller, the oxidation kinetics at the temperatures and Reynolds numbers of interest require further investigation.

If the T_2 inventory in the primary loop can be limited to 0.04 g, then permeability calculations indicate a 10,000 m² steam generator operated at 450°C would release about 50 Ci/day to the secondary coolant if an oxide coating can be preserved on the steam generator tubes (~7500 Ci/day without a coating). Therefore, it is concluded that if T_2 conversion to T_2O is effective and ~100 Ci/day releases are allowed, no intermediate loop (IHX) or double walled steam generator will be required. If the T_2 conversion is less effective, an IHX or double walled steam generator could control this release path.

Table X.4-4 Primary Loop Tritium Control Parameters

BLANKET TRITIUM

T₂ PARTIAL PRESSURE IN TUBES	20 torr
T₂ INVENTORY IN TUBES	~ 700 g

HELIUM PRIMARY LOOP

TRITIUM SOURCE (3000 MW_t)	520 g/DAY
HELIUM FLOW RATE (4000 MW_t, ΔT = 175°C)	4300 kg/s
VOUME FLOW RATE (50 ATM)	1270 m³/s
HELIUM LOOP VOLUME	1500 m³

PRIMARY LOOP CLEANUP

ASSUMED T₂ CONVERSION TO T₂O AND HTO	~ 100%
PRIMARY LOOP T₂ INVENTORY (5 PASSES)	0.04 g
MOLECULAR SIEVE SLIP STREAM	1%
MOLECULAR SIEVE INLET/OUTLET PRESSURES	0.006/0.001 Torr
PRIMARY LOOP T₂O INVENTORY	1.1 g
COST OF MOLECULAR SIEVES (ONLY)	\$200,000
MOLECULAR SIEVE OPERATING TEMPERATURE	40°C

Concerning releases from the reactor building, we have assumed that losses from the primary coolant loop to the building (in the absence of a direct cycle gas turbine) can be limited to ~ 1 full helium loop inventory per year. Therefore, about 1.1 g/yr or about 30 Ci/day would enter the building atmosphere. Typical CANDU experience indicates the expected loss rate of tritium from the building (in Ci/day) is less than 10^6 times the room concentration (in Ci/m³). Therefore, for a loss rate of < 10 Ci/day, the room concentration must be $< 10^{-5}$ Ci/m³. For a lower loss rate of < 1 Ci/day, the required room concentration is $< 10^{-6}$ Ci/m³.

The FED/INTOR work⁽⁶¹⁾ indicates the air dryer cost (in dollars/day) equals 1.6×10^{-3} times the air flow rate (in m³/day). Therefore, to recover 30 Ci/day at a concentration of 10^{-5} Ci/m³ (for < 10 Ci/day release) would require a process flow rate of 3×10^{-6} m³/day, or 4800 \$/day in cost. For a < 1 Ci/day release, the above cost becomes 48,000 \$/day. Although the later cost appears to be high, it is probably a conservative estimate because no economics of scale or reactor relevant payback periods (i.e., 30 years) were taken into account. Furthermore, if we assume that the 1 GW_e fusion reactor costs 1.5 times the cost of an LWR (currently about 2 billion dollars) and pays a capital cost of 10%, then the other plant capital costs alone would be 8.2×10^5 \$/day. This is 17 times higher than the cost of the air dryer system.

X.4.2.5 Beryllium Resource Issues

The helium cooled FLIBE blanket would contain about 200 m³ of FLIBE, or about 50 MT/GWe of beryllium. As discussed in section 5.2.3 of this report, the estimated U.S. beryllium resource is 73,000 MT. Therefore, each 1 GWe reactor inventory would require 0.07% of the overall resource. If we assume a 1% beryllium loss for each 4 year blanket lifetime and that 36,000 MT would ultimately be available for fusion reactors, then the domestic resource could support about 500 GWe of capacity for 200 years. This is enough to qualify the helium cooled FLIBE blanket for "intermediate range" service.

X.4.3 Conclusions

The helium cooled FLIBE blanket appears to be both feasible and attractive. Its advantages and disadvantages are listed below:

Advantages

- Simple mechanical design - only one fluid, nothing else flows, no drainage;
- Load following capability through all power levels;
- Swelling tolerant - long blanket lifetime;
- Potentially tolerant to tube failure during operation;
- Excellent breeding potential;
- Potentially low tritium inventory - all T_2 , no TF;
- Tritium isolation in separate tubes;
- Safe chemistry;

Issues (further work required)

- Tritium processing and control - loop design and chemistry;
- Wall loading limits (possibly as low as 3 MW/m^2).

Aside from the use of a FLIBE breeder, the concept of a non-purged breeder appears attractive for more detailed consideration. A substitution of lead-lithium for FLIBE in the current design could have merit since the design already utilizes a lead multiplier. A two zone Pb/Pb-Li blanket could have superior thermal performance and reduced compatibility concerns when compared with a one zone Pb-Li design because the film drop in the first row of tubes could be reduced.

X.5 Summary and Recommendations

In summary, some attractive alternate concepts which should be considered as candidate mainline concepts have been identified. The helium cooled FLIBE design and the HTS cooled solid and liquid breeder designs both show promise. But without further study, it is difficult to compare these alternate concepts on an equitable basis with the current mainline concepts. In order that a fair comparison can eventually be made, it is recommended these alternate concepts be studied further.

The HTS cooled blanket designs should be assigned the highest priority in future efforts. This coolant is unlike any of those currently being considered for the mainline concepts in that it offers a unique opportunity to operate at low pressure. The HTS cooled solid breeder blankets appear to have a wider range of operating temperatures than the HTS cooled liquid breeder blankets, and thus permits a less restricted choice of structural materials. Unless tritium recovery from solid breeders proves to be troublesome, the HTS cooled solid breeder designs will have advantages over liquid breeder designs, and should be given a higher priority for further study.

The helium cooled FLIBE breeder design is being recommended as an attractive alternative to other helium cooled liquid breeder designs. One potential advantage over the helium cooled ${}^{17}\text{Li}$ - ${}^{83}\text{Pb}$ design already in the mainline study is that the FLIBE breeder may permit a wider operating temperature window. Another potential advantage is that use of FLIBE could reduce spatial power peaking in the blanket, and thus relax thermal design constraints. To equitably compare this alternate concept with its counterpart already in the mainline study, it is recommended this blanket be studied further. Because it is an alternative to a class of blankets already being studied, this recommendation is made with a lower priority than that for the HTS cooled blankets.

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XI. SAFETY ANALYSIS

XI.1 Introduction

Fusion's ultimate potential as an energy source and its acceptance by society will depend, in part, on how well fusion development is successful in protecting the public and environment from potential harm. Just as blanket choices should decrease costs and enhance fusion's economic attractiveness, so should blanket choices decrease potential risk to the public and enhance fusion's safety and environmental attractiveness. An increase in safety and environmental attractiveness of designs at the current stage of fusion development will tend to increase public support and acceptance, decrease the need for expensive and complicated safety systems in the future, and increase the future general potential of fusion energy.

To date, safety efforts in the study have divided into three areas. First, since safety is one of the areas of comparison among blanket options, an appropriate methodology is needed to rank-order concepts (Section XI.2). Second, to support early decisions in the study, first stage safety comparisons have been done (Section XI.3). Third, specific safety analyses have been performed. Results not presented elsewhere have focused on molten salt issues (Section XI.4, Appendix E).

XI.2 Safety Evaluation Comparison Methodology

Concepts surviving the initial screening step in the Blanket Comparison and Selection Study will be compared in distinct areas--engineering feasibility, economics, safety, and R&D. This section contains the basic safety methodology including the basic method, potential reference accident/initiators, safety design goals/philosophy, and the safety evaluation indices. This methodology will eventually provide a numerical safety ranking of various blanket concepts.

XI.2.1 Basic Method

Before any concept is safety ranked-ordered, it must first pass the screening criteria. It is noted that several of these screening criteria have safety relevance. These are:

Maximum Tritium Blanket Inventory = 10 g/MW_{th} (40 kg for a 4000 MW_{th} reactor)

Maximum Operational Tritium Loss Rate = 100 Ci/day

Minimum Lifetime = 5 MW-yr/m² (low chance of failure within one year)

"Chemical compatibility" (coolant/breeder combinations of water/lithium, water/Li₇Pb₂, and HTS(heat transfer salt)/lithium are deemed not acceptable because of the potential likelihood of an aggressive chemical reaction releasing substantial energy within the blanket)

The comparative evaluation indices must be compatible with these values. In the context of the study, concepts that pass the screening criteria may then be rank-ordered. All subsequent criteria/indices are comparative, not defining absolute acceptability.

In the safety area, there are specific evaluation indices. Each concept will receive a score for each index (I_i) between 0.0 and 1.0. Because of the level of knowledge and restrictions on time, money and manpower in the study, individual indices are mixtures of quantitative and qualitative information. Ideally, a complete quantified risk assessment would be performed; however, this is not yet possible. As understanding improves through Fusion Safety Program tasks such as comparative risk assessment, some modification of these indices is expected.

Each index also has a weighting value (W_i) indicating its relative importance. An explanation of the specific weights is given in Section III.2.2.1. The sum of weighting values equals 100. An overall safety figure of merit (SFM) is defined as the weighted sum of index scores:

$$SFM = \sum_i I_i W_i \quad [XI.2-1]$$

Thus, the highest possible safety score would be 100. There are 11 indices.

One additional complication of the study is the requirement that both mirrors and tokamaks be considered. This issue appears to be more of a concern for the other evaluation areas--economics and engineering feasibility. At present, it is intended that the same set of safety evaluation indices and their weighting values will be used for both the mirror and tokamak versions of each blanket concept. Once specific versions are presented, it is possible they could warrant different scores for the same index because of either differences between the versions themselves or different potential interactions with the tokamak configuration versus the mirror configuration. If different scores do in fact occur, then two separate lists and rankings (tokamak, mirror) will be generated.

In preparing individual evaluation indices, several objectives were considered. These include the following:

- o Index must be consistent with screening criteria
- o Index should reflect a safety or environment relevant concern
- o Index should help form the basis for future comparisons
- o Index should provide good and clear guidance to designers
- o Index should allow a high degree of designer flexibility
- o Knowledge needed for the index should be available for analysis.

In addition, the complete set of indices should be consistent and sufficient to reflect as accurate and comprehensive a safety comparison as possible, given the state of knowledge. Certain objectives often conflict,

such as the desire for knowledge to be available for analysis conflicting with the desire for the comparison to be comprehensive; completeness requires including potential concerns where knowledge is not available.

XI.2.2 Potential Reference Accidents/Initiators

Based on current understanding of fusion reactors and early comparative risk assessment efforts, several specific blanket-relevant potential accidents appear to be significant contributors to total accident risk, hence important enough for consideration in design. The design goals and evaluation indices presented later include these concerns. To help understand the basis for the indices, the following list of potential reference accidents is presented:

- o Loss of site power (includes turbine trip and loss of flow accident)
- o Loss of primary coolant, large break into building (substantial loss of cooling to large fraction of blanket)
- o Loss of primary coolant, break into vacuum chamber
- o Loss of primary coolant, break into breeder (or multiplier) zone
- o Loss of vacuum integrity
- o Loss of limiter (or choke coil) integrity
- o Loss of breeder system integrity
- o Plasma disruptions
- o Seismic events
- o Off-normal magnet behavior (forces, missiles, etc.).

Only those accidents relevant to blanket concept choice will be considered in this study. Note, lithium fire concerns are included under accidents such as loss of primary coolant and loss of breeder system integrity.

XI.2.3 Safety Design Goals/Philosophy

The primary purpose of fusion safety and environmental efforts is the protection of the public from harm. The second and third purposes are the

protection of fusion plant workers and the protection of plant equipment. Fusion's ultimate potential as an energy source and its acceptance by society will depend, in part, on how well fusion technology development meets these challenges. The following list of safety design goals represents appropriate attempts to fulfill these purposes. These goals and associated concerns have been incorporated into the safety evaluation indices to favor those blanket concepts that show the most promise in meeting these challenges. Concepts that are more successful in meeting these goals will be scored higher in the comparison via the indices.

The goals are divided into three categories (as are the indices)--source term characterization, fault tolerance, and non-accident concerns. If elemental sodium or potassium are used, all following statements concerning lithium also apply to sodium or potassium.

XI.2.3.1 Source Term Characterization

Source term goals can be expressed very simply:

1. Concept design should minimize radioactivity (and chemical toxicity) and its vulnerability. This includes all blanket materials, e.g. coolant, breeder, neutron multiplier, and structure.

XI.2.3.2 Fault Tolerance

Unfortunately, fault tolerance cannot be so easily expressed. The guiding philosophy is that designs should tolerate potential faults (accident initiators). An intrinsically tolerant design is preferable to one requiring an active protection system. Based on the potential accident initiators, the following design goals for fault tolerance were prepared:

2. First wall and blanket should be capable of withstanding complete severing of a coolant tube within the blanket module without
(a) propagation of failure to other components, modules, or

structures or (b) loss of radioactivity from the module and primary coolant system in excess of that normally entrained in blanket fluids. No new radioactivity should be mobilized. This includes possible coolant-breeder chemical reactions and containment of coolant pressure. For designs with a large number and long lengths of tubes, there is significant concern that the event rate for blanket coolant tube break may be about once per year.

3. First wall and blanket should be capable of withstanding complete loss of site power (station blackout) for four hours without failure of components or structures. The expected event rate is 0.12/yr, with power recovery occurring within four hours for 77% of these events.⁽¹⁾ A concept requiring auxiliary power generators is less satisfactory than one that does not need backup power.
4. Blanket modules should be capable of withstanding a major loss of coolant accident without (a) significant structural failure or (b) loss of radioactivity from the module and primary coolant system in excess of that normally entrained in blanket fluids. If this cannot be met, the probability of a loss of coolant should be below $\sim 10^{-4}$ /yr.
5. Blanket structures should be capable of withstanding reasonable external forces without (a) significant structural failure or (b) loss of radioactivity from the module and primary coolant system. Such forces include plasma disruptions, seismic events, and off-normal magnet interaction with the blanket. Disruption and magnet failure rates are unknown. There is a need to better define a worst case plasma disruption (thermal and magnetic effects) and a worst case uncontrolled magnet quench and resulting effects. For present purposes, an appropriate seismic criterion is that included in the STARFIRE⁽²⁾ siting discussions.

6. Lithium-water reactions in or near the blanket should be eliminated by design. For liquid lithium modules, this eliminates the option of water-cooled blanket modules. Water-cooled shields, limiters, divertors, or choke coils may be acceptable with liquid lithium modules, provided that the probability of substantial lithium-water reaction is $\sim 10^{-4}$ per year or lower.
7. First wall and blanket should withstand failures in nearby components. These include limiter/divertors, shields, and loss of vacuum integrity. This prevents failure propagation.
8. Concept design should prevent severe accidental pressure sources compromising reactor building containment integrity. Such accidents include loss of primary coolant (e.g., water overpressure) and rupture of magnet helium integrity. Thus, design penalties to the building caused by blanket choices should be considered.
9. Concept design should prevent concrete reactions that could compromise reactor building containment integrity, e.g., lithium-concrete reactions. If this cannot be met, the probability of such reactions compromising containment should be below $\sim 10^{-3}$ /accident transient. Again, design penalties to the containment caused by blanket choices should be considered.
10. Concepts should be designed to prevent fires from chemical spills, e.g., lithium-air reactions.

XI.2.3.3 Non-Accident Concerns

There is potential risk to the public and plant workers from issues other than accidents. These issues include normal operational tritium losses, occupational worker radiation exposure, and material waste disposal. The following design goals address these concerns:

11. Concept design should minimize normal tritium losses to the environment. Concepts requiring fewer tritium control barriers/systems are preferable because of the higher assurance of success and higher flexibility in meeting possible future standards.
12. Work force radiation exposure should be minimized through increased component/blanket reliability (fewer maintenance operations needed), increased blanket simplicity (easier and faster maintenance), lower radiation fields, increased use of remote maintenance techniques, and lower mobility of radioactive and toxic chemical contaminants.
13. Radioactive waste generated from normal operation, replacement of blanket components, and decommissioning should be minimized. This includes waste from maintenance and filter change-outs. The volume, volatility, and biological hazard of waste are relevant.

Several evaluation indices use the concepts of vulnerability or fault tolerance. Both include failure rate probabilities. The Fusion Safety Program data on failure rates are being established from sources such as the NREP, CRBR, HTGR, and IEEE studies. These data will be used when applicable in evaluating designs.

XI.2.4 Safety Evaluation Indices

The safety evaluation indices fall into three categories, source term characterization, fault tolerance, and non-accident concerns. Several indices are built in whole or in part on the Relative Consequence Indices (RCI's) of Ref. 3. The numerical values presented for radioactivity inventories in the indices themselves are appropriate for a STARFIRE size reactor.

As written, the indices include issues relevant for comparison of multiple structural materials, specifically, steels and vanadium alloys.

Inclusion of radically different materials, e.g. very low activation SiC, would require several changes.

It is emphasized that the accident-related indices, 1 to 8, incorporate consequence and probability. The first category of indices, 1 to 3, relate to the risk of the radioactive source term common to all accident initiators. These three source term characterization indices include both the radioactive inventory, including tritium, and its vulnerability. Ideally, possible release fractions to the environment would be calculated including initial mobilization of the radioactivity in the blanket and transport from the blanket area to the outside environment. At present, insufficient data prohibits this approach. Rather, the radioactive inventory will be evaluated as indicated in the indices. Transport within the building will be considered as part of vulnerability.

The second category of indices, 4 to 8, relate to the risk of specific accident initiators. A concept can score well by showing (a) that the consequences indicated in the relevant design goals cannot happen or are of low severity due to the concept design, or (b) that the probability of the accident occurring is sufficiently low, as indicated in the design goals. "Design" includes passive and active measures, although the potential unreliability of active protective systems would be considered. Since the indices are comparative, the concepts which best meet the concerns in each fault tolerant index will get the best score of 1.0.

XI.2.4.1 Source Term Characterization (W = weighting value)

1. Structure Source Term Characterization

W = 10

This index breaks into two equal components. The first component is the total relative public health effects that would result from accidental release. Since potential amounts of structure that are releasible appear to fall below the threshold for acute effects and above that for latent effects for average site and weather conditions,⁽³⁾ the measure is limited to total latent health effects,

RCI_{RC}^{te} .^a Among materials in this study, the vanadium alloy is best and 316 SS/PCA is worst. Latent effects scale linearly.

Thus,

$$\text{Relative health effects score} = 1.01 - \frac{1}{100} RCI_{RC}^{te} \quad [XI.2-2]$$

and

$$\text{Relative health effect score (V-15Cr-5Ti)} = 1.00 \quad [XI.2-3]$$

$$\text{Relative health effect score (316 SS)} = 0.01 \quad [XI.2-4]$$

The second component, vulnerability, includes the measures of (a) the temperature margin between the operating temperature and the threshold temperature for rapid oxidation (RCI_{SO}^T = relative temperature margin),^b (b) oxide volatilization rate (RCI_{SO}^V = relative volatilization rate), (c) oxidation rate (RCI_{SO}^{ox} = relative oxidation rate) and (d) relative transportability. The total vulnerability score ranges from 0.0 to 1.0.

2. Breeder Source Term Characterization

W = 10

There are three components to the breeder source term characterization index. For purpose of these indices, the breeder is considered to include the neutron multiplier (if any). The first relevant component is the amount (in grams) of tritium that could be released in an accident. This includes "bake-out" scenarios due to loss of cooling. The

a. RCI_{y}^{x} = Relative Consequence Index, x = measure of hazard,
y = problem area; here, x = te = total health effects,
y = RC = radioactivity consequences (from Ref. 3).

b. x = T, v, ox, defined in text; y = SO = structural oxidation problems.

graph in Fig. XI.2-1 shows the score as a function of vulnerable grams of tritium. The utility function in the graph does not account for the presence of a containment building. Reduction of releases due to a containment building will have the effect of shifting the utility curve to the right; that is, raising scores for a given inventory lost from the blanket. It is hoped to further study this reduction in the second year of the study. At the present time, the beneficial effect of a containment building cannot be quantified and is handled qualitatively in the third component, vulnerability, of this index.

Figure XI.2-1. Utility Function for Indices 2, 3, and 9

Based on references 2-5, it appears that tritium released in amounts of ~ 10 -100 kg could cause acute fatalities or injuries. Thus, these amounts are scored near zero. For example, using the data from the STARFIRE report⁽²⁾, a building level release of ~ 2 kg could cause an acute fatality. It is interesting to note that the limit on the diffusion-related inventory (somewhat vulnerable) used in the STARFIRE study was $1 \text{ kg/GW}_{\text{th}}$ ($= 4 \text{ kg/reactor}$). Below levels of ~ 10 -100 kg,

latent effects tend to dominate and a linear scaling is appropriate. Around the lower bound of 10-100 g, a lower releasible blanket inventory does little additional good since other vulnerable components, e.g. cryopumps, have such inventories; hence, the score for this measure approaches the upper limit of 1.0.

The second relevant component is the non-tritium radioactivity in the breeder and multiplier. If possible, this will be replaced in the future with a health effect calculation using the FUSECRAC^(3,6) code. Then, this component could be very similar to the structural health effects component in index 1. For now, the graph in Fig. XI.2-1 is used to convert radioactivity to the score. Based on Ref. 3, for the isotope mixes that are relevant in this study, releases of $10^7 - 10^8$ Ci would cause acute fatalities (average site and weather conditions). Thus, these amounts are scored near zero. Below this level, latent effects would occur; thus, a linear scoring is appropriate. Around $10^4 - 10^5$ Ci, higher scores are no longer appropriate because latent effects would approach zero and the lowest activities of most concepts fall into this range (see Ref. 3 for examples). Only isotopes with half lives greater than 15 min are included; shorter lived isotopes would substantially decay before exposure of the public following an accident.

The third component, vulnerability, consists of the measures of pressure, probability of loss of breeder system integrity, chemical reactions with air, chance for recovery, building transport, chemical form of released tritium (e.g., T_2O or T_2), and potential for mobilization.

3. Coolant Source Term Characterization

W = 10

There are three components of the coolant source term characterization index. The first component is the amount (in grams) of tritium that could be released in an accident. The graph in Fig. XI.2-1 translates the amount in grams to the score for this component.

The second component is the radioactivity (non-tritium) in the primary coolant system. This includes coolant activation products and mobile/releasible activated corrosion products. Fig. XI.2-1 translates curies to the score. If possible, this will be replaced with public health effect calculations.

The third component, vulnerability, consists of the measures of mobility of corrosion and activation products, pressure, chemical reaction potential with air, chance for recovery, chemical form of released tritium, and building transport.

XI.2.4.2 Fault Tolerance

4. Fault Tolerance to Breeder-Coolant Mixing W = 6

Relevant measures include the potential for fault propagation, mitigation potential, chance for recovery, maximum temperatures, maximum pressures, and chance for breeder-coolant mixing (i.e., probability of a LOCA internal to a module). See design goal #2 for more details.

5. Fault Tolerance to LOCA, LOFA, Loss of Site Power (LOSP) W = 6

Relevant measures include the allowable time to action before damage or radioactivity release, mitigation schemes, afterheat levels and the probability of a major LOCA (i.e., the probability of a LOCA external to the blanket). See design goals 3 and 4 for more details.

6. Fault Tolerance to External Forces W = 6

Relevant measures include response/damage potential from plasma disruptions, off-normal magnet interactions and seismic events. See design goal 5 for more details.

7. Fault Tolerance to Near-Blanket System Interactions W = 6

Relevant measures include response to loss of vacuum integrity, probability of LOCA into the vacuum chamber, blanket-shield interactions, blanket-limiter or divertor interactions, chance for recovery, and chemical reactions possible within the vacuum chamber. See design goals 6 and 7 for more details.

8. Fault Tolerance of Containment Integrity W = 6

Relevant measures include potential for chemical spills/fires, building overpressure, and mitigation schemes. See design goals 8, 9, and 10 for more details.

XI.2.4.3 Non-accident Concerns

9. Radioactivity Emission Rate W = 20

The relevant measure is the normal tritium loss rate in curies per day. Fig. XI.2-1 translates this value into the index score. Based on Ref. 7, it is highly desirable to keep loss rates below ~ 10 Ci/day. Thus, values above ~ 10 Ci/day are scored near zero. Since tritium's effect on man and environment is linear with the amount released in this range, the index score scales linearly below ~ 10 Ci/day. At levels below ~ 0.1 Ci/day, there is very high assurance of meeting relevant standards, thus these values receive an index score near 1.0. In the event that significant non-tritium radioactivity would be released due to normal operation, it would be included in this index. See design goal 11.

10. Occupational Exposure W = 10

Relevant measures include time required for maintenance, activity/dose levels near serviced components, module lifetime, total tritium inventory, degree of remote maintenance, and amounts of operationally generated wastes. See design goal 12.

Relevant measures include radiation exposure from waste processing, waste class per 10 CFR 61, integral biological hazard potential (IBHP), total tritium inventory, and total radioactivity produced (integrated over reactor lifetime). See design goal 13.

XI.2.5 Summary

A structured methodology that includes a wide range of significant human safety concerns exists to compare blanket concepts. This methodology will allow a numerical safety rank-ordering of concepts in the Blanket Comparison and Selection Study.

The indices are summarized in Table XI.2-1. The direct component of risk from mobile radioactivity (indices 1 to 3) is responsible for 30% of the total figure of merit while the component to risk from the response of a design to specific transients (fault tolerance, indices 4 to 8) has another 30%. Non-accident concerns (indices 9 to 11) account for 40% of the total ranking score.

XI.3 First Stage Comparisons

Although the various blanket concepts are continuing to evolve, a first stage comparison of the concepts was performed to aid initial concept selections, focus further work, and test the methodology. Information is not yet sufficient in this study to directly compare all concepts. Rather, examinations and comparisons have been limited to three narrower areas--liquid breeder concepts, solid breeder concepts, and neutron multipliers.

XI.3.1 Liquid Breeder Concept Comparison

A first stage safety comparison of the various coolant/liquid breeder options was performed; this first stage effort did not consider structural material options integrated with coolant/breeder choices. Thus, index 1

TABLE XI.2-1 SUMMARY OF SAFETY EVALUATION INDICES

Index Name	Weighting Value
1. Structure Source Term Characterization	10
2. Breeder Source Term Characterization ^a	10
3. Coolant Source Term Characterization	10
4. Fault Tolerance to Breeder-Coolant Mixing	6
5. Fault Tolerance to LOCA, LOFA, and LOSP ^b	6
6. Fault Tolerance to External Forces ^c	6
7. Fault Tolerance to Near-Blanket System Interactions ^d	6
8. Fault Tolerance of Containment Integrity	6
9. Radioactivity Emission Rate	20
10. Occupational Exposure	10
11. Waste Disposal	10

a. Breeder index includes neutron multiplier, when applicable.

b. Loss of coolant, loss of flow, loss of site power.

c. E.g. plasma disruptions, seismic events, and off-normal magnet behavior.

d. E.g. blanket-shield interaction, blanket-limiter or choke coil interactions, and response to loss of vacuum integrity.

(structural source term characterization) is not yet relevant. Waste characterization of the liquid breeders has not yet been performed so that no discussion of index 11 is included. For the remaining 9 indices, a relative comparison of the various mainline liquid breeders was performed. The relative standing of each concept for each index is indicated in Table XI.3-1. Future evaluations will consist of numerical scores (0.0 to 1.0) for each concept and index, but the limited information currently

TABLE XI.3-1. FIRST STAGE LIQUID BREEDER SAFETY COMPARISON

		Breeder:					
		Li	17Li-83Pb	17Li-83Pb	Li	17Li-83Pb	17Li-83Pb
		Coolant:			He	He	H ₂ O
Index Number	Weight Value	Index ^a Name	Relative Standing				
2	10	Breeder STC ^b	-		-		
3	10	Coolant STC	-	-	+	+	
4	6	FT ^c Breeder-Coolant Mix	+	+			-
5	6	FT LOCA, LOFA, LOSP		+			
6	6	FT External Forces		-	-	+	
7	6	FT Near-Blanket	-		-		
8	6	FT Containment	-		-		-
9	20	Radio-activity Emissions			+	+	-
10	10	Occupational Exposure		-	-	+	+
		Relative Rank: ^d	4	3	5/6	2	1
							5/6

- a. No zero order difference for indices not listed, same structural metal assumed for all concepts.
- b. STC = source term characterization.
- c. FT = fault tolerance
- d. Relative rank among liquid breeder concepts, 1 = best, 5/6 = tied for worst.

available did not warrant that level of comparison--rather, a plus/minus relative standing is indicated. It is emphasized that future numerical scores could change the rankings presented.

In the breeder source term characterization (index 2), lithium has two disadvantages relative to ^{17}Li -83Pb, i.e., higher tritium inventory and higher chemical reaction potential. For example, the current estimate of the tritium inventory for the self-cooled Li concept is 400 g compared to 80 g for the ^{17}Li -83Pb concept. ^{17}Li -83Pb has one disadvantage, the activity induced in the lead and resulting from corrosion products. Recent EG&G Idaho results indicate activities on the order of 10^5 Ci of ^{204}Tl , 10^8 Ci of ^{203}Pb , and 10^6 Ci of ^{203}Hg and ^{205}Hg for a 20 MW-yr/m² wall loading of a 4000 MW size machine. Overall at present, lithium is relatively less attractive for this index.

For coolant source term characterization (index 3), the same issues apply, and lithium is relatively less attractive than ^{17}Li -83Pb. Sodium activates to very high levels (section XI.4.2) and, like lithium, has a high chemical reaction potential. Thus sodium is scored less attractive than ^{17}Li -83Pb. Helium has significant advantages of very low tritium inventory and low radioactivity. Water has somewhat offsetting characteristics: less radioactivity than ^{17}Li -83Pb but worse tritium problems.

The breeder-coolant mixing problem (index 4) translates to coolant tube ruptures/leakage for $\text{H}_2\text{O}/^{17}\text{Li}$ -83Pb and $\text{Na}/^{17}\text{Li}$ -83Pb and breeder tube in-leakage for the helium cases. Because the self-cooled Li and ^{17}Li -83Pb cases totally avoid this problem, they were scored a relative plus. Recent analysis⁽⁸⁾ suggests that a rupture of a water tube is likely to lead to module rupture for certain geometries whereas this is not expected of sodium or helium cooled designs.

For loss of coolant, flow, or power transients (index 5), the $\text{Na}/^{17}\text{Li}$ -83Pb case appears significantly better because of the presence of two liquid metals; both are good heat sinks and likely to remain single phase. Further transient analysis for the final blanket concepts is needed.

The primary issues concerning fault tolerance to external forces (plasma disruptions, seismic events, off-normal magnet behavior; index 6) are the density and MHD sensitivity of the various fluids. Lithium has MHD concerns but represents less mass in the blanket. ^{17}Li - ^{83}Pb has MHD concerns similar to lithium but is 20 times more dense, a net disadvantage. Helium and water are MHD insensitive. Thus He/Li is scored higher than Li/Li. $\text{H}_2\text{O}/^{17}\text{Li}$ - ^{83}Pb and He/ ^{17}Li - ^{83}Pb appear roughly equal to Li/Li (water and helium better than lithium, ^{17}Li - ^{83}Pb worse than lithium).

The key relative concern present under index 7, near-blanket interactions, is the likely presence of water in nearby components such as limiters or divertors (tokamak), choke coil (mirror), and shields. Unfortunately, it is not yet possible to determine the probability of interaction between such water and lithium or sodium in a blanket. However, since the interaction is mechanically conceivable and would result in relatively severe consequences, blankets containing elemental lithium and sodium were marked a relative disadvantage.

The two prime issues for containment fault tolerance to blanket materials (index 8) are lithium or sodium-concrete reactions and water pressurization of the building. Although helium and liquid metal coolants are also under pressure, only water (≥ 15 MPa pressure) represents a pressure source worse than cryogenic helium from a magnet cryostat rupture.⁽³⁾ Blankets with high pressure water or chemically reactive lithium or sodium were marked a relative disadvantage. As the future comparison effort in the study focuses on fewer concepts and interplay between safety and economics is considered, a consistent treatment of the costs of containment (steel liners over concrete for lithium and sodium and high design overpressure for water) versus the safety concerns is needed.

For the materials considered here, the primary radioactive emission (index 9) is likely to be tritium. The Li/Li, ^{17}Li - $^{83}\text{Pb}/^{17}\text{Li}$ - ^{83}Pb , and Na/ ^{17}Li - ^{83}Pb systems currently call for intermediate loops that require their own tritium systems to lower tritium loss to the steam cycle. Helium has a potential advantage since the tritium in helium could be converted to

oxide form, unlike the case in liquid metals, and thus permeation losses are likely to be low.⁽³⁾ The actual kinetics of converting HT/T_2 to HTO/T_2O in a primary helium coolant are not well understood and may cause increased tritium losses; further study is planned. Further work on normal leakage of tritiated helium coolant is needed to determine if it is a significant release pathway. Permeation from a water loop, like helium, is likely to be very low. However, the estimated attainable equilibrium HTO concentration coupled with expected leakage across the steam generator is likely to result in relatively high losses, e.g., 10 Ci/day for STARFIRE. Because implantation from the plasma may increase the amount of tritium entering water coolant above that considered in STARFIRE, water coolant may not meet the 100 Ci/day screening criterion; additional work is planned to examine this.

To date, little work has been done in the area of occupational exposure (index 10). Helium operation is likely to result in substantially lower exposure while the heavily activated liquid metals, ^{17}Li - ^{83}Pb and sodium, would appear to be substantially worse.

Ignoring structural material variation, first stage safety comparison of liquid metal breeder options indicate that helium-cooled designs are most attractive with $He/^{17}Li$ - ^{83}Pb slightly more attractive than He/Li . Next come the self-cooled designs, and ^{17}Li - $^{83}Pb/^{17}Li$ - ^{83}Pb is slightly more attractive than Li/Li . Last are $Na/^{17}Li$ - ^{83}Pb and $H_2O/^{17}Li$ - ^{83}Pb . The high activation of sodium versus lithium and their generally similar chemical reactivity problems lead to the conclusion that lithium is strongly preferred over sodium. The $H_2O/^{17}Li$ - ^{83}Pb concept may have severe difficulties, specifically water pressure/coolant tube break and tritium control.

Second-year efforts in the study will focus on more quantification in the comparison for those concepts which survive first level selection. Also, the structural material choice will be added to the comparison efforts.

XI.3.2 Solid Breeder Concept Comparison

As for the liquid breeder comparison, the first stage effort did not consider structural material options integrated with breeder/coolant choices, and index 1 was not evaluated. The first stage comparison is summarized in Table XI.3-2.

To date, there have not been consistent tritium inventory and transient tritium release calculations in the study for all the various solid breeder candidates. The one remaining relative difference at this time among solid breeder concepts is the possibility that $\text{He/Li}_2\text{O}$ and $\text{H}_2\text{O/Li}_2\text{O}$ would not require a neutron multiplier. Assuming all other solid breeder concepts include a multiplier with its associated chemical or radiological hazards (section XI.3.3), $\text{He/Li}_2\text{O}$ and $\text{H}_2\text{O/Li}_2\text{O}$ have a relative advantage for the breeder/multiplier source term (index 2). If other concepts do not require a multiplier, they would also exhibit a relative advantage.

For coolant source term characterization (index 3), helium designs have an advantage over water, as for liquid breeder designs.

The current reference cases for the water designs are use of the STARFIRE geometry for $\text{H}_2\text{O/Li}_2\text{O}$ and a pod geometry for $\text{H}_2\text{O/ternary ceramics}$. The pod geometry consists of a module designed to withstand accidental pressurization by a water intrusion. The pod is filled with lead multiplier with separate breeder and coolant tubes immersed in the lead bath. Of solid breeder concepts, the STARFIRE geometry water design is far less satisfactory for fault tolerance to coolant-breeder interaction (index 4).

Insufficient analysis has been completed to differentiate among the solid breeder concepts for fault tolerances to LOCA, LOFA and LOSP (index 5). Helium can be pumped after depressurization while water pumps cavitate.

TABLE XI.3-2. FIRST STAGE SOLID BREEDER SAFETY COMPARISON

		Breeder:	Li ₂ O	Ternary	Li ₂ O	Ternary
		Multiplier:	--	Pb	--	Be or Pb
		Coolant:	H ₂ O	H ₂ O	He	He
		Geometry:	STARFIRE	Pb bath/pod	Pod	Pod
Index Number	Weight Value	Index ^a Name	Relative Standing			
2	10	Breeder STC ^b	+		+	
3	10	Coolant STC			+	+
4	6	FT ^c Breeder- Coolant Mix	-			
8	6	FT Containment	-	-		
9	20	Radioactivity Emission	-	-	+	+
10	10	Occupational Exposure			+	+
11	10	Waste	+		+	
		Relative Rank: ^d	3	4	1	2

- a. No zero order difference for indices not listed, same structural metal assumed for all concepts.
- b. STC = source term characterization.
- c. FT = fault tolerance.
- d. Relative rank among solid breeder concepts, 1 = best, 4 = worst.

At present, there are no clear indications that any of the solid breeder concepts have a clear advantage or disadvantage in the areas of fault tolerance to external forces (index 6) and near-blanket system interactions (index 7).

As for the liquid breeder designs, water-cooled designs have a disadvantage in the area of containment building tolerance to blanket choices (index 8). Also, again helium has an advantage and water a disadvantage in terms of tritium control (index 9) and occupational exposure (index 10).

Some initial waste analysis (index 11) has been performed for the solid breeders, independent of coolant. At present, Li_2O appears to be easier to recycle, having minimal reprocessing requirements. The chemical composition of the ternary oxides will change during use because of lithium burnup, necessitating chemical reprocessing to reuse. Current 10 CFR 61 regulations applied to the solid breeders indicate that all are likely to be suitable for shallow land burial, i.e., low level waste. When the 10 CFR 61 methodology is extended to isotopes not covered in the existing regulation, some breeders like Li_8ZrO_6 may require deep geological burial; however, preliminary analysis indicates that even Li_8ZrO_6 would be suitable for shallow land burial.

The above solid breeder waste results can be compared with initial structural material examinations. Based on 10 CFR 61, PCA steel would require deep geological burial whereas V-15Cr-5Ti appears to qualify for shallow land burial. Tenelon, a high manganese, low nickel austenitic stainless steel, may qualify for shallow land burial. HT-9 examination is planned.

Overall, helium-cooled solid breeder designs appear more attractive from the safety standpoint than water-cooled designs, ignoring the effect of structural material choice. Helium inherently runs at lower pressure, is chemically inert, and has easier tritium control. Second-year analysis will focus on more quantification for surviving concepts and integration of structural material comparison.

XI.3.3 Neutron Multiplier Comparison

Initial comparison of the two primary neutron multiplier candidates, beryllium and lead, was suggested and performed to search for reasons to

choose between them. A detailed comparison would require specific designs. Also, any further comparison between multiplier materials would likely be strongly coupled to other blanket material choices.

Even though lead activates, it is 1-100 times less toxic than beryllium per mass based on air exposure. Any additional activation due to impurities in lead has not been considered. The limits on beryllium air concentration are $2 \mu\text{g}/\text{m}^3_{\text{air}}$ occupational exposure for an 8 hour work day and $10 \text{ ng}/\text{m}^3_{\text{air}}$ for public exposure.⁽⁹⁾ A limit on unactivated lead air concentration is $200 \mu\text{g}/\text{m}^3_{\text{air}}$ for a 40 hour work week.⁽¹⁰⁾ Lead radiological maximum permissible concentrations per 10 CFR 20⁽¹¹⁾ are $30 \mu\text{g}/\text{m}^3_{\text{air}}$ (workers) and $1 \mu\text{g}/\text{m}^3_{\text{air}}$ (public), dominated by ^{203}Pb for lead activity levels averaged over the blanket, in the form of $^{17}\text{Li}-^{83}\text{Pb}$. Lead radiological limits for lead multiplier activity levels would be about an order of magnitude stricter. Given the uncertainties and difficulties in comparing chemical limits and radiological limits, the best estimate is that per mass beryllium is 1-100 times more toxic than activated lead.

In terms of loss of flow and loss of coolant transients, beryllium at 70% theoretical density has over twice the volumetric heat capacity and close to three times the thermal conductivity of lead. Thus beryllium may have an advantage as a better heat sink. Neither activated pure lead nor beryllium appear to be major contributors to afterheat. Note, although the multiplier is a small fraction of the blanket, it is importantly located next to the highest afterheat source, the first wall, thus the multiplier may be relatively more important in thermal transients.

Advantages for responses to external forces are mixed. Beryllium is about an order of magnitude less dense and more electrically conductive; thus, seismic events may be worse for lead and MHD events worse for beryllium.

Since either the beryllium or lead as multipliers would tend to be encapsulated in the blanket, effects on normal emissions of operational occupational exposure should be minimal. Although not directly considered in the safety indices, beryllium toxicity is a concern for initial fabrication and construction related occupational exposure.

In the area of waste disposal, it is highly likely that beryllium would be reprocessed and recycled, resulting in exposure. Further, chemical toxicity does not decay. It is unclear how lead would be handled, but it appears to represent a lesser waste/recycle hazard.

In summary, although differences do exist between beryllium and lead, they appear to be small compared with coolant, breeder, and structure choices. The safety preference between them is not straightforward and likely to be determined by the specifics of the actual design. In general, one should avoid a multiplier if possible since its inclusion increases complexity and adds to chemical or radiological toxicity. The lead-bath pod design for the H_2O /ternary ceramic concept is an exception. Using the lead multiplier in that way appears to have made it a net safety advantage over the previous STARFIRE concept.

XI.4 Molten Salt Safety Studies

Past studies have examined the possibility of using molten salts as primary coolants and/or tritium breeders in fusion reactors. A recent study found that salts may offer distinct safety advantages compared with other coolants.⁽³⁾ Two analyses have been performed in the area of molten salt safety issues. First, an early examination of possible molten salts was conducted with a safety focus. Second, after initial efforts in the study focused on heat transfer salt, HTS, the possible emission control problems for HTS were examined. A summary of these efforts follows. More details are given in Appendix E.

XI.4.1 Safety Survey of Molten Salt Candidates

Previously, molten salts have been considered only as primary blanket coolant or blanket tritium breeder. However, other applications exist, including limiter/divertor coolant, shield coolant, and secondary loop coolant. This effort has three general stages. First, the desired material properties of molten salts for fusion applications are examined. Second, the exceedingly large number of possible salts is narrowed to a limited number of likely options. Third, a general focus for future research is indicated. Details are given in Section E.2. The most attractive molten salts resulting from the survey are listed in Table XI.4.1.

The more attractive fluoride salts are LiF-BeF_2 and NaF-NaBF_4 . Flibe, 34-66 composition LiF-BeF_2 , has been mentioned in previous fusion studies. It would be an attractive primary coolant if additives could be found to lower its high melting point, 459°C , and its high viscosity. NaF-NaBF_4 may be a suitable secondary loop coolant for those systems that require one.

The more attractive chloride salts are LiCl-BeCl_2 and LiCl-AlCl_3 . Very little is known of these materials and they may be too corrosive or too unstable for fusion use. Of the two, LiCl-BeCl_2 is more stable, with a higher melting point; thus it is the preferred blanket coolant. LiCl-AlCl_3 has a low melting point ($\sim 103^\circ$ at 40% LiCl) so that it may have potential for limiter, shield, or divertor use if it is found to be sufficiently stable.

The best nitrate/nitrite type of salt is $\text{NaNO}_3\text{-NaNO}_2\text{-KNO}_3$, e.g. HTS. This salt may be used in several applications. Additives may be necessary to lower its melting point, 140°C , in order for it to be used as a limiter, shield, or divertor. The primary concerns with this material are its limited stability and high levels of induced radioactivity. One resulting concern, routine radioactivity emissions, is discussed in the next section.

TABLE XI.4-1. MOST ATTRACTIVE MOLTEN SALTS FROM EACH CLASS FOR VARIOUS FUSION APPLICATIONS

	Fluoride Salt	Chloride Salt	Nitrate/ Nitrite Salt
Primary coolant	LiF-BeF ₂ (with additives)	LiCl-BeCl ₂ (with additives)	NaNO ₃ -NaNO ₂ -KNO ₃
Limiter, divertor, Shield coolant	None	LiCl-AlCl ₃	NaNO ₃ -NaNO ₂ -KNO ₃ (with additives)
Secondary coolant	NaF-NaBF ₄	None	NaNO ₃ -NaNO ₂ -KNO ₃

XI.4.2 Emission Control for HTS

The relatively high levels of radioactivity that would be produced in HTS (7% NaNO₃-49% NaNO₂-44% KNO₃) used in a fusion blanket would result in several concerns: potential for release in accident situations, normal operational emissions, maintenance radiation exposure, and waste disposal. As part of the safety input to the study, the potential feasibility and costs for control of normal operational emissions have been briefly examined. Details are given in Section E.3.

The basic results are shown in Table XI.4-2. The major isotopes of concern are ¹⁴C, ²²Na, and ³⁹Ar. If all of the ¹⁴C and ²²Na that is produced is normally released to the environment, site boundary public dose would be too high. The amount of these isotopes released must be reduced with respect to the amount produced by factors of at least 90 and 2,000 respectively. Some of the required reduction may well occur naturally--the ¹⁴C and ²²Na may stay chemically bound in the salt and not be routinely lost.

The case of ³⁹Ar is similar except that it is a noble gas and will not be chemically bound in the salt. The amount released would have to be at most a few percent of that produced in order to keep public doses to sufficiently low levels.

TABLE XI.4-2. SUMMARY OF MAJOR CONCERNS FOR HTS EMISSIONS

Isotope	Required Reduction ^a Factor	Potential ^b Cost (\$/yr)	Comments
¹⁴ C	~90	2 x 10 ⁸	Actual costs likely to be far lower due to (a) most ¹⁴ C staying in salt, (b) better scaling than linear for ¹⁴ C treatment rate, (c) chemical form may be easier to collect than CO ₂
²² Na	~2 x 10 ³	Not estimated	²² Na will follow normal Na in processing. Actual releases would be caused by specifics of yet unspecified HTS processing
³⁹ Ar	~40	10 ⁵ -10 ⁶	All ³⁹ Ar likely to leave the salt (unlike ¹⁴ C, ²² Na) so this could actually represent the worst case isotope.

a. Required Reduction Factor defined as yearly dose from instantaneous release of all isotope produced divided by 10 mrem/yr (whole body) or 30 mrem/yr (skin).

b. Based on linear scaling of fission control technology. Since isotopes other than noble gases will not all leave the salt, non-argon costs are likely to be over-estimates. Costs given for capture, retention and storage. See Appendix E.

The basic conclusion is that adequate control of radioactive emissions from HTS appears feasible, although specific control technology would have to be developed and demonstrated. Further, the possible costs for control systems could be significant. For purposes of the study, the following recommendations are made: (a) adequate emission control be judged potentially feasible, and thus HTS passes the spirit of the 100 Ci/day (of ³H) screening criterion, (b) further data be collected/developed on the

expected chemical processing of HTS to better define the source term, and (c) given such refinement of the source term, appropriate scores should be assigned for relevant economic and safety evaluation indices.

XI.5. Summary

A safety evaluation methodology now exists to compare and rank order blanket concepts. Although not as detailed and complete as a full probability risk assessment, it is believed that this methodology allows all significant aspects of the comparison to be considered. The biggest weakness is insufficient data for detailed accident initiator frequency assessments. The methodology includes accidents, normal radioactivity emission rate (primarily tritium), occupational exposure, and waste issues.

To help guide earlier stages of the study, a series of initial comparisons using the safety evaluation indices have been performed. These are no means final; rather, the intent is to help focus attention on the relative advantages and disadvantages of each concept to promote design improvement. At this stage, among coolants, the safety ordering (best to worst) is helium, liquid metals, and pressurized water. The alternative concept HTS coolant has not received enough analysis to include it in the comparison. Among liquid breeders, ^{17}Li - ^{83}Pb has a slight overall advantage over pure lithium. Liquid breeders have not yet been compared with solid breeders. Among solid breeders, there is no strong safety preference at this time. For those concepts that require a neutron multiplier, which is generally a safety disadvantage, there is no strong preference between beryllium and lead.

These initial comparison results are not surprising since they generally follow the result of a recent systematic study⁽³⁾ of blanket material choice on potential fusion accident consequences. In that study the coolant order was liquid metal, helium, and water, with the molten salt, flibe, considered quite attractive. ^{17}Li - ^{83}Pb and lithium were considered quite close overall. Three differences between the current study

and that effort explain the reversal in order between liquid metal and helium coolants. First, the current helium design is more tolerant to pressure driven transients than that assumed in the earlier study. Second, a key assumption in Ref. 3 is that the chemical reaction problems of lithium are solvable by design by eliminating oxidants in or near the blanket. However, the current study includes the assumption that water is used as the coolant in the limiter/divertor (tokamak) or choke coil (mirror) so that at least one oxidant is near the blanket. Third, recent activation results indicate that the activity levels in ^{17}Li - ^{83}Pb from the lead should not be ignored as they were in Ref. 3.

Molten salts have some generally attractive features, particularly low pressure and low MHD-related problems. The molten salt, flibe, scored well in the earlier study⁽³⁾ but has not received strong consideration in the current effort. The molten salt HTS has some serious safety problems due to the activity levels caused by $\text{N}(^{14}\text{C})$, $\text{Na}(^{22}\text{Na}, ^{24}\text{Na})$, and $\text{K}(^{39}\text{Ar}, ^{41}\text{Ar})$ in the salt. The accident-related consequences have not yet been seriously studied but the normal radioactivity emission control would be difficult, although likely feasible.

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APPENDIX A. BCSS DESIGN GUIDELINES

A set of design guidelines for use in all study tasks was issued at the beginning of the study. The purposes are:

- (1) to establish the value (or range of values) of parameters and to state assumptions that require consistency in treating all (or most) blanket concepts; and
- (2) to provide guidance on the approach to handling issues of broad interest in all concepts.

These guidelines have been updated periodically as required. The present version is provided in this Appendix. A number of the key guidelines are reviewed in Sec. II.2 of this report.

BLANKET COMPARISON AND SELECTION STUDY**DESIGN GUIDELINES****REV. 2 - 10 JUNE 1983**Purposes

- To establish the value (or range of values) of parameters and to state assumptions that require consistency in treating all (or most) blanket concepts.
- To provide guidance on the approach to handling issues of broad interest in all concepts.

[Note: Parameters and data specific to one blanket concept will be generated by the group working on the concept, not by Design Guidelines Group.]

Examples

- Maximum allowable temperature for structural materials
- Operating temperature range for solid breeders
- Surface heat load, erosion rate
- Divertor vs. limiter for tokamaks

GROUP ORGANIZATION

Leader: Abdou/Morgan

Participants

Leader of Evaluation Criteria Group (Baker)

- All leaders of individual tasks
 - e.g., Smith (structural material data and limits); Johnson (solid breeders data and limits)

Procedure

- Specifications for Design Guidelines will be communicated to all project members by Abdou or Morgan.
- Guideline revisions will be provided in the future, for the duration of the project.

How parameters and assumptions are added to the list

- Based on requests initiated by the project manager or any member of the project.
- The technical specifications will be developed by the appropriate group (e.g., solid breeder tritium recovery group).
- The requests can be made to Abdou, Morgan, or the appropriate group leader.
- The final specifications will be reviewed and issued by the Design Guideline Group Leader.
- Any member of the project can suggest changes. All requests will be reviewed.

Parameter(s): Neutron Wall Load

Value: 5 MW/m² [Note: A range of values may be added later.]

Parameter: Surface Heat Load

Value: Consider all the following values in the first phase of the project:

10 W/cm²

50 W/cm²

100 W/cm²

For cases where large changes occur in the results, also consider intermediate values (25 W/cm² and/or 75 W/cm²).

- In cases where it is necessary to consider a single value for the surface heat load: assume 50 W/cm².
- Later phases of the project will consider one value or a range for surface heat load based on results of evaluation in the first phase.

Parameter: First Wall Thickness

Approach: Determine the maximum allowable thickness for each value of the surface heat load, accounting for constraints specified below and elsewhere in the guidelines. Consider all material to be continuous with first wall, i.e., no separate layer of sacrificial material. The following guidelines shall apply:

- o Erosion rate (FW plasma-facing surface) = 1 mm/calendar year.
- o Bulk temperature limits and stress limits apply to all FW/B structure and to all FW "erodable" material.

Parameter(s): Maximum Allowable Structure Temperature

Maximum Allowable Temperature at Structure Interface with Coolant or Breeder

Approach:

- Values not shown will be provided later by the Materials and Corrosion Groups.
- The lifetime of any structural material varies with the operating temperature. This relationship is provided at discrete values of temperature for PCA (see Stress Limits), and will be provided for other materials at a later date.

STRUCTURE BULK TEMPERATURE LIMITS

Preliminary Design Maximum Temperature Limits ^a (°C)		
Austenitic Steel ^c (PCA)	Ferritic Steel (HT-9)	Vanadium Alloy (V-15Cr-5Ti)
550	TBD ^b	TBD

^a Minimum temperature limits (if any) not included.

^b TBD = To Be Defined (by Materials Task Group)

^c Annealed or 20% cw.

Parameter: Stress Limits

Approach: The allowable stresses for PCA austenitic stainless steel in both unirradiated and irradiated conditions are shown in the following table and figures. Both primary and primary-plus-secondary stress limits are to be met for all FW and blanket PCA structure, including any FW erosion allowance. (Background and rationale for these limits are discussed in Saurin Majumdar's memo of June 1, 1983.)

ALLOWABLE STRESSES FOR PCA

Temp. (°C)	Unirradiated (≤ 10 dpa)					Irradiated (> 10 dpa)		
	S_m (MPa)	$S_{mt}^{(1)}$ (MPa)				S_m (MPa)	$S_{mt}^{(2)}$ (MPa)	
		10^4 h	2×10^4 h	3×10^4 h	5×10^4 h		75 dpa	100 dpa
100	225	225	225	225	225	225	190	155
200	208	208	208	208	208	208	190	155
300	205	205	205	205	205	205	190	155
400	205	205	205	205	205	205	190	155
500	205	205	205	205	205	190	185	150
550	195	195	195	190	175	175	150	130

(1) For $t \leq 2.8 \times 10^4$ h, $S_{mt} = S_m$ (Unirradiated).

(2) For $dpa \leq 70$, $S_{mt} = S_m$ (Irradiated) Based on a Maximum Strain Limit of 5%.

Parameter: Stress Limits (cont'd).

STRUCTURE TEMPERATURE LIMITS AT LIQUID METAL INTERFACEPreliminary Design Temperature Limits^a (°C)

System	Austenitic Steel (PCA)	Ferritic Steel (HT-9)	Vanadium Alloy (V-15Cr-5Ti)
Lithium Circulating Static	400 } 450 } ^b	500 550	750 } 750 } ^c
¹⁷ Li-83Pb Circulating Static	350 400	450 500	>650 >650

^a Minimum temperature limits (e.g., due to liquid metal embrittlement) are not included.

^b Could be higher if initial Ni dissolution rates can be tolerated.

^c Vanadium alloys may weaken above 700°C because of loss of strengthening due to oxygen. Also, pickup of N from Li may embrittle the V alloys.

Parameter: Total First Wall Plus Blanket Thickness

(Does not include shield thickness)

Approach: Optimize but limit to ≤ 70 cm if possible. (If thicker blanket is necessary, then trade must be made with shield thickness. Dose rate to SC magnets will govern total blanket/shield thickness for a given concept.) Larger thickness may:

- Increase reactor capital cost
- Increase tritium breeding ratio
- Increase the fraction of thermal power recovered as useful energy

Energy deposited in the shield is by definition not recoverable. All (neutron) energy recovery regions (breeder, multiplier, reflector, etc.) should be included in the first wall and blanket.

Parameter(s): Properties and Operating Temperature Ranges for Solid Breeders
Value(s):

PRELIMINARY VALUES FOR
CANDIDATE SOLID BREEDER MATERIALS^a

PROPERTIES				RECOMMENDED TEMPERATURE LIMITS			INFORMATION ONLY			
	MP, °C	ρ_{Li} , g/cm ³	K^b , W/mK	T_{min} , °C	T_{max} , °C	ΔT , °C	$K \cdot \Delta T$	$\rho_{Li} \cdot K \cdot \Delta T$	Tritium Disso- lution	Breeding Capa- bility
Li ₂ O	1433	0.93	3.4	410 ^c	^{c,h} 800	440	1496	1391	Low	Good
γ -LiAlO ₂	1610	0.28	2.2	300 ^d	1200 ^f	900	1980	554	Moderate	Poor
Li ₅ AlO ₄	1047	0.61	2.3	350 ^d	780 ^g	430	989	603	High	Fair
Li ₂ SiO ₃	1200	0.36	1.5	410 ^d	1000 ^f	590	900	319	High	Poor
Li ₄ SiO ₄	1250	0.54	1.5	320 ^d	950 ^f	630	945	510	Moderate	Fair
Li ₂ ZrO ₃	1616	0.33	1.3	400 ^e	1400 ^f	1000	1300	429	Moderate	Poor
Li ₈ ZrO ₆	1295	0.68	1.5	350 ^e	980 ^g	630	945	643	Moderate	Fair
Li ₂ TiO ₃	1550	0.33	2.0	400 ^e	1185 ^g	785	1570	518	Moderate	Poor

^a Separate effects data not available, therefore effects of radiation on temperature limits is unknown and not reflected in this table.

^b Estimated for 85% smeared density and temperature of 1000 K.

^c Established from chemical considerations, i.e., reaction with moisture to form LiOH.

^d Established from diffusion/inventory considerations.

^e Estimated assuming similar properties.

^f Established from onset of closed porosity, experimentally.

^g Estimated assuming $T_{max} = 0.8 T_m$, K.

^h 1000°C for design approaches with helium purge gas flow directed only to the "cold" region of the breeder.

Parameter: Tritium Breeding Ratio (TBR)

Approach:

- Each blanket concept will be judged by its Tritium Breeding Potential (among other factors).
- Example (not a specification): $(TBR)_N = \text{maximum net TBR}$
 - $(TBR)_N < 1.05$ Rejected
 - $1.05 \leq (TBR)_N < 1.10$ High Risk
 - $1.10 \leq (TBR)_N < 1.15$ Medium Risk
 - $1.15 \leq (TBR)_N < 1.20$ Low Risk
 - $(TBR)_N \geq 1.20$ Attractive
- The breeding ratio evaluation scale will be developed by the Tritium Breeding Issues Group for review by Evaluation Criteria Group.
- Concept designers should ensure that the breeding ratio with full coverage is greater than 1.2. Examine methods to increase the breeding ratio, i.e., evaluate breeding potential.
- Tritium Breeding Issues Group is examining:
 - Breeding requirement (depends on T inventory, doubling time, etc)
 - Impact of overall reactor on breeding ratio

Parameter: Blanket L/R Time Constant (τ)

Value: 50-200 ms (desired range for two-directional current flow in a segmented blanket)

(See L. R. Turner memo of 11 March 1983)

Reactor Configuration

For designs that may be strongly influenced by the reactor configuration (e.g., MHD effects for flowing liquid metals), consider:

Tokamaks: STARFIRE (with 10 DEMO - type Tf coils, 10 T max. field)

Mirrors: MARS

Tokamak Impurity Control

Assume a FED/INTOR type of limiter at the bottom of the plasma chamber. (See Chapter VII of USA FED-INTOR/82, October 1982.)

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APPENDIX B. POWER CONVERSION SYSTEMS AND IHX CONSIDERATIONS

B.1 Power Conversion Systems

E.1.1 Introduction

Because all of the blanket concepts considered in this study operate at moderate-to-low coolant temperatures, 600°C or less, the only practical choice for power conversion is steam. Rankine steam cycles offer the highest efficiencies at moderate temperatures, primarily because, unlike the gas turbine cycle, the recirculating power required to recompress the working fluid is small.

Four steam cycles are shown in this appendix. Since they are suitable for use with peak coolant temperatures of 320°, 400°, 500°, and 600°C, they should cover the range of requirements for the dozen blanket concepts examined in this study. The cycles were developed using the code STMC, developed by GA Technologies. It can analyze steam power cycles with 0 to 3 reheats, 6 to 8 steam extractions, and moisture extractions. Realistic pressure drops and temperature differentials are included, as is a correction for turbine efficiency drop due to moisture. The code has been benchmarked to an existing power plant to within 0.05 efficiency percentage points.

While the choice of cycle depends upon peak coolant temperature, the details of the cycle design depend upon minimum coolant temperature as well. The minimum temperature affects coolant flow rate and therefore, pumping power, and, in some designs, the peak temperatures in the first wall. While the economics of fusion reactor plants almost certainly encourages striving for maximum power conversion efficiency, compromises are necessary in order to meet blanket coolant requirements and at the same time eliminate a common problem called the pinch point, which is the minimum coolant-to-steam temperature difference. It occurs at that location in the steam generator where boiling begins.

A typical heat line, which essentially shows the temperature difference between the coolant and steam along the steam generator, is shown in Fig. B.1-1. As seen, the pinch point arises from the fact that, while the

Figure B.1-1. Schematic of steam and coolant heat lines illustrating pinch point problem.

coolant temperature uniformly decays as it passes through the steam generator, the steam temperature does not: the boiling phase of heat addition occurs at constant temperature. High pressures and reheats, which raise efficiency, also exacerbate the pinch point problem.

B.1.2 Selected Power Cycles

The power cycles chosen are not the highest performance cycles possible for the given range of temperatures, but rather reflect the compromises necessary to satisfy coolant inlet and outlet temperatures consistent with pinch point requirements. They therefore have rather low steam pressure and no reheats. Further iterations seem warranted to see if adjustments in coolant and steam conditions would produce any net gains.

Details of the cycles chosen are shown in Table B.1-1. Note that power conversion efficiency does not include blanket pumping power loss. Pumping

power also counts as a heat addition and so the efficiency degradation is less than might be expected and is given by

$$\frac{\eta_{\text{net}}}{\eta_{\text{pc}}} = 1 - \frac{P_{\text{circ}}}{Q_{\text{in}} \eta_{\text{cA}}} \left(\frac{1}{\eta_{\text{pc}}} - 1 \right) , \quad (\text{B.1-1})$$

where η_{pc} is the efficiency of the power conversion system alone, η_{cA} is the compressor adiabatic efficiency (85% to 90%), P_{circ} is the circulator pumping power, Q_{in} is the fusion power, and η_{net} is the overall conversion efficiency.

TABLE B.1-1. SELECTED POWER CYCLES.

	A	B	C	D
Coolant temperatures, °C	285 to 320	280 to 400	275 to 500	300 to 600
Type	Sat Vapor	Superheat No reheat	Superheat No reheat	Superheat No reheat
Peak press, MPa	6.4	6.9	8.3	16.5
Peak temperature, °C	280	380	460	538
Steam extractions	6	7	6	7
Condensing temperature, °C	32	32	32	32
Moisture extractions	3	1	1	1
Power conversion efficiency	0.340	0.365	0.391	0.429

The heat lines for the selected cycles are shown in Fig. B.1-2. In order to satisfy minimum coolant temperature criteria, pinch point temperature differences of only 10°C are required. But because heat exchanger size depends on the logarithmic mean of the inlet and outlet temperature differences, a doubling of this value to the more typical 20°C might reduce heat exchanger size by only 20% to 30%. The turbine steam temperatures can be selected from available machinery with minimum impact on the selected cycles.

Figure B.1-2. Heat lines for the four selected cycles.

B.1.3 Higher Performance Options

As mentioned above, the selected cycles reflect compromises necessary to maintain blanket coolant conditions. Typically, they have lower pressure steam and no reheats. If some adjustment in blanket temperature conditions could be made, higher performance cycles could be possible. The greatest gains are possible with the two intermediate temperature cycles. Results of modifications to these cycles are shown in Table B.1-2 and Fig. B.1-3. Note that each improvement increment in efficiency comes at a price of increased power conversion complexity and increased coolant inlet temperature. Whether the gains are worth it could be answered by an economic analysis of the entire reactor plant.

TABLE B.1-2. HIGHER PERFORMANCE VARIATIONS OF CYCLES B AND C.

Cycle	B-1	B-2	C-1	C-2
Steam pressure, MPa	10.3	10.3	12.4	12.4
No. reheats	0	2	0	1
Net efficiency	0.387	0.396	0.408	0.418
% improvement	6.0	8.5	4.3	6.9
Coolant inlet temperature, °C	310	320	310	325
Change, °C	+30	+40	+35	+50

B.1.4 Recommendations

Because of the high cost of a fusion reactor core, economic issues are unlikely to be resolved in the same way as they are in fission or fossil plants. In HTGR plants, for example, it was found that a medium performance power cycle was optimum. Here, however, it seems that any improvement in efficiency may be justified, provided it does not impact the reactor core cost. Therefore, such improvements must come from increased power conversion system complexity (and therefore capital cost) rather than increased primary coolant (and therefore blanket) temperatures.

Figure B.1-3. Heat lines for higher performance cycles.

B.2 IHX Considerations

B.2.1 Introduction

The objectives of this task were to assemble information pertaining to the need for and cost of an intermediate heat exchanger, double-walled steam generator, or similar component (thus the designation IHX); and to consistently evaluate the need for an IHX for all blanket/coolant configurations and provide uniform recommendations on the necessity and impact of an IHX. The areas selected for initial consideration were safety, tritium permeation, thermal efficiency, and cost.

The approach taken to achieve the above objectives was as follows:

1. Consult the literature as to conditions in which fission reactor designs have/have not incorporated an IHX.
2. Explore the options available for an IHX.
3. Formulate list of reasons for and against an IHX specific to each particular blanket/coolant selection.
4. Compile a uniform list of blanket/coolant characteristics from which to evaluate thermal efficiency degradation, economic penalty/benefit, and safety benefits of incorporating an IHX.
5. Recommend IHX option for each blanket/coolant concept.

Steps 1 to 3 are discussed in the remainder of this section, including the data input from the blanket groups. Steps 4 and 5 are discussed in Sections B.2.2 through B.2.4, covering the effect of an IHX on power conversion efficiency, cost of an IHX, and the recommendations of this study, respectively.

Review of the literature showed that fission reactors for electrical production with either water or gas primary coolant have not incorporated any type of IHX. The primary concern has been radiation exposure from fission product and tritium carry-over into the secondary loop in both water and gas systems. Experience has borne out the lack of need for an IHX from purely

safety reasons, although steam generator (SG) repairs have been severely complicated by the radioactivity transferred to the SG. A secondary concern in gas systems has been the reaction of core materials with steam and other impurities from tube leakage, and this situation is well documented, analyzable, predictable, and acceptable without an IHX.

Reactors with liquid metal primary coolant have incorporated an intermediate coolant loop and either a regular heat exchanger, a duplex (double-walled) heat exchanger in which a nearly metallurgical bond is made between the tubes, or a duplex heat exchanger in which machined grooves between the tubes serve for leak detection. The primary concerns have been protection of equipment from the reactivity of the liquid metal and radiation exposure from the release of fission products contained in the primary coolant circuit in the event of steam-liquid metal reaction. The IHX affords the benefits of providing thermal inertia with which to minimize the effects of trips and other thermal transients (important in liquid metal systems to avoid the thermal shock resulting from good conductivity) and serving as an additional barrier to radioactive product transport to the SG.

The literature review also resulted in a compilation of potential IHX candidates. These are listed in Table B.2-1. The table includes options that are still in early stages of development, but are included for completeness. Only the standard heat exchanger, purged-duplex heat exchanger, and intermediate loop were evaluated within this scope of work.

TABLE B.2-1. POSSIBLE IHX OPTIONS.

Standard heat exchanger (steam generator)
Duplex heat exchanger (with/without machined grooves within tubes for leak detection)
Purged-duplex heat exchanger (purged tube annulus)
Fluid-bath or fluidized-bed heat exchanger
Intermediate loop with standard heat exchanger/steam generator
Intermediate loop with duplex heat exchanger
Intermediate loop with purged-duplex heat exchanger
Intermediate loop with combinations of above

The reasons both in favor of and against an IHX are presented in Table B.2-2. The list of Table B.2-2 could be quantitatively evaluated if the data of Table B.2-3 were available for each blanket/coolant concept. In fact, with the data available for the concepts presented in Table B.2-4, only the tritium permeation losses, the reactivity of the primary coolant, the capital equipment cost and efficiency degradation could be quantified at present. The recommendations presented in Section B.2.4 were thus formulated on the basis of only these latter quantities.

TABLE B.2-2. REASONS IN FAVOR OF AND AGAINST AN IHX.

In favor of having an IHX:

- Minimize the cost and radiation exposure of tritium permeation losses
- Minimize probability and quantity of radioactivity mobilization from interactions of primary coolant with steam during fault conditions
- Minimize probability of damage to blanket from interactions of blanket materials with steam
- Minimize probability of damage to plant equipment from interactions of primary coolant with steam and steam-side components during fault conditions
- Minimize downtime and complications of repair and radiation exposure to maintenance workers and the public by reducing the radioactivity in the steam generator and steam power loop
- Provide thermal inertia

Reasons against having an IHX:

- Increased capital equipment cost
- Decreased efficiency of electricity production
- Increased probability of system failure and downtime resulting from additional system and attendant complexities

TABLE B.2-3. UNIFORM LIST OF BLANKET/COOLANT CHARACTERISTICS.

- Tritium

Inventory, form, concentration, and partial pressure in primary loop

- Safety

- Activation product level in primary loop
- Thermal-hydraulics characteristics of primary loop; coolant/blanket structural materials definition; max/min temperatures, pressure; total volume and mass; mass flow rate

- Cost

- Capital cost of IHX equipment
- Thermal efficiency degradation
- Failure probabilities of steam-side components; estimates of downtime/cost to repair steam generator and steam loop failures with and without radioactivity; estimates of occupational exposure during such repairs.

B.2.2 Effect of IHX on Power Conversion Efficiency

An IHX reduces efficiency because of the lower peak power conversion temperature and added circulator (pumping) power. Since pumping power is typically in the range of 1% for liquid metal systems to 2.5% for helium and also counts as heat addition, the main effect is that of the ΔT . Note that even this effect can be minimized by design with greater surface area such that, in effect, the tradeoff is one of efficiency versus capital cost.

The efficiency degradation can be computed parametrically for each steam power cycle as a function of ΔT . However, it can be shown that for an actual efficiency 75% of Carnot, the effect of a ΔT_{IHX} is more deleterious at low temperatures and is at worst a drop of three percentage points in efficiency

TABLE B.2-4. IHX TASK INPUT FROM BLANKET GROUPS

<u>Primary Coolant Characteristic</u>	<u>Li/Li</u>	<u>LiPb/LiPb</u>	<u>LiPb/HTS</u>	<u>Li₂O/H₂O</u>	<u>Pb/LiAlO₂/H₂O</u>
Tritium(a)					
Form	T ₂	T ₂	T ₂ O	T ₂ O or T ₂	T ₂ O
Inventory, g	400	80	NA	65	<0.5(b)
Concentration, Ci/m ³	5.0 × 10 ³	1.0 × 10 ³	NA	10 ³	<19
Partial pressure, Pa	10 ⁻⁷	1	NA	7	<0.1
Source term into coolant, Ci/d	4.3 × 10 ⁶	4.3 × 10 ⁶	2.1 × 10 ³	0.1 or 100	<1(c)
Activation Products					
Form			Ar ³⁹ Na ²² Na ²⁴		
Inventory, Ci			10 ⁷ 10 ⁷ 6 × 10 ⁷		
Thermal-Hydraulics					
Coolant	Li	17Li83Pb	NaNO ₂ (49%) NaNO ₃ (7%) KNO ₃ (44%)	H ₂ O	H ₂ O
Min/Max temp., °C	250/400; 300/500; 400/600	350/500; 400/600	240/470	280/320	300/320
Pressure, in/out, MPa	3.0/0.2	3.0/0.2	<1	15	15
Volume m ³	760	790	300	650	300
Mass, kg	380,000	7,400,000	530,000	403,000	200,000
Mass flow rate, kg/s	6,400	140,000	11,000	18,400	36,000
Volume flow rate, m ³ /s	13	15	6	30	54

TABLE B.2-4 (Continued)

<u>Primary Coolant Characteristic</u>	<u>Li₂O/He</u>	<u>Li/He</u>	<u>¹⁷Li¹⁸³Pb/He</u>	<u>FLIBE/He</u>
Tritium(a)				
Form	T ₂ O	T ₂	T ₂ O/T ₂	T ₂ O & T ₂
Inventory, g	5 × 10 ⁻³	1 × 10 ⁻⁷	41	4
Concentration, Ci/m ³	0.02	5 × 10 ⁻⁷	170	32
Partial pressure, Pa	2 × 10 ⁻³	4.5 × 10 ⁻⁸	3.1/2.2 × 10 ⁻¹²	1.2 * ?
Source term into coolant, Ci/d	2.6 × 10 ³	10	5.5 × 10 ⁶	5 × 10 ⁶
Activation Products				
Form				
Inventory, Ci				
Thermal-Hydraulics				
Coolant	He	He	He	He
Min/Max temp., °C	275/500	275/525	275/500	325/500
Pressure, in/out, MPa	5	5	5	5
Volume m ³	2420	2420	2420	3000
Mass, kg	8500	8500	8500	11,000
Mass flow rate, kg/s	3420	3060	3420	4300
Volume flow rate, m ³ /s	970	870	970	1200

(a) Not including first wall permeation.

(b) Without cleanup after ten years.

(c) Assuming oxide barriers and removal from the lead.

for a ΔT_{IHX} as high as 40°C. Thus for the purposes of this study, the IHX need not be further defined.

B.2.3 Cost of IHX

Unit cost data obtained from References 1 and 2 were used to evaluate the cost of IHX components. Costs are normalized to 1983 dollars. The unit cost data and cost for a 4000 MW(t) reactor are shown in Table B.2-5. Note that no specifics of the design (materials, purge gas, etc.) are assumed; the data are merely considered typical for the range of components which might be used in a fusion power plant. Also, not included is the additional cost of associated pumping and piping. Still, the results show that the capital cost of an IHX unit can be significant.

TABLE B.2-5. COST OF IHX COMPONENTS.

Component	Unit Cost [\$ /kW(t)]			Cost for 4000 MW(t) (\$M)		
	Primary Coolant			Primary Coolant		
	H ₂ O	He	Li/LiPb	H ₂ O	He	Li/LiPb
Steam generator(1)	11	28	20	44	112	80
Double-walled,(2) purged steam generator	17	38	28	68	152	112
Intermediate heat exchanger(1) (to Na)	NA	20	33	NA	80	132

Note: Cost of other intermediate loop components such as pumps and piping not evaluated.

B.2.4 Recommendations

Due to the possibility of mobilizing significant activity in the event of a steam generator leak, an intermediate loop is recommended for the

lithium-cooled blanket design. This is required in order to provide physical separation between the primary coolant and water. Due to the reduced chemical activity of $^{17}\text{Li}^{83}\text{Pb}$, an intermediate loop is not required. However, in order to meet the study's design guideline for tritium leakage of ≤ 100 Ci/d, a double-walled, purged-annulus steam generator is recommended for designs with $^{17}\text{Li}^{83}\text{Pb}$ coolant.

No type of IHX is necessary for He-, H_2O -, and HTS-cooled concepts from considerations of either reactivity of the primary coolant or tritium permeation. This recommendation assumes no permeation of T_2O , and a factor of 100 reduction in T_2 permeation due to an oxide barrier in the SG. This recommendation should be re-evaluated if these assumptions are reassessed, or if the 100 Ci/d safety guideline on the allowable tritium loss rate is modified. The first wall tritium permeation rate was not included in the calculations due to the uncertainty in its magnitude. This could be a significant source of T_2 into the primary coolant and should be included in future calculations. Similarly, isotopic exchange across the SG was not included and should be examined further. Finally, all the recommendations should be re-evaluated if any of the other criteria in Table B.2-2 are quantified.

REFERENCES FOR APPENDIX B

1. S. C. Schulte, et al., "Fusion Reactor Design Studies - Standard Unit Costs and Cost Scaling Rules," Battelle Report PNL-2987, September 1979.
2. B. Badger, et al., "WITAMIR-I," University of Wisconsin, Report UWFD-400, September 1980.

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APPENDIX C. HELIUM-COOLED, VANADIUM ALLOY STRUCTURE BLANKET DESIGN

C.1 Helium Coolant Moisture Control

As indicated in Section IV.3, in order to use vanadium or vanadium alloy as the structural material at temperatures between 400° to 550°C, the moisture level in the helium coolant has to be extremely low: ~ 0.01 and 0.1 vppm for liquid lithium and solid breeder blankets, respectively. The minimum moisture level at which a power conversion helium coolant can be maintained depends upon the knowledge and trade-offs of many factors. For example, the impurity source and sink rates of the coolant loop, chemistry control of the equilibrium level of the H₂, O₂, and H₂O concentrations, and the efficiency and cost of the coolant clean-up system need to be identified and evaluated. Since the detailed design of the blanket coolant loop is not available, the approach in this first year of the BCSS study is to identify what is a reasonably achievable impurity level in the helium coolant and try to match the vanadium alloy requirements.

Experience from the 300 MW(e) GCFR design⁽¹⁾ indicate that, with in-leakage of 0.58 kg/h from the steam generator section of the helium loop and a reasonable coolant cleanup rate of 1300 kg/h, the moisture level in the helium loop can be maintained at ~ 100 vppm. This would not be acceptable for vanadium alloys at temperatures of interest.

If necessary, the moisture levels can be reduced by the following options:

Option 1. Single wall heat exchanger improvements

- Material selection (e.g., Incoloy - 800 tubes)
- Improved QA and QC procedures
- Care with design details
- Controls on fabrication processing
- Water-side corrosion control

Option 2. Double wall heat exchanger

Option 3. Use of a closed-cycle gas turbine (CCGT) - e.g., 50 MW(e) Oberhausen 2 turbine plant

C.2 Conclusions and Recommendations

Based on the investigations of the effects of moisture on vanadium metal and alloys, and reasonably achievable moisture content in a helium-cooled power conversion loop, the following conclusions and recommendations can be made:

1. Helium coolant with a conventional steam cycle power conversion system using a single wall steam generator will not work with vanadium metal, due to the extremely low requirements on moisture level.
2. Vanadium alloy interaction with moisture should be determined further, preferably by experiment.
3. If a moisture level of $\ll 100$ vppm is required with vanadium alloys, designs using double wall heat exchanger and CCGT should be considered.
4. If a CCGT is to be selected as an option, the properties of vanadium alloys at high temperature should be identified, since for high thermal efficiency, a coolant temperature greater than 700°C is preferred.
5. If the moisture level requirement is ≤ 100 vppm, trade-off studies mentioned earlier will need to be performed to determine the best course of action.

REFERENCE FOR APPENDIX C

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APPENDIX D. HIGH WALL LOADING STUDY

D.1 Introduction

Several workers⁽¹⁻⁴⁾ have emphasized the substantial impact of power density on the economic potential of fusion power. In this context conceptual design studies of fusion reactors have exhibited a trend towards reduced size and high wall loading, design directions which strive to achieve high power density. The Blanket Comparison and Selection Study (BCSS) has adopted a relatively high neutron wall loading of 5 MW/m^2 as a design guideline. At the same time, it has been recognized that some economic benefit might be derived from operation at neutron wall loadings in excess of 5 MW/m^2 . In response to this issue, a High Wall Loading Study (HWLS) was initiated to complement the main thrust of the BCSS. The objective of the HWLS is to develop a quantitative understanding of the economic and engineering implications of operation at neutron wall loadings in the range $\sim 5 \text{ MW/m}^2 - 10 \text{ MW/m}^2$. The following sections of this Appendix discuss the approach and the preliminary results of the HWLS.

D.2 Economic Implications

The BCSS is examining blanket designs in the context of both the mirror and tokamak concepts. At present, documented economics models are available only for the tokamak concept. Therefore, the tokamak concept has been taken as the basis for the initial HWLS economic assessments. As a point of departure, the STARFIRE design⁽⁵⁾ and the STARFIRE/WILDCAT costing model⁽⁶⁾ have been adopted.

D.2.1 Design Space

Reactor design space was examined as a function of neutron wall loading by adopting the following assumptions:

1. The fusion power is fixed at the STARFIRE value ($\sim 3600 \text{ MW}$).
2. The toroidal field on axis is fixed at the STARFIRE value ($\sim 5.8 \text{ T}$).
3. Volume averaged beta scales as the square root of power density and is normalized to a value of 6.7% for the STARFIRE parameters.

4. Blanket and shield thickness are fixed at the STARFIRE values.
5. Blanket performance and power conversion efficiencies are fixed at the STARFIRE values.

Based on the above assumptions, plasma radius, maximum toroidal field and volume averaged beta were calculated as a function of neutron wall loading for aspect ratios (A) of 3, 4, 5 and 6. These results are shown in Figs. D-1, D-2 and D-3, respectively.

Figure D-1 shows that the plasma radius decreases as wall loading increases. The plasma radius for an aspect ratio of 4 and a wall loading of 10 MW/m^2 is approximately 1 meter. The plasma radius for all four aspect ratios decreases by a factor of almost two as wall loading increases from 2 to 10 MW/m^2 ; however, plasma radius appears to level off at wall loadings greater than 10 MW/m^2 .

Figure D-2 shows that maximum field increases as wall load increases. The maximum field for $A=3$ is ~ 18 tesla at 10 MW/m^2 . An upper limit of approximately 14 Tesla is fixed by present magnet technology; thus, an aspect ratio of 3 is not considered as a "viable" design parameter in this study. At an aspect ratio of 4 it is seen that the maximum field at 10 MW/m^2 is approximately 13 Tesla, a feasible value. The magnets are assumed to be graded, with Nb_3Sn superconductor used for fields in excess of ~ 9 Tesla, and the remainder of the coil being NbTi superconductor. A higher maximum field will require that a greater percentage of the coil be constructed of the more costly Nb_3Sn material. Thus, the TF magnet cost model adopted here⁽⁶⁾ may yield optimistically low values.

Figure D-3 gives the volume averaged betas required for the design space of Fig. D-2. Note that extremely high betas ($\sim 15\%$) are required to achieve neutron wall loadings approaching 10 MW/m^2 under the assumptions of this study. It has been suggested⁽⁷⁾ that such values of beta might be achieved through proper shaping of the plasma. The plasma shaping coils and system may represent an appreciable increase in capital costs. At this point of the study, we have chosen not to address the design and economic impact of achieving high values of beta. Thus, the preliminary results must be viewed as optimistic with regard to the economic impact of high wall loading operation.

Figure D-1. Minor radius vs. neutron wall load.*

Figure D-2. Max field vs. neutron wall load.

* Note that fusion power and field on axis are held fixed for Figs. D-1 through D-6.

Figure D-3. Average beta vs. neutron wall load.

D.2.2 Cost Analysis

The cost of electricity exhibits a functional dependence as shown below:

$$\text{COE} \propto \frac{C_{\text{cap}} + C_{\text{o\&m}} + C_{\text{cr}} + C_{\text{f}}}{A_v \times E} \quad [\text{D-1}]$$

where

C_{cap} = Annual Capital Cost

$C_{\text{o\&m}}$ = Annual Operations and Maintenance Cost

C_{cr} = Annual Scheduled Component Replacement Cost

C_{f} = Annual Fuel Cost

A_v = Availability

E = Net Electric Power

In this study $C_{\text{o\&m}}$, C_{f} and E are assumed fixed at the STARFIRE values. Thus, only C_{cap} , C_{cr} and A_v are variables.

The annual capital cost is calculated as a fixed fraction of the total capital cost, C_t . The total capital cost is calculated using the costing models of reference 6. Relative total capital cost for an aspect ratio of 4 is shown as a function of wall loading in Fig. D-4. These relative total capital costs are normalized to the STARFIRE value of total capital cost. Note that the capital cost decreases by approximately 40% as neutron wall loading increases from 2 to 6 MW/m². The capital costs continue to decrease for wall loadings greater than 6 MW/m², however, this decrease is slight.

Total capital cost is composed of several cost components: reactor plant equipment costs, turbine plant equipment costs, electric plant equipment costs and miscellaneous equipment costs. Reactor plant equipment cost is the largest component of the total capital cost. The remaining cost components are generally designated as "balance of plant costs". Figure D-5 shows the variation of relative reactor plant equipment cost as a function of wall loading for an aspect ratio of 4. These relative plant equipment costs are normalized to the STARFIRE value of total capital cost. Reactor plant equipment cost initially decreases by a factor of about 2.5 as wall loading is increased from 2 to 6 MW/m²; it then decreases only gradually as wall load is increased beyond 6 MW/m². By comparing Figs. D-4 and D-5 it is observed that the

Figure D-4. Relative total capital costs vs. neutron wall load.

Figure D-5. Relative reactor equipment costs vs. neutron wall load.

balance of plant costs tend to dampen the impact of high wall loading on the total capital cost.

The annual scheduled component replacement cost is assumed to be dominated by the first wall/blanket replacement cost. Following reference 6, this cost is given by:

$$C_{cr}(\text{M\$}) = 17.36 \times \tau_s / \tau \quad [\text{D-2}]$$

where

- τ_s = First Wall/Blanket design lifetime for the STARFIRE design (6 yrs)
- τ = First Wall/Blanket design lifetime for the system under analysis

The first wall/blanket design lifetime, τ , can be calculated by assuming that it scales as the ratio of neutron wall load to the STARFIRE wall load. Thus, τ is given by:

$$\tau = (L_s / L) \times \tau_s = 22.32 / L \quad [\text{D-3}]$$

where

- L_s = STARFIRE wall load (3.72 MW/m^2)(6)
- L = Analyzed system wall load

C_{cr} can then be written as a linear function of neutron wall load:

$$C_{cr} = 4.67 \times L \quad [\text{D-4}]$$

As the neutron wall load is increased, the first wall/blanket lifetime decreases; this in turn could cause availability to decrease if the additional changeouts cannot be performed during the assumed annual maintenance shutdown. An expression for availability was developed based on the following assumptions:

1. The number of first wall/blanket sectors was fixed at the STARFIRE value of 24.

2. It takes 10 days to replace 2 sectors and the replacement is done on a non-interference basis. It is assumed that 4 sectors are replaced every 1/6th of the lifetime.

The availability can then be written as linear function of neutron wall load:

$$A_v = 0.8 - (0.015 \times L) \quad [D-5]$$

Figure D-6 shows two relative cost of electricity curves for an aspect ratio of 4; the top curve is COE calculated with a variable availability as given by Eq. [D-5] and the bottom curve is for a fixed availability of 0.75 (the STARFIRE value). The relative cost of electricity shown in this figure is normalized to the STARFIRE value of cost of electricity. As can be seen, COE, as given by both curves decreases by $\sim 20\%$ as neutron wall load is increased from 2 to $\sim 6 \text{ MW/m}^2$; this decrease is due primarily to the decrease in capital costs. The COE curve with a variable availability exhibits a minimum at $\sim 6 \text{ MW/m}^2$ and increases dramatically for wall loadings greater than $\sim 8 \text{ MW/m}^2$. Above this wall loading, COE appears to be a slowly increasing linear function of wall load; this is due primarily to the linear dependence of C_{cr} on wall load. Thus, for the assumptions adopted here, the COE appears to be very sensitive to availability and mildly sensitive to first wall/blanket replacement cost.

D.2.3 Future Work

The economic implication of high neutron wall loading, as presented above, are expected to be sensitive to several key assumptions. Future studies will investigate this sensitivity with regard to the following issues.

In order to achieve high wall loading, high betas were assumed. The design and economic impact of the poloidal field system necessary to provide the high beta configuration will be assessed. In addition, the design and economic impact of achieving high wall loading through increased on-axis toroidal field will be investigated.

In the studies reported here it was assumed that inboard shield thickness was independent of wall loading. In future studies the impact of a variable inboard shield thickness will be investigated.

Figure D-6. Relative COE vs. neutron wall load.

The cost analysis is quite sensitive to the unit cost of materials. This sensitivity will be examined in future studies.

As indicated above, the toroidal field coil cost algorithm may predict optimistically low values. In the future, an effort will be made to refine and possibly redefine this algorithm, based on more recent magnet cost studies.

In formulating the availability and annual scheduled component replacement cost algorithms it was assumed the first wall/blanket sectors would be replaced as a unit. At high wall loadings the first wall is expected to have a significantly shorter lifetime than the blanket. Utilizing a design that allows the first wall to be replaced separately may result in lower annual scheduled component replacement costs. The impact of such a design will be studied.

The annual scheduled component replacement costs were assumed to be dominated by the first wall/blanket replacement costs; however, at high wall loadings the cost of limiter/divertor component replacement may represent an appreciable fraction of the total. Future work will include the development of limiter/divertor replacement cost algorithms.

The availability algorithm employed may be too simplistic. Future work will investigate availability in more detail.

D.3 Engineering Implications

In examining the engineering implications of high wall loading operations the emphasis has been on predicting first wall lifetime as a function of erosion rate, heat load, and coolant characteristics. Such predictions require the ability to define the stress and temperature distributions within the first wall as well as the erosion rate at the first wall, a complicated and difficult task. However, some simplifying assumptions can allow a quick and useful comparison between different materials.

D.3.1 Model

The erosion rate of the first wall is very difficult to predict due to the uncertainty in the plasma edge conditions. It is assumed for this study that redeposition occurs such that the net erosion rate is held to only 1 mm/y.

As a starting point, the first wall configuration is assumed to consist of tubes with pressurized water coolant (different coolants would only change the heat transfer coefficient h and the pressure). The tube cross section, shown in Fig. D-7, is assumed to have a uniform volumetric nuclear heat generation rate within the walls of the tube and a surface heat load on only the half of the tube facing the plasma. Further, the surface heat load is a function of the angle, i.e.,

$$\begin{aligned} q'' &= \bar{q}'' \cos \theta & \theta &\leq \pi/2 & [D-6] \\ &= 0 & \theta &> \pi/2 \end{aligned}$$

A two dimensional solution to this situation is given in Appendix I of reference 8. For this case, the only modification to the solution of reference 8 was to assume the tubes had no axial or bending constraint. A computer program was developed for this analysis, allowing individual parameters (e.g., wall load, dimensions of tube) to be varied and the resulting stresses and temperatures to be compared against the maximum stresses and temperatures allowed for that material. Thus, the minimum and maximum wall thickness can be found. Using the difference between the two and dividing by the net erosion rate of 1 mm/y, the wall lifetime can be found.

D.3.2 Results and Discussion

In order to compare different first wall materials, several parameters were held constant. First, the inner radius was fixed at 6 mm (the size used in reference 8) and the different tube materials were assumed to have equal roughness (for coolant flow). Then, using pressurized water (2000 psi/13.8 MPa) the heat transfer coefficient h was determined (using the Dittus-Boelter correlation) to be $2.1 \times 10^4 \text{ W/m}^2 - ^\circ\text{K}$. Maximum bulk water temperature was set at 533°K (500°F). With these parameters fixed, only neutron wall load, surface heat load and outer radius needed to be varied.

Four materials were investigated, a high strength/high temperature material (V-15Cr-5Ti), two high strength/medium temperature materials (PCA austenitic stainless steel and HT-9 ferritic steel), and a low strength/low temperature material (Cu-Be). The material properties are shown in Table D-1, where

Figure D-7. Tube cross-section.

the strength values quoted are approximately 1/3 of the ultimate tensile strength after irradiation. For Cu-Be, irradiated values could not be found so strength values are about 1/5 of the ultimate tensile strength and $T_{\max}$ is approximately 1/2 the melting point (which may be too high). The impacts of radiation-induced swelling and creep have not been considered in this evaluation.

TABLE D-1. MATERIAL PROPERTIES

	Austenitic Stainless Steel	Ferritic Steel	V-15Cr-5Ti	Cu-Be
α ($\mu\text{m}/\text{m}^\circ\text{K}$)	17.7	11.3	10.6	16.0
k ($\text{W}/\text{m}^\circ\text{K}$)	19.5	29.0	29.4	180.0
E (GPa)	193.1	206.8	116.0	131.0
ν	0.294	0.265	0.3	0.3
$\sigma_{\max}$ (MPa)	130	125	230	100
$T_{\max}$ ($^\circ\text{K}/^\circ\text{F}$)	823/1022	823/1022	1023/1382	680/765
Volumetric Heat ⁽⁹⁾ Generation Rate MW/m^3	11.24	11.24	7.82	14.55
$1 \text{ MW}/\text{m}^2$ Wall Load				

The results for the four materials are shown in Figs. D-8 through D-17. Neutron wall loadings of 5 and 10 MW/m^2 were examined. The surface heat load is shown as a percent of the neutron wall load annotated on the curves. The lifetime curves (Figs. D-8, D-12, D-14 and D-16) readily show that austenitic stainless steel can only handle relatively low surface heat loads. For a 5 MW/m^2 neutron wall load and a surface heat load of $\sim 75 \text{ W}/\text{cm}^2$, the lifetime is about 3 y. Ferritic steels show substantial improvement, being able to handle about twice the surface heat load as austenitics for the same lifetime.

The lifetime curves for V-15Cr-5Ti and Cu-Be (Figs. D-14 and D-16) show that they both exhibit relatively long lifetimes for the assumed values of strength, $T_{\max}$ and erosion rate. The vanadium alloy is limited by temperature rather than by stresses, though a higher temperature coolant would change that. The Cu-Be is also limited by temperature.

The beginning of life (BOL) thickness curves (Figs. D-9, D-13, D-15 and D-17) follow the same trends as the lifetime curves. The austenitic stainless and ferritic steels have a slight increase in minimum thickness required (at end-of-life) at high surface heat loads due to high thermal stresses. This causes a slight change in the slope (from the lifetime curves) at high surface heat loads. The vanadium and copper alloys do not exhibit this change.

Two more curves are included to illustrate the information obtained from simple solution. Figure D-10 shows the maximum temperature difference between the first wall (on the side facing the plasma) and the bulk fluid temperature as a function of surface heat load. Figure D-11 shows the stress distribution on the outside and inside of an austenitic stainless steel tube 2.4 mm thick with a 5 MW/m^2 neutron wall load and a 25 W/cm^2 surface heat load.

D.3.3 Future Work

Future work will include examining the effects of different configurations and coolants on wall lifetime. Both lobe and plate first wall configurations will be considered. Gas coolants such as helium (at 50 atm) and liquid metals or molten salts at low pressure will be examined for their effects on wall lifetime. Also, the effects of erosion rate, allowable stresses and stress criteria will be investigated.

Figure D-8. Lifetime vs. wall load (austenitic stainless steel).

Figure D-9. BOL thickness vs. wall load (austenitic stainless steel).

Figure D-10. Maximum temperature difference vs. wall load (austenitic stainless steel).

Figure D-11. Stress distribution (austenitic stainless steel).

Figure D-12. Lifetime vs. wall load (ferritic steel).

Figure D-13. BOL thickness vs. wall load (ferritic steel).

Figure D-14. Lifetime vs. wall load (V-15Cr-5Ti).

Figure D-15. EOL thickness vs. wall load (V-15Cr-5Ti).

Figure D-16. Lifetime vs. wall load (Cu-Be).

Figure D-17. BOL thickness vs. wall load (Cu-Be).

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APPENDIX E

MOLTEN SALT SAFETY STUDIES

E.1 Introduction

Past studies^(2,3) have examined the possibility of using molten salts as primary coolant and/or tritium breeders in fusion reactors. Another study found that salts may offer distinct safety advantages compared with other coolants.⁽¹⁾ As discussed below, there are reasons for a somewhat new look at the issue of what molten salts might find application in fusion. Some analyses have been performed in support of the alternative concept task. An early examination of possible molten salts was conducted with a safety focus. After initial efforts in the study focused on HTS, the possible emission control problems for HTS were examined.

E.2 Safety Survey of Molten Salt Candidates

The effort described here is a systematic examination of the wide range of molten salts and possible applications.

E.2.1 Molten Salt Applications

Previously, molten salts have been considered only as primary blanket coolant or blanket tritium breeder.⁽¹⁻³⁾ However, other applications exist, including limiter/divertor coolant, shield coolant, and secondary loop coolant. For example, use of a lithium coolant/breeder module may eliminate the option of a water-cooled limiter. MHD forces and possible operating temperatures of lithium may prevent having a lithium-cooled limiter. Molten salts may offer a solution.

This effort has three general stages. First, the desired material properties of molten salts for fusion applications are examined. The wide range of applications adds to the difficulty of this task. Second, the exceedingly large number of possible salts is narrowed to a limited number of likely options. Third, a general focus for future research is indicated.

E.2.2 Motivation

Past studies have surveyed the molten salt field^(2,3) and failed to find any truly superior candidates. The best two appeared to be flibe (LiF-BeF_2) and HTS. The former was considered as a coolant/breeder and rejected for insufficient tritium breeding.⁽³⁾ The latter was considered as a non-breeding coolant, but recent work shows that the sodium and potassium in HTS lead to very high levels of radioactivity from the fusion neutron spectrum.⁽⁴⁾ Most past researchers have divided salts into (a) coolant/breeders that provide both blanket cooling and tritium breeding and (b) non-lithium-bearing coolants. Salts that fall between these cases generally have not been considered. For example, flibe has inadequate breeding to serve as a single coolant/breeder, so one study⁽³⁾ eliminated it without giving it strong consideration as a partiallybreeding coolant that would require a separate breeder. Since current results in the Blanket Comparison and Selection Study indicate that many tritium breeders will require a neutron multiplier to obtain adequate breeding, a partiallybreeding coolant would offer advantages over a non-lithium-bearing coolant. Of course, a single coolant/breeder that provided adequate breeding would appear preferable to having separate coolant and breeder, but no molten salt has been found that could be used as a single coolant/breeder.

Thus, a primary motivation for a new salt survey is the inclusion of partiallybreeding salts. In fact, as discussed later, there are several reasons why a lithium-bearing salt may be preferable to non-lithium salts, even if a separate breeder is required. A second motivation is the inclusion of safety concerns. Indeed, this study will focus on safety considerations. The third motivation is the awareness of applications other than primary blanket coolant.

E.2.3 Approach

Ideally, information would be sufficient to clearly define desired material properties of salts for each of the possible applications. Some of these properties would be absolute requirements; others would be desired

property objectives. Then, a finite number of alternatives would be considered and compared with the requirements. Surviving options would be rank-ordered via the desired property objectives. Unfortunately, that approach is not feasible today due to (a) insufficient clarity of the objectives relevant to the possible applications, (b) the enormous number of possible salts, and (c) insufficient data relevant to many of the desired material properties. A more modest approach is needed.

The first step is to determine the basic desired material properties without separation into absolute requirements and desired property objectives. Rather, the desired properties are used to perform two stages of screening: (a) eliminate salts with very low chance of use, and (b) eliminate lower quality possibilities. Also, the level of understanding and data neither allows or requires explicit separation of those material properties desired for each application. Instead, the breadth of applications is another source of uncertainty in the desired properties themselves. Where there is a wide difference among applications, the focus centers on blanket or limiter coolants rather than secondary coolants.

The second step is screening of the possible salt anions against the desired properties. The third step is screening of the cations. The two salt components are first screened separately to more effectively survey the vast field of alternatives. The fourth step is screening of actual salts. The final result is a narrowed field of alternatives with indications of what additional work is needed.

A good molten salt choice should satisfy many desires, listed in Table E.2-1. Unfortunately, very little definitive data exists for many salt options. The most available information is melting point via phase diagrams. The importance of the various properties depends on the application.

For primary coolant application, all of the properties in Table E.2-1 are fairly important. "Low melting point" can be very high; fission molten salt reactors used a 500°C cutoff.^(5,6) A limiter, divertor, or shield

TABLE E.2-1. SOME DESIRED SALT QUALITIES

Safety	Feasibility	Economics
Low activation	Low melting point	High possible operating temperatures
Low corrosion product transport	Low corrosion	Low material cost
Good compatibility with other possible blanket materials (e.g., lithium, ^{17}Li - ^{83}Pb , water, solid breeders, structural materials)	Good compatibility with available structural materials.	No need for secondary coolant loop or double-wall steam generator
Good tritium control	Good high temperature and radiation stability	
Low tritium inventory	Good neutronics (low neutron absorption)	
Low pressure	High neutron multiplication and/or tritium breeding	
	Good general coolant properties	

coolant would certainly require far lower operating temperatures. Assuming a steam turbine cycle, a secondary salt should also have a lower melting point to avoid freezing problems in the steam generator.^(5,6)

Neutronic, breeding and activation considerations are not relevant for a secondary loop coolant and may be less important for limiter, divertor, or shield applications than they are for a primary blanket coolant.

Compatibility desires are functions of what other materials are present. The primary coolant should be compatible with the limiter, divertor, shield, and secondary coolants. The coolant most often selected for limiter/divertor and shield has been water. Since no molten salt that provides adequate breeding by itself has been found, it is assumed that a

separate breeder would be present and the coolant salt would have to be compatible with the one chosen.

If liquid lithium is used in the blanket, there is a strong incentive to find an alternative to water as a limiter coolant. Should a salt be selected for such an application, it should be somewhat compatible with liquid lithium and/or ^{17}Li - ^{83}Pb . Finally, if a secondary loop salt coolant is needed, it should be compatible with water in the steam turbine loop and the steam generator tube material.

It should be noted that it is difficult to say which (if any) of the compatibility desires are absolute requirements. For example, even though sodium and water are clearly not compatible, LMFBR designs do use a sodium secondary loop with a steam turbine loop. However, this same example does indicate why one desires such compatibility.

Tritium is another important area. From the safety standpoint, a low tritium inventory in the molten salt would be preferable. On the other hand, many of the breeder materials may require assistance in achieving a sufficient breeding ratio. Thus, it would be desirable if a blanket coolant salt added to breeding, either due to containing lithium itself or by providing some neutron multiplication.

E.2.4 Anion Screening

Given the desired salt properties, the possible salt anions can be surveyed to select the best choices. As mentioned previously, a two stage approach will be used. First, those anions with very little chance are eliminated. Second, the remaining choices are further examined and narrowed.

The first stage of anion screening focuses on melting point and corrosion criteria. Based on information available,^(2,3,7-11) sulfates and carbonates generally have unacceptably high melting points and corrosion problems. Chlorates and silicates are either completely lacking in corrosion information or are very unstable. Although hydroxides are generally

mentioned in salt reviews, they are very corrosive.^(2,3) Salt anions that appear to offer some chance of success are nitrates/nitrites and the various halides.

Direct comparison of nitrate/nitrites as a class with the halides was not useful. Comparison among the four nuclearly stable halides (fluorine, chlorine, bromine, and iodine) was useful. Going from fluorine to iodine, thermal stability of the salts generally decreases (disadvantage), melting points decrease (advantage), and corrosion problems appear to increase (disadvantage). As indicated in the Cation Screening section, a very rough estimate of activation levels of elements indicates that iodine would be significantly worse than the other halides (from 14 MeV neutrons). Iodine is also an order of magnitude more expensive than the other halides.⁽¹²⁾ Further consideration among halides was focused on fluorine and chlorine.

Therefore, of the anions considered, the possible candidates are nitrate/nitrites, fluorides, and chlorides. This result is not surprising since the previously identified best candidate salts have been flibe (fluoride) and HTS (nitrate/nitrite).^(2,3) Among these choices, fluorides have generally better high temperature stability and lower corrosion problems than nitrates/nitrites and chlorides.⁽⁵⁾ Further comparison of specific salts containing these three anions is included in the Salt Screening section. First, appropriate cations are identified.

E.2.5 Cation Screening

Cation screening primarily focuses on neutronics, activation, and cost. A two step screening was used.

High cost eliminates the rare earths and precious metals. For all applications other than secondary coolant, high neutron absorption eliminates boron, hafnium, and cadmium.

Using data in Ref. 13, all naturally occurring, nuclearly stable elements were divided into three classes on the basis of estimated activation levels when exposed to 14 MeV neutrons (Table E.2-2). It is emphasized

TABLE E.2-2. CLASSES OF ACTIVATION LEVELS ESTIMATED FROM REFERENCE 13 FOR EXPOSURE TO 14 MeV NEUTRONS^a

Class 1	All isotopes produced (except tritium) with half-lives greater than 15 minutes reach levels below 10^5 Ci. ^b	H He	Li Be	B C	O Ne	Si
Class 2	All isotopes produced with half-lives greater than one day reach levels below 10^8 Ci. ^{c,d}	N F Mg Al P	Cl ^e Ar ^e Ca Ti V	Ga Se ^e Br Kr Sr	Ba La Pr Nd Gd ^e	Dy Ho Hf ^e Pt ^e Hg ^e Pb
Class 3	At least one isotope with half-life greater than one day would reach levels over 10^8 Ci, or all isotopes of the element are nuclearly unstable.	All other elements				

a. Based on 20 m^3 of element exposed to 5 MW/m^2 level of 14 MeV neutrons.

b. Of Class 1, only Li, Be, C (?), Si (?) are useful cations.

c. Of Class 2, only F, Cl, Br, NO_3/NO_2 are useful anions.

d. Of Class 2, only Mg, Al, Ca, Ti, V, Sr, Ba, Ta, Pb are useful cations.

e. Borderline case.

that such a rough estimate is wholly inadequate to determine actual activity levels, but it does generally indicate what elements one would like and not like in a molten salt to be used in or near the blanket. For salts used in the neutron field, Class Three (Table E.2-2) elements were not considered further.

One side issue should be mentioned. The amount of radioactivity induced in an element is roughly proportional to the amount present. Thus, whereas some elements are poor choices for use as a primary blanket salt because of high levels of activation, they may be allowable for use as a minor additive in a salt. For example, a small amount of sodium, which

leads to very high activity levels if used in bulk,⁽⁴⁾ might be used as an additive to a main salt to improve some other material property, like stability.

Among cations, lithium is a special case for two reasons. First, use of lithium in a blanket salt would add to tritium breeding while probably increasing the tritium inventory in the salt. Given the high level of difficulty in achieving an adequate tritium breeding ratio, lithium as a cation appears to be a net special advantage.

The second special issue is chemical compatibility with lithium and ^{17}Li - ^{83}Pb . As discussed in the Salt Screening section, the only halide salts that would not react with elemental lithium or ^{17}Li - ^{83}Pb are lithium halide salts. Unfortunately, among nitrate/nitrites, even $\text{LiNO}_3/\text{LiNO}_2$ would react with elemental lithium and ^{17}Li - ^{83}Pb . Even if multiple cations are used, e.g. LiF-BeF_2 , the reactivity with lithium is reduced to the extent that lithium is already present in the salt. For these two reasons, use of lithium as a salt cation appears to offer special advantages.

Based on the desired salt properties, the next most attractive cations appear to be Be, Al, and Si. Other options include Mg, Ca, Pb, V, and Ti. Beryllium and lead have the special advantage of providing neutron multiplication. For salts used as secondary loop coolant, away from neutrons, other cations are attractive. The best examples are sodium and potassium.

E.2.6 Salt Screening

Having narrowed the number of salt component options, analysis continued on specific combinations. Salt chemistry is quite complex, and insufficient data exists to choose a specific salt composition. Rather, the goal is to identify attractive basic compositions, and indicate what properties should be promoted by additional additives. For example, LiF-BeF_2 is a possible blanket coolant but one would like to find an additive (SiF_4 , AlF_3 ?) to lower the melting point and viscosity. Further screening will be divided among the three salt anions.

E.2.6.1 Fluoride Salts

Two specific generic issues for fluoride salts are chemical reactions and thermal stability. These help determine the best fluoride options for the various salt applications.

Most fluoride salts, except LiF, would react with lithium by the reaction:

where M = metal cation.

In the case of BeF_2 ,⁽¹⁾

Although the kinetics are unknown, the reaction would release significant energy. Judging from the Periodic Table, most other fluorides would be at least as bad. BeF_2 would also react with $Li_{17}Pb_{83}$,⁽¹⁾

Thus, there is a significant incentive to use LiF. Unfortunately, LiF melts at 848°C, so other fluorides would have to be added.

The more stable fluorides, LiF, BeF_2 , NaF, are inert toward water, air, CO_2 , and graphite.^(1,2,5,6) From the chemical reaction standpoint, LiF is favored, with elements nearer to lithium in the Periodic Table being next most favored.

The melting points of some fluoride salts are listed in Table E.2-3. Unfortunately, although LiF would have many attractive properties, its high

melting point eliminates it. However, given that the fission molten salt reactors have used fluoride salts with melting points as high as 500°C,^(5,6) several fluoride salts are possible candidates. As a class, the fluorides show very good thermal stability and acceptable corrosion.^(5,6) Further work is warranted for specific salt compositions.

The fission salt choice for primary coolant was LiF-BeF₂-ThF₄-UF₄ (71.5-16.0-12.0-0.5 mole %).^(5,6) The molten salt demonstration breeder called for a LiF-BeF₂ (34-66 mole %) loop next to the fissile salt. Since elements like sodium and boron were ruled out for primary fusion blanket application, the best starting point for a fusion primary blanket fluoride coolant would appear to be LiF-BeF₂. A recent fusion/fission hybrid study selected LiF-BeF₂ as the coolant.⁽¹⁵⁾ They focused on the 34-66 composition (459°C m.p.) rather than 47-53 (363°C m.p.) because of its lower viscosity.

Previous studies have included flibe-cooled (34-66 composition, LiF-BeF₂) pure fusion reactors with a separate lithium breeder.^(1,16) Primary remaining questions are (a) possible reduction of melting point and viscosity, (b) MHD assisted corrosion, (c) selection of breeder material, and (d) selection of structural material. Salt phase diagrams (also, see Table E.2-3), give reason to hope that materials could be added to LiF-BeF₂ to lower its melting point. Possibilities include AlF₃, CaF₂, PbF₂, NaF, and SiF₄.

MHD-assisted decomposition and accelerated corrosion of salts is a generic issue, which is relevant to all molten salts⁽¹⁻³⁾ and would have to be studied for any molten salt used in the magnetic field.

Since flibe contains lithium and beryllium, it supplies both some tritium production and neutron multiplication. In fact, flibe comes close to being an adequate breeder by itself.^(2,3) Thus, the required breeder performance is reduced. Separate breeder possibilities with flibe include Li and Li-Pb (if the chemical reaction is fairly benign), Li₂O and LiAlO₂ (if appropriate temperatures can be maintained given the high

TABLE E.2-3. FLUORIDE SALT MELTING POINTS

Salt	Lowest Melting Point (°C)	Composition of Lowest Melting Point Salt (mole % LiF)	Reference
LiF	848	100	9
LiF-BeF ₂	363	47	3,7
LiF-BeF ₂ -NaF	~275	--a	7
LiF-NaF	649	61	10
LiF-NaF-KF	454	46.5 ^b	10
LiF-AlF ₃	709	64.5 ^c	10
LiF-CaF ₂	765	79	10
LiF-MgF ₂	~700	35	7
LiF-SiF ₄	? ^d	?	14
LiF-PbF ₂	? ^e	?	7
NaF	995	100% NaF	9
KF	856	100% KF	9
NaF-NaBF ₄	304	60% NaF	7

- a. Several compositions.
- b. 11.5% NaF, 42.0% KF.
- c. 710°C eutectic at 85.5% LiF.
- d. SiF₄ vaporizes at -65°C.
- e. PbF₂ melts at 850°C.

operating temperature of flibe), and LiF. LiF would be used as a solid breeder⁽¹⁷⁾ and would be compatible from the accident viewpoint with molten LiF-BeF₂; presumably a thin wall would separate them.

If the flibe melting point could be reduced, then steels would be an attractive structural material. Otherwise, high temperature vanadium alloy would be a possible alternative.

No attractive low melting point (100-200°C) fluoride is known, hence no attractive fluoride salt candidates for limiter-type applications has been identified. The only possibility might be LiF-SiF_4 ; however, since SiF_4 vaporizes at -65°C ,⁽¹⁴⁾ LiF-SiF_4 may well be unstable even in the 100-200°C temperature range.

Since the tritium partial pressure over flibe is relatively high, a secondary coolant would likely be needed to limit tritium losses via the power cycle.⁽¹⁾ An attractive fluoride secondary coolant is NaF-NaBF_4 , which was selected for the fission molten salt breeder.⁽⁵⁾ It is compatible with flibe and water.

E.2.6.2 Chloride Salts

As a class, the chlorides are less stable and more corrosive than the fluorides. This is why the fission program chose fluorides over chlorides. However, chlorides do have lower melting points, which is a distinct advantage for certain applications.

Chlorides are similar to fluorides except they would be somewhat more reactive. Only LiCl would not react with lithium or $^{17}\text{Li-83Pb}$. Some chlorides would react with water. For example, AlCl_3 exothermically reacts with water to evolve HCl fumes.⁽⁹⁾ It is far less likely that a chloride salt would be compatible with lithium, $^{17}\text{Li-83Pb}$, or water.

The melting points for various chloride salts are listed in Table E.2-4. Comparison with Table E.2-3 indicates that chlorides have considerably lower melting points than fluorides. The corresponding penalty is the increase in corrosion and thermal instability.^(2,3,5,6) It is unknown whether the corrosion rates of chlorides would allow fusion blanket application.

TABLE E.2-4. CHLORIDE SALT MELTING POINTS

Salt	Lowest Melting Point (°C)	Composition of Lowest Melting Point Salt (mole % LiCl)	Reference
LiCl	610	100	9
LiCl-BeCl ₂	~300	~55	7
LiCl-AlCl ₃	103	40	8
LiCl-CaCl ₂	496	60	8
LiCl-MgCl ₂	570	60	10
LiCl-PbCl ₂	400	37	8
LiCl-SiCl ₄	? ^a	?	14
NaCl	800	100% NaCl	9
LiCl-NaCl	557	77.5	10
KCl	770	100% KCl	9
LiCl-KCl	355	58.5	9
NaCl-KCl	685	50% NaCl	9

a. SiCl₄ melts at -70°C.

As for the fluorides, a lithium-beryllium combination seems most attractive for primary coolant application. The generally lower melting points of chlorides increases the likelihood of finding a more attractive melting point salt than LiF-BeF₂. However, the lower stability raises serious questions about corrosion, thermal decomposition, and chemical reaction at blanket module temperatures. Chloride salts when heated to decomposition produce toxic fumes.⁽⁹⁾ Also unlike the fluoride case, many chlorides would not be compatible with water. Thus if a chloride were used as blanket coolant, an alternative to a water-limiter might be desired or required.

Unlike the fluorides, it may be possible to use a chloride as a coolant for limiter, shield, or divertor. A likely starting point is LiCl-AlCl_3 (103°C m.p.). Here, the reactivity with water is less of a concern since the use of a salt in this application would have the effect of replacing water, not co-existing with it. SiCl_4 additive should be more useful here than SiF_4 is for fluorides since both are covalent and SiCl_4 is more stable.⁽¹⁴⁾ (Whereas ionic LiCl is less stable than ionic LiF .)

No truly attractive chlorides suggest themselves for use as a secondary coolant. A prime reason is the reactivity with water. Some combination of the more stable chlorides, KCl , NaCl , LiCl , and BeCl_2 , may prove useful.

E.2.6.3 Nitrate/Nitrite Salts

Thermal stability and chemical reactions are serious concerns for nitrate/nitrite salts. Yet, a good salt does exist for secondary coolant loop use.

The nitrate/nitrite salt that has received the most attention as a coolant, HTS or Hitec (7% NaNO_3 -49% NaNO_2 -44% KNO_3), will react violently with lithium.^(18,19) Basically, the nitrates are excellent oxidizers and would not be compatible with lithium. Unlike LiF and LiCl , the $\text{LiNO}_3/\text{LiNO}_2$ salts appear to react exothermically with elemental lithium. Using the basic data in Ref. 20-22, one finds

$$\Delta G_{25^\circ} = -40 \text{ kcal/g-mole-Li}$$

$$\Delta H_{25^\circ} = -42 \text{ kcal/g-mole-Li}$$

$$\Delta G_{25^\circ} = -31 \text{ kcal/g-mole-Li}$$

$$\Delta H_{25^\circ} = -30 \text{ kcal/g-mole-Li}$$

Also, since the nitrate/nitrites are strong oxidizers, it is extremely unlikely they would be compatible with refractory metals like vanadium. The nitrate/nitrites are compatible with water and are generally compatible with fluoride and chloride salts.^(5,6)

Thermal decomposition appears to be more of a concern for nitrate/nitrite salts than for fluorides and chlorides. Decomposition and melting temperatures for some nitrate/nitrites are listed in Table E.2-5. Melting points are generally lower than for fluorides and chlorides.

Thermal stability is limited,^(23,25) although radiation seems not to further lower stability. Stability in an electromagnetic environment would likely be worsened, although studies would be needed to quantify the effect. Since the nitrate/nitrites are generally less stable than the halides without radiation or MHD and have marginal stability for high temperature application, the actual blanket environment with radiation and MHD may be too hostile for nitrate/nitrite salts.

The most studied salt in this class is HTS, which melts at 142°C. An early report⁽¹⁹⁾ indicated that a loop running with a maximum temperature of 593°C would experience 0.5% NaNO_2 decomposition per day via the reaction:

At that temperature, several days exposure of the salt to CO_2 , NO, steam, or air was found to significantly raise the melting point, but N_2 as a cover gas kept the melting point very close to the original. The fission breeder program anticipated using HTS with an N_2 cover gas⁽⁶⁾ as this would shift the equilibrium to the left, preventing nitrite decomposition.

Other studies⁽²⁴⁾ have indicated a different decomposition reaction for a similar salt (draw salt, 63% NaNO₃-37% KNO₃):

Estimates of the possible allowable temperature for HTS are less than or equal to 530°C.^(3,33) It should be noted that the sodium components in the salt are limiting, while KNO₃ is more stable.⁽¹⁹⁾ Available evidence suggests (see Table E.2-5) that lithium nitrate/nitrite based salts would be even less stable than sodium or potassium based salts.

The most stable nitrate/nitrite salts seem to include sodium or potassium, e.g. HTS. The lithium salts appear very unstable. Be, Pb, Ca, and Mg-based salt stability is unknown, but Periodic Table considerations strongly suggest that none of these would be more stable than HTS. Since HTS appears to have marginal stability and other nitrates/nitrites are worse, HTS appears to be the best blanket coolant in this class of salts. The major concerns are very high levels of radioactivity and thermal decomposition. For 5 year operation at 4 MW/m² neutron wall load, a HTS blanket would activate to 10⁷ Ci of Na²² (2.6 yr) and 10⁷ Ci of Ar³⁹ (269 yr), among others. However, only the argon activity seems particularly mobile. As discussed below, tritium control may not be a serious problem with HTS. Additives or a slightly different composition may increase the stability. Only steels could be used with a nitrate/nitrite salt, which is likely to be too oxidizing for vanadium applications.

For reasons outlined above, HTS also seems the most promising starting point for limiter-type applications. Although its 142°C melting point may not be low enough for such uses, lithium additions (see Table E.2-5) appear to lower the melting point further. The increased instability due to LiNO₃ or LiNO₂ could be acceptable since a salt in these applications would not tend to be operated at as high temperatures (>400°C) as a blanket coolant would.

TABLE E.2-5. NITRATE/NITRITE SALT MELTING AND DECOMPOSITION TEMPERATURES

Salt	Melting Point (°C)	Decomposition Temperature (°C)	Reference
LiNO ₃	253	253	9
LiNO ₂	220	~350	10
LiNO ₃ -LiNO ₂	196	?	8
NaNO ₃	307	~500	9
NaNO ₂	282	~300	10
NaNO ₃ -NaNO ₂ (40-60)	233	?	10
KNO ₃	337	~550	9
KNO ₂	440	~500	10
KNO ₃ -KNO ₂ (15-85)	316	?	7
LiNO ₃ -NaNO ₃ -KNO ₃ (37.5-18-44.5)	120	?	9,8
NaNO ₃ -KNO ₃ (46-54)	222	~500	9
LiNO ₃ -NaNO ₃ (50-50)	196	?	8
Li/Na-NO ₃ /NO ₂ (50-50) (40-60)	126	?	8
Li/Ca-NO ₃ /NO ₂ (80-20) (40-60)	178	?	8
NaNO ₃ -NaNO ₂ -KNO ₃ (HTS) (7-49-44)	142	≤530	3,10,14
LiNO ₂ -KNO ₂	98	?	8

For secondary coolant application, HTS again appears the best starting point among nitrate/nitrite salts. Here, however, most of HTS's disadvantages are irrelevant. Neither activation of sodium or potassium nor MHD or

radiation assisted corrosion/decomposition are relevant for a secondary coolant. Also, there is no incentive from any standpoint to use vanadium tubing on the secondary loop. HTS would be compatible with water (steam turbine loop) and several primary blanket coolants--helium, water, and most salts (most fluorides and perhaps some chlorides).

Apparently, tritium would be fairly easy to control, although definitive quantitative studies are needed. Tritium entering HTS would oxidize to T_2O and be drawn off as water.^(5,6,19) This fact led the fission molten salt program⁽⁶⁾ to select HTS for use as a loop between flibe and the steam turbine loop for the purpose of controlling tritium.

There may be advantages to mixing fluorides, chlorides and/or nitrate/nitrites, although only very limited phase diagram data exists. It may be possible to gain the advantages of each while avoiding the disadvantages. Of course, the opposite may be true. A combination of the higher stability of fluorides with the lower melting point of the nitrate/nitrites could be most useful.

E.2.7 Summary and Conclusions

A systematic examination of possible molten salts for fusion reactor application was motivated by the awareness that previous surveys had given little attention to partially breeding salts (salts containing lithium, but not capable of sufficient breeding by themselves) and applications other than primary blanket coolant. Even if a coolant salt could not adequately breed by itself, there are advantages for the coolant salt to have some lithium content anyway. These include the facts that (a) the lithium would add to the breeding from the separate breeder, (b) lithium content decreases the salt reactivity with elemental lithium or ^{17}Li - ^{83}Pb , and (c) lithium avoids the activation problems of sodium and potassium.

Possible fusion applications include primary blanket coolant, limiter, divertor or shield coolant, or secondary loop coolant. The desired salt properties differ as functions of the application.

The most attractive salt anion classes are fluorides, chlorides, and nitrate/nitrites. Attractive salt cations are Li, Be, Ca, Pb, Mg, Al, Si, Na, and K, although Na and K should not be used in a neutron environment due to activation. For the reasons above, lithium has special advantages. Beryllium-based salts have the advantages of having stability close to that of lithium and providing neutron multiplication.

For each of the salt classes (fluoride, chloride, nitrate/nitrite), the most attractive salts for various applications are listed in Table E.2-6. In many cases, there would be advantages to finding additives to improve certain salt properties. Specifics are listed in the following future work section.

TABLE E.2-6. MOST ATTRACTIVE MOLTEN SALTS FROM EACH CLASS FOR VARIOUS FUSION APPLICATIONS

	<u>Fluoride Salt</u>	<u>Chloride Salt</u>	<u>Nitrate/ Nitrite Salt</u>
Primary coolant	LiF-BeF ₂ (with additives)	LiCl-BeCl ₂ (with additives)	NaNO ₃ -NaNO ₂ -KNO ₃
Limiter, divertor, Shield coolant	None	LiCl-AlCl ₃	NaNO ₃ -NaNO ₂ -KNO ₃ (with additives)
Secondary coolant	NaF-NaBF ₄	None	NaNO ₃ -NaNO ₂ -KNO ₃

Possible primary coolant salts include LiF-BeF₂ (flibe), LiCl-BeCl₂, and NaNO₃-NaNO₂-KNO₃ (HTS). The halides offer better stability, low activation, partial breeding, neutron multiplication and lower chemical reactivity. HTS offers a lower melting point and lower viscosity. Adequate tritium control appears possible with all of these, although it would have to be demonstrated. Among the halides, LiF-BeF₂ is generally more attractive than LiCl-BeCl₂ due to better stability.

The halides would be far more compatible with elemental lithium and ^{17}Li - ^{83}Pb (potential breeders) and V-alloy (potential structure). All three salts would be compatible with steel and not offer safety problems with water.

The most promising starting composition for a primary coolant salt appears to be LiF-BeF_2 . The fission program also started here and added fissile material, while keeping melting point below 500°C . The fusion objective would be different--to try various additives to improve flibe properties, primarily lowering its high (459°C) melting point and lowering viscosity. If such a program failed to lower the melting point, this salt could probably be used only with vanadium, perhaps HT-9. HTS would be the backup salt for use with steels. Possible useful additives to flibe may include AlF_3 , PbF_2 , SiF_4 , and CaF_2 . Experiments are also needed to determine MHD effects on stability and corrosion.

An interesting salt for limiter, shield or divertor use is LiCl-AlCl_3 . The density of the eutectic (40% LiCl , $\sim 103^\circ\text{C}$ m.p.) is known, 1.6 g/cc, but heat capacity, viscosity, corrosion and thermal conductivity are not. If LiCl-AlCl_3 proved a poor choice, HTS would be the backup salt.

The most attractive choice for secondary loop coolant appears to be HTS. Experiments would be useful to verify tritium control and to find additives that could increase its thermal stability.

Experiments are needed to increase understanding and find appropriate additives to molten salts that appear the most attractive for each application. In parallel, some design work utilizing such salts could better define their usefulness.

E.3 Emission Control For HTS

The relatively high levels of radioactivity that would be produced in HTS (NaNO_3 - NaNO_2 - KNO_3) used in a fusion blanket would result in

several concerns: potential for release in accident situations, normal operational emissions, maintenance radiation exposure, and waste disposal. As part of the fusion safety input to the study, the potential feasibility and costs for control of normal operational emissions have been briefly examined. Topics included are HTS radioactivity levels, methodology approach, required reduction factors, and specific problem isotopes.

The basic conclusion is that adequate control of radioactive emissions appears feasible, although specific technology would have to be developed and demonstrated. Further, the possible costs for control systems could be significant. For purposes of the study, the following recommendations are made: (a) adequate emission control be judged potentially feasible, and thus HTS passes the spirit of the 100 Ci/day (of ^3H) screening criteria, (b) further data be collected/developed on the expected chemical processing of HTS to better define the source term, and (c) given such refinement of the source term, appropriate scores should be assigned for relevant economic and safety evaluation indices.

E.3.1 Radioactivity Levels

An interest has been expressed in the HTS salt as a primary coolant; therefore, an activation analysis for a HTS/17Li-83Pb blanket was conducted at EG&G Idaho. After later HTS design work is finished, activation analysis should be repeated for that design. The present calculation is only intended to determine a rough estimate of HTS-induced radioactivity, scaled to a STARFIRE size machine, 5 MW/m² wall loading, and 5 full years of operation. The results are listed in Table E.3-1. Only the HTS activity (from N, Na and K) in Table E.3-1 will be examined in this section.

E.3.2 Approach

Site boundary doses were calculated using a NRC standard dispersion value of 10^{-5} sec/m³, dose factors from Ref. 26-28 and the isotope production rate. The maximum individual was assumed to be exposed to all isotopes as they are produced with no delay, an upper bound calculation.

TABLE E.3-1. NON-STRUCTURAL ACTIVITY LEVELS IN A HTS^a/17Li-83Pb BLANKET AFTER FIVE FULL YEAR OPERATION AT 5 MW/m² NEUTRON WALL LOADING FOR A STARFIRE SIZE REACTOR

<u>Source Element</u>	<u>Product Isotope</u>	<u>Shutdown Activity (Ci)</u>	<u>Isotope Half Life</u>
N	¹⁴ C	1.2 x 10 ⁶	5730 yr
Na	²² Na	1.7 x 10 ⁷	2.6 yr
	²⁴ Na	7.2 x 10 ⁷	15.0 hr
K	³⁶ Cl	1.2 x 10 ⁴	3 x 10 ⁵ yr
	³⁸ Cl	1.5 x 10 ⁶	37.3 min
	³⁸ K	6.2 x 10 ⁶	7.6 min
	³⁹ Ar	1.5 x 10 ⁷	269 yr
	⁴¹ Ar	9.9 x 10 ⁶	1.83 hr
Pb	²⁰⁴ Tl	9.1 x 10 ⁴	3.77 yr
	²⁰³ Pb	1.4 x 10 ⁸	52.0 hr
	²⁰⁵ Pb	2.6 x 10 ²	1.4 x 10 ⁷ yr
	²⁰³ Hg	1.5 x 10 ⁶	46.8 d
	²⁰⁵ Hg	1.5 x 10 ⁶	5.2 min

(a) HTS is 12 a/o Na, 10 a/o K, 22 a/o N, 56 a/o O.

Exposure pathways included both external (immersion in air) and internal (inhalation and ingestion). For these rough calculations, whole body doses were used; however, the presence of some pure β -emitters like ³⁹Ar also necessitated calculation of skin dose.

The calculated doses were then compared to values of 10 mrem/yr whole body dose and 30 mrem/yr skin dose to estimate the Required Reduction Factor that would be needed for adequate reduction of emissions. Since relevant future emission standards are unknown, these reduction factors are only rough estimates, which could be over- or under-conservative.

Finally, the possible means to achieve adequate reduction were examined. Three isotopes, ^{14}C , ^{22}Na , and ^{39}Ar appear to require special control efforts. These are discussed separately.

E.3.3 Required Reduction Factors

Based on the approach outlined above, required reduction factors were calculated for HTS isotopes. The results are listed in Table E.3-2. Simple hold tanks would be adequate for ^{24}Na , ^{36}Cl , ^{38}Cl , ^{38}K , and ^{41}Ar , even if all produced isotopes were to instantaneously leave the salt as they were produced. Any holdup in the salt, as would be expected for non-noble gases, would reduce required hold times from those indicated in Table E.3-2. ^{14}C ($T_{1/2} = 5730 \text{ yr}$), ^{22}Na ($T_{1/2} = 2.6 \text{ yr}$), and ^{39}Ar ($T_{1/2} = 269 \text{ yr}$) are not adequately controlled by hold tanks. Actual control requirements for these would depend on specifics of HTS processing.

Anticipated HTS operation suggests two primary operational release paths. First, some fraction of the salt is likely to be continuously processed to control impurities and remove tritium. Any volatile isotopes would likely be released to a processing stream at this point. Unfortunately, the details of such processing are unknown. Second, to keep HTS in the liquid phase at shutdowns, water would be added. Upon startup, the water would be drawn off, potentially taking with it certain impurities and induced isotopes. Again, specifics are not known. The following sections contain specific discussions for the three problem isotopes.

E.3.3.1 Carbon-14

As seen in Table E.3-2, reduction of the order of 100 would be required if all ^{14}C tried to leave the salt. However, after one year, the ^{14}C produced would result in less than 200 wppm C in HTS, so it is not clear how much of the ^{14}C would be releasible and thus would have to be treated and captured. Although it is not known in what chemical form

TABLE E.3-2. SUMMARY OF REQUIRED REDUCTION FACTORS^a FOR HTS ISOTOPE EMISSIONS

Isotope	Required Reduction Factor	Organ	Pathway	Comments
¹⁴ C	91	Whole Body	All internal	Major concern
	26	Whole Body	Inhalation	
	0.06	Skin	External	
	0	Whole Body	External	
²² Na	2 x 10 ³	Whole Body	External	Major concern
	1 x 10 ³	Skin	External	
²⁴ Na	2 x 10 ⁷	Whole Body	External	Hold for ~15 days
	1 x 10 ⁷	Skin	External	
	9 x 10 ⁶	Whole Body	Inhalation	
³⁶ Cl	0.04	Skin	External	Minor concern
	2 x 10 ⁻⁹	Whole Body	External	
³⁸ Cl	3.8 x 10 ⁶	Whole Body	External	Hold for 14 hr
	3.7 x 10 ⁶	Skin	External	
³⁸ K	-- ^b	--	External	Hold
³⁹ Ar	40	Skin	External	Major concern
	0	Whole Body	External	
⁴¹ Ar	6.5 x 10 ⁶	Whole Body	External	Hold for 41 hr
	4.2 x 10 ⁶	Skin	External	

a. Required Reduction Factor defined as yearly dose from instantaneous release of all isotope as produced divided by 10 mrem/yr (whole body) or 30 mrem/yr (skin).

b. Not evaluated, half life of 7.6 min would make it easy to control.

carbon would be present, an upper bound estimate of control problems is obtained by assuming that the form would be CO₂. The high mobility of CO₂ compared with other forms makes this a worst case analysis.

Feasibility of a 100 reduction can apparently be assumed. However, costs could be high. NEA analysis⁽²⁸⁾ postulated decontamination of

CO₂ by a factor of 100 at a cost of 240K\$/GWe-yr for a heavy water reactor and 16K\$/GWe-yr for a fuel reprocessing plant. These values correspond to 0.8 - 1.1K\$/Ci removed. PWR and BWR control estimates⁽³⁸⁾ correspond to 16 - 21K\$/Ci. Another group estimated costs at ~5K\$/Ci.⁽²⁹⁾ However, all these estimates were for relatively low amounts of ¹⁴C, all in the form of CO₂ gas. The highest flow rate was for heavy water reactors (300 Ci/GWe-yr, 0.8K\$/Ci). An HTS reactor would produce ~2.4 x 10⁵ Ci/yr. Scaling from 300 Ci/GWe-yr to 2.4 x 10⁵ Ci/yr gives costs of ~200M\$/yr, if economies of scale were not effective. Since this estimate is based on all ¹⁴C produced being mobile as CO₂, actual releases from the salt and associated costs would be expected to be less. Thus 200M\$/yr is an approximate upper bound, unless future site boundary doses limits were more restrictive than 10 mrem/yr for whole body. Further processing analysis would be required to improve the estimate.

Relevant CO₂ control technology involves precipitation and storage as CaCO₃, either from (a) molecular sieves or (b) caustic scrubbing and absorption in NaOH and precipitated with Ca(OH)₂.^(28,30) The carbonate would typically be enclosed in cement and buried.

E.3.3.2 Sodium-22

Examination of Table E.3-2 indicates that very little of the ²²Na (or ²⁴Na) that is produced could be allowed to be released. The reduction factor is equivalent to containing 99.95% of the activated sodium. Unlike tritium, ¹⁴C and ³⁹Ar, however, sodium is far less mobile and would not normally be expected to be in gaseous form. In fact, no ²²Na or ²⁴Na is typically detected as being emitted from the EBR-II sodium cooled reactor.^(30,31) Thus unless HTS processing results in significantly increased sodium mobility, adequate containment of ²²Na and ²⁴Na under normal operation should be feasible. However, until HTS processing details are better known, it will not be clear that sodium containment will be as successful as for EBR-II.

One possible mechanism for operational sodium release is through the water drawn off the salt while bringing the salt/water mixture from shut-down to operating temperatures. Large amounts of water are likely required with associated concerns about sodium dissolved in the water being released.

Although ^{22}Na may not be an operational problem, it is clear that its half-life (2.6 yr) would give maintenance and short-term disposal problems. Further, if a mechanism for sodium mobilization in an accident were found, it would constitute a major accident dose contribution.

E.3.3.3 Argon-39

The specific chemical processing details for the salt are less important for the case of ^{39}Ar , being a noble gas. The analogous case of ^{85}Kr in fuel reprocessing plants is analyzed by assuming greater than 99% release unless specific krypton control technology is used.⁽²⁸⁾ The following discussion assumes that argon control technology will be similar to that for krypton. Actually, argon may be slightly more difficult to control since (a) cryogenic distillation is the likely capture technology for ^{85}Kr ^(28,33) and (b) the argon boiling point (-186°C) is lower than krypton (-152°C).

As seen in Table E.3-2, a reduction of at least a factor of 40 (97.5% removal efficiency) is necessary for site boundary skin dose from ^{39}Ar to be adequately reduced. Currently, the Idaho Chemical Processing Plant is removing krypton and xenon from the waste stream for economically attractive resale. The current removal efficiency is about 75% with approximately 90% achievable with their installed equipment, which was sized and designed for economic, not dose reduction, purposes.⁽³¹⁻³³⁾ This equipment utilizes cryogenic distillation capture followed by retention and shipping of the krypton and xenon in pressurized cylinders. The EPA believes 86-90% removal of ^{85}Kr is achievable.⁽³⁰⁾ A decontamination factor of 100 (99% removal) was taken as feasible in NEA analysis, using

cryogenic distillation capture and retention by adsorption in a zeolite bed in pressurized cylinders.⁽²⁸⁾

Cryogenic distillation technology to achieve a factor of 100 reduction for ^{85}Kr was calculated⁽²⁸⁾ as costing 38K\$/GWe-yr for a processing rate of 3.8×10^5 Ci/GWe-yr (0.1 \$/Ci). For the HTS case, one would need to capture 3×10^6 Ci/yr of ^{39}Ar . Thus anticipated costs are 0.1 - 1M\$/yr. The certainty of this cost estimate is likely to be far better than for ^{14}C because (a) there is less scale-up from the fission technology, and (b) the chemical form for argon and krypton is known to be the same, whereas the chemical form and amount of ^{14}C from HTS is not known.

E.3.4 Summary and Conclusions

The issue of adequate control of emissions from a HTS cooled fusion reactor has been examined and summarized in Table E.3-3. Given the state-of-knowledge, no certain insurmountable problems can be identified. Problem isotopes other than tritium are ^{14}C , ^{22}Na , and ^{39}Ar . Since the details of HTS processing are unknown, a definitive statement on feasibility and costs cannot be made at present.

Extrapolation of fission technology for ^{14}C , ^{22}Na , and ^{85}Kr offers the hope of keeping HTS-related emissions to acceptable levels. If future requirements are significantly more strict than those of today, adequate control may not be feasible. However, for purposes of the BCSS, it should be assumed that control is feasible; that is, HTS passes the spirit of the 100 Ci/day emission criterion for tritium.

The possible costs for emission control range from 10^5 to 10^9 \$/yr. The lower bound is determined by relatively good estimates for ^{39}Ar control based on ^{85}Kr technology. The upper bound represents a conservative linear scaling of ^{14}C removal costs assuming all ^{14}C produced in HTS had to be processed as CO_2 with no benefit of economy of

TABLE E.3-3. SUMMARY OF MAJOR CONCERNS FOR HTS EMISSIONS

Isotope	Required Reduction ^a Factor	Potential ^b Cost (\$/yr)	Comments
¹⁴ C	~90	2×10^8	Actual costs likely to be far lower due to (a) most ¹⁴ C staying in salt, (b) better scaling than linear for ¹⁴ C treatment rate, (c) chemical form may be easier to collect than CO ₂
²² Na	$\sim 2 \times 10^3$	Not estimated	²² Na will follow normal Na in processing. Actual releases would be caused by specifics of yet unspecified HTS processing
³⁹ Ar	~40	10^5 - 10^6	All ³⁹ Ar likely to leave the salt (unlike ¹⁴ C, ²² Na) so this could actually represent the worst case isotope.

a. Required Reduction Factor defined as yearly dose from instantaneous release of all isotope produced divided by 10 mrem/yr (whole body) or 30 mrem/yr (skin).

b. Based on linear scaling of fission control technology from Ref. 28. Since isotopes other than noble gases will not all leave the salt, non-argon costs are likely to be over-estimates. Costs given for capture, retention and storage.

scale compared to fission processing rates of ¹⁴C. Future specification of HTS operational chemistry would be needed to reduce the cost uncertainty.

Given available knowledge, it is clear that achieving adequate HTS emission control would be a major effort. For purposes of the current study, an HTS blanket would probably get a score near zero on safety evaluation index 9 (radioactive emission rate). Thus HTS would have to exhibit advantages elsewhere for it to score well in the safety evaluation. Other issues related to HTS activation like accidental radioactivity releases, maintenance, and waste disposal have not yet been examined.

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