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TOKAMAK POWER SYSTEMS STUDIES - FY 1985

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SUMMARY

Scope of Work

The Tokamak Power System Studies (TPSS) at ANL in FY-1985 were devoted to exploring innovative design concepts which have the potential for making substantial improvements in the tokamak as a commercial power reactor. Major objectives of this work included improved reactor economics, improved environmental and safety features, and the exploration of a wide range of reactor plant outputs with emphasis on reduced plant sizes compared to STARFIRE. The activities concentrated on three areas: plasma engineering, impurity control, and blanket/first wall/shield technology.

This year's activity in plasma engineering focused on two key physics areas which are central to improving tokamak reactor performance. The first issue is the advantages and disadvantages of operation in the second stability regime. The other area is an assessment of steady-state current drive with the fast wave. Sensitivity studies on plasma temperature, current drive power, power level, wall load, magnetic field, etc. led to the development of a reference reactor concept which served as a model for the impurity control and blanket technology studies.

Our TPSS effort in impurity control focused on innovative systems that could result in substantial improvements in tokamak reactor attractiveness. The main concepts examined were non-water cooled, self-pumped first walls and limiters. Conventional pumped limiter operation at low edge temperatures and a copper-lithium alloy coated limiter were also studied. Supporting work in the areas of 1-D plasma transport calculations, neutronics, 2-D impurity transport and sputtering computations of He saturated materials were also performed. An initial engineering design, plasma engineering analysis, and materials related analysis were carried out for the self-pumped first wall and limiter systems.

The blanket/first wall/shield design activity focused on the development of improved concepts that can be incorporated into the high β reference reactor concept developed in the ANL TPSS effort. Generic blanket concepts considered in this study were based on the leading candidates identified in the Blanket Comparison and Selection Study (BCSS). These included the leading self-cooled liquid metal concept (Li/Li/V) and the leading solid breeder concept (He-cooled Li_2O). In addition, an innovative concept based on a self-

cooled Flibe design was developed and evaluated. Emphasis was placed on design simplicity, improved safety, reduced activation and high performance. Several special issues, e.g., tritium containment and recovery, inherent safety, blanket energy multiplication, electrical insulators and the possible use of organic coolants (carried out by UCLA) were considered in detail to support the blanket development issue. Other major considerations in blanket/first wall/shield development related to reduced reactor size and power, improved mass power density, high reliability and long component lifetime.

Technical Results

Recent MHD stability theory has indicated that the tokamak β may be increased via two routes -- increased shaping of the plasma cross section (elongation, indentation) and adjustments to the current density (safety factor) profile. A major experiment at the Princeton Plasma Physics Laboratory (PPPL's PBX device) will test some aspects of this theory. In certain regions of parameter space β may exceed the Sykes-Troyon value by very large factors, and these equilibria are accordingly said to be in the "second regime of stability". Our assessment of second stability tokamak theory identified two hallmarks which are particularly attractive for enhancing tokamak reactor prospects.

1. High Aspect Ratio is preferred in order to attain the second stability regime; this is expected to simplify reactor maintenance and also it permits the use of fast wave current drive (FWCD) at low frequencies ($\omega \sim 2 \Omega_D$).
2. Low Toroidal Current is favorable to the second stability regime; this is beneficial to many subsystems. The EF coil system will store an order of magnitude less energy compared to first stability equilibria, while the TF coil needs less structure to withstand overturning moments. In addition, the plasma inductive energy released in disruptions is much lower. Finally, the lower plasma current reduces the circulating power for FWCD and the "hollow" current density profile associated with low toroidal current is also favorable to FWCD since the current density peaks near the low electron density edge region.

An obvious benefit of the second stability regime is lower toroidal magnetic field, compared to conventional, Troyon limits. For $\beta \gtrsim 0.2$, the maximum field at the TF coil is $B_M \sim 6 - 7T$, and the TF stored energy is ~ 5 GJ, about 10% of the STARFIRE value. Some aspects of TF magnet design have been examined by LLNL.

The penalties paid for achieving the second stability regime are increased shaping of the plasma cross section and current density profile control. However, recent work at PPPL (J. Manickam, et al.) has shown that $\beta \gtrsim 0.2$ is stable with no indentation, which eliminates the need for a midplane pusher coil. Stable operation with low current ($I_p < 5$ MA) and mild triangularity may be achievable with elongations of $\kappa \gtrsim 2.0$.

The fast wave current drive (FWCD) study was initiated in recognition of the difficulty of driving currents near the high density, magnetic axis with lower hybrid waves. The better accessibility of FWCD motivated ray tracing studies (in cooperation with D. Ignat, PPPL) in two dimensions. A coupled ray-tracing/MHD calculation has been developed which, for the first time, enables the calculation of the actual 2-D equilibrium current density generated by steady-state wave drivers. Preliminary calculations with narrow spectra (a single $n_{||}$) demonstrate that hollow current equilibria (~ 4 MA) with $\beta > 0.10$ are possible with 60 MW of rf power. The low frequency FWCD technique appears to avoid parasitic alpha damping, and the same system can be tuned slightly during startup to effect ion heating to ignition. It is planned to experimentally study FWCD on PLT.

The self-pumped first wall/limiter concept uses vanadium or possibly other materials (Ni, Fe, Nb, Ta) to selectively trap impinging helium from the plasma on appropriate surfaces. No vacuum ducts or pumps are used (except for a very small startup system). Vanadium is added to the surface at an average rate of 3.3 times the α -particle production rate. Two embodiments of this concept appear most promising, a first wall limiter and a slot limiter; both are lithium cooled. (A helium cooled system was also assessed but appears to present difficult heat removal problems.)

The first wall/limiter concept combines the functions of first wall and limiter into a single wall, shaped to conform to the plasma edge flux. The first wall is made of vanadium structural alloy. Helium trapping occurs on a 300 m^2 surface to which pure vanadium is added at a rate of 1.5 mm/yr (for a ~ 1000 MW fusion power reactor). Vanadium can be added to the plasma scrapeoff

zone and/or edge regions via pellet injection, without apparent deleterious effects. (This is not the case for tantalum, however, which was found to cause too much radiation cooling.) An edge temperature less than 50 eV and possibly much lower is predicted by 1-D transport code analysis. Sputtering of the surface is therefore low. Based on estimates of heat flux, thermal transport and He retention properties, a total surface growth of > 1.5 cm is possible, permitting a nominal 10 year lifetime. A structure combining the functions of the limiter, first wall, blanket and shield, with an integrated lithium coolant system, is possible with this concept.

The slot self-pumped limiter consists of a ~ 60 m² front face, two leading edges and a slot region. The limiter is toroidally continuous, and can be located at various poloidal locations, e.g., the outboard midplane. The front face is designed to accept the main heat load while helium trapping is done in the slot region. A lithium cooled, tantalum structural limiter was designed. The design uses insulators to alleviate thermal stresses on the front surface and can accept heat loads up to ~ 4 MW/m². Trapping materials of vanadium, nickel or iron appear suitable. The trapping material is added to the slot plasma where it is transported to the slot surfaces. A potential advantage of the slot system is that the main heat load is separated from the trapping sites. Also, the material injection is performed away from the main plasma. A 10 year lifetime is possible with this system by adding a total of ~ 6 cm of material to the slot surfaces.

In other work, a Cu-Li coated pumped limiter was found to have low net erosion rates over a fairly broad edge temperature range of 10 $\sim$ 200 eV. The low erosion rate is a result of the lower sputtering of copper due to the presence of lithium and the high redeposition fraction of the sputtered copper. A 2-D Monte Carlo impurity transport code and a Monte Carlo sputtering code have been developed for use in impurity control analysis.

The Li/Li/V system was developed as the reference self-cooled liquid metal blanket concept with a ferritic steel structure and LiPb breeder as backup options. The MHD effects associated with self-cooled liquid metal blanket/ first wall systems are substantially reduced by the lower magnetic fields resulting from higher β plasmas, lower neutron wall loadings resulting from reduced power output, and smaller reactor size. Therefore, substantially

improved performance characteristics of self-cooled liquid metal blanket concepts are achievable compared to the BCSS guidelines because of the relaxed design constraints.

Electrical insulators have been incorporated into the blanket to further reduce operating pressures. Use of flow tailoring of the liquid metal to improve first wall cooling capability may also be feasible. For the somewhat lower neutron wall loadings corresponding to reactor outputs of 600-800 MWe, a partially permanent blanket has been developed that could last the entire reactor lifetime. Only a relatively thin blanket section near the first wall needs to be routinely replaced, thus minimizing waste management. A high temperature shield, utilizing lithium coolant, maximizes energy recovery and eliminates the safety concern associated with a water-cooled shield. The vanadium-alloy structure potentially provides long lifetime and higher temperature operation. Placement of manifolds in the core of the higher aspect ratio torus should provide a simpler design, better maintenance access, improved safety and reduced shield costs.

Effort on the helium-cooled Li_2O blanket concept has primarily involved attempts to improve the economic performance of this blanket which was identified as the most serious limitation for this concept in the BCSS. Potential improvements for this concept related to (1) the use of vanadium alloy as the structure to permit higher helium outlet temperatures, (2) containment of Li_2O in a sealed tube to allow higher operating temperatures, (3) elimination of one tritium recovery system by recovering tritium directly from the helium coolant, and (4) use of beryllium for energy multiplication to improve the economics.

The two key feasibility issues of this He/ Li_2O blanket relate to the use of the vanadium alloy structure and excessive hydrogen (D) permeation through the first wall from the helium into the plasma chamber. To prevent oxidation of vanadium, the oxygen potential in the system must be maintained at very low levels. Therefore, tritium will be present in the reduced form, i.e., T_2 or HT. Tritium containment and recovery become much more difficult without the ability to oxidize the tritium and without use of oxide barriers. Addition of hydrogen or deuterium appears necessary to maintain acceptable tritium inventories in the ceramic breeder. Preventing excessive hydrogen/ deuterium

permeation through the first wall becomes a major constraint which requires use of an effective hydrogen barrier. These key issues have not been completely resolved.

An innovative blanket concept which utilizes the molten salt Flibe as both breeder and coolant has been evaluated. Key features of this concept relate to (1) design simplicity since Flibe serves as both breeder and coolant similar to the self-cooled liquid metal concepts, (2) low operating pressures for the Flibe, (3) inertness to water and air, and (4) low activation. Vanadium base alloys are considered as the only candidate structural material because of the relatively high melting temperature (363°C) of Flibe, which requires relatively high operating temperatures. Other critical issues associated with this concept relate to startup problems associated with the high melting temperature, tritium containment/recovery, marginal tritium breeding, and compatibility with the structure. Analyses indicate that both the tritium containment/ recovery issue and the tritium breeding issue may be resolved by addition of beryllium and/or lithium to the Flibe. These stabilizing elements should also improve the compatibility issue.

Analyses of afterheat have been conducted for the Li/Li/V and the Flibe/Flibe/V concepts to evaluate the inherent safety features of these concepts. For neutron wall loadings of at least $2\text{--}4\text{ MW/m}^2$, and conservative assumptions on heat losses, these concepts are considered to be inherently safe; i.e., even with only passive cooling excessive temperatures for the structure are not reached under postulated accident conditions.

Conclusions

Low current tokamak equilibria have been found stable at $\beta \gtrsim 0.20$ to all ideal modes, without indentation. At these values of beta an INTOR-sized tokamak could provide a range of reactor performance; at low wall loads a unit producing $\sim 300\text{ MW(e)}$ could serve as a minimum cost reactor, whereas higher wall loads ($> 5\text{ MW/m}^2$) would evince economies of scale and provide the lowest costs of energy. Self-consistent calculations of the equilibrium, generated with fast wave current drive, suggest that the rf power required will be acceptable, $\sim 80\text{ MW}$. This particular wave appears to be the best candidate for steady-state reactors. We expect the EF and TF coil systems for this highbeta reactor to be substantially reduced in size from those previously envisioned.

Because it eliminates almost all vacuum ducts and much of the penetration shielding and it simplifies maintenance, the self-pumped first wall/limiter concept appears promising for commercial reactors. The potential improvements are a cost savings of the order of \$100M, a doubling of mass power density, a long (10 year) limiter lifetime and a reduction in tritium processing and inventory.

Liquid lithium as breeder and coolant with a vanadium alloy structure can provide a simple, high performance blanket for the high- β reactors. Analyses indicate that this concept can be inherently safe with minimal waste management requirements because of the potentially long lifetime and low activation characteristics of a vanadium structure.

In general, the ANL TPSS effort has demonstrated that substantial improvements can be made in the tokamak reactor concept in terms of reduced reactor sizes, improved power density (and hence lower costs) and improved safety and environmental features.

TPSS Activities for FY-1986

Regarding the physics of the second stability regime there are concerns regarding resistive (tearing) modes. Although the safety factor is monotonic in poloidal flux, there are very sharp gradients in the current density, which may cause problems. Likewise, ideal stability at high beta requires a close fitting conducting first wall; the engineering implications of such a component will be examined in FY-1986.

Studies of the EF system are required to optimize its design with respect to plasma shaping, stored energy, number of coils, coil placement and maintenance, etc.

The FWCD calculation of 2D equilibria must be generalized to arbitrary $n_{||}$ spectra. The goal here is to match in detail the second stability current profiles which have been tested for stability by the numerical codes at PPPL. This will quantify the wave driver system requirements. Also, a comparison of ridged waveguides and loop antennas would be a worthwhile extension of this study of FWCD.

A self-consistent self-pumped first wall/slot limiter concept with a high β /current drive concept should be developed. Other specific issues needing analysis are 2-D impurity transport in the limiter scrapeoff zone and further engineering/materials assessments of the self-pumped concept.

The blanket/first wall/shield activity for FY 1986 will focus on further development of the self-cooled lithium blanket with the vanadium alloy structure. Use of electrically insulated walls, both laminated and coated, to minimize MHD effects will be evaluated in more detail. Effort will also be placed on evaluation of MHD flow tailoring. Further evaluation will be performed on issues associated with the use of a vanadium structure, e.g., cost, use of a bimetallic heat transport system, and the potential for radiation embrittlement. An integrated power conversion and maintenance concept will also be developed. The hot shield concept, increased blanket energy multiplication and shield optimization will also be studied. Low activation ferritic steels will be considered as a backup structure and Flibe as the backup breeder/coolant. Research progress on solid breeders will continue to be monitored.

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CHAPTER 1

INTRODUCTION

CHAPTER 1

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1. INTRODUCTION

This chapter provides a brief background of the Tokamak Power Systems Study as well as a general discussion of the basic goals and new directions for tokamak reactor studies which served as the overall framework for the work presented in this report.

1.1 Background

The last major tokamak commercial reactor design study in the U.S. was STARFIRE⁽¹⁾ which was conducted in 1979-1980. Since then a number of developments have occurred both in the national magnetic fusion energy program (e.g., new physics and technology developments and an emphasis on developing substantially improved fusion concepts) and outside the program (e.g., utility concerns about large capital investments) which suggest that further studies of the tokamak as a power reactor are required. The U.S. Department of Energy/Office of Fusion Energy (OFE) commissioned a variety of tokamak reactor scoping studies in FY-1985 to explore a possible basis for more in-depth conceptual designs to be undertaken in the future. This activity was called the Tokamak Power Systems Study (TPSS) and included ANL, the Fusion Engineering Design Center at ORNL, UCLA, MIT, LANL, GA Technologies, TRW and RPI.

Over the last 15 years there have been a number of studies which have explored the application of the tokamak concept^(2,3,4) as physics/ignition test reactors, engineering/materials test reactors, demonstration plants and commercial power plants.⁽⁵⁾ The earlier (1970-1975) commercial reactor studies were directed at developing a basic understanding of the general features of tokamak reactors. A second series of studies (1975-1979) began to incorporate several features aimed at improving the attractiveness of the tokamak as a power reactor. These features included non-circular cross sections for higher β , RF heating, higher efficiency power conversion systems, improved maintenance, and (in the case of NUWMAK)⁽⁶⁾ smaller plant output and significantly reduced reactor size. These efforts culminated in the STARFIRE study in 1980.

While STARFIRE incorporated many features considered to be necessary or at least desirable for an attractive commercial tokamak reactor concept, it was only a step in that direction. Shortcomings of the STARFIRE design included:

- a relatively large capital cost investment,
- a modest β which resulted in moderate to large magnetic fields and a moderate mass and size,
- an RF current drive system that may likely result in excessive recirculating power,
- reliability concerns regarding water-cooled blankets that require a large number of high pressure tubes,
- feasibility issues of pumped limiters,
- use of a structural material that will not permit shallow land waste burial, and
- substantial and expensive shielding requirements driven in part by vacuum pumping ports.

1.2 Goals for Tokamak Reactor Studies

These considerations and others^(7,8,9) have resulted in the following as goals/new directions for future tokamak reactor studies:

- Reduced Output per Tokamak Reactor. The target value is about 400 MW(e) to 800 MW(e) compared to STARFIRE's value of 1200 MW(e). It is anticipated that two to four reactors could be located at each plant site, thus possibly sharing support facilities and balance-of-plant equipment.
- Reduced Reactor Mass per Output Power. The relevant figure-of-merit is the mass power density⁽¹⁰⁾ where the mass includes the blanket, shielding, magnets and torus structure. The target value is 100 kW(e)/tonne or more which represents a factor of 2-3 improvement over STARFIRE.

- Emphasis on Fusion's Inherent Safety Features. The goal is to continue to identify passive means for rapid plasma shutdown and first wall/blanket cooling so that no operator-initiated action is required. In practice this may require limiting the first wall neutron load to 5 - 10 MW/m². In addition, materials with low neutron activation levels several years after shutdown are desirable to minimize radioactive waste concerns.
- Steady-State Operating Mode. Steady-state operation provides a number of cost advantages and design simplifications.(1,11,12)
- Design Simplification. There are a number of design simplification ideas which need to be considered such as reduced magnetic fields and plasma currents, combined plasma current drive and heating systems, reduced vacuum pumping and shielding requirements, simplified blanket designs, etc.
- Overall Plant Efficiency. The goal is to achieve a net plant electrical output which is at least 30% of the total thermal power. Major issues include improved plasma current drive concepts (by improved current drive efficiency and/or reduced plasma current) and improved blanket power conversion systems by increased blanket energy multiplication and/or more advanced balance-of-plant power conversion concepts.
- Minimize On-Site Fabrication/Construction. The goal is to be able to factory produce and ship all components of the tokamak.

1.3 New Directions for Tokamak Reactors

The previous sections identified some desired improvements in STARFIRE and some basic goals which served as a basis for several new ideas which were looked at in this year's TPSS activity at ANL. This section summarizes several of these new directions.

1.3.1 Smaller Output per Reactor

A primary concern of the electric power industry is the financial investment required for building large [> 1000 MW(e)] plants, particularly large nuclear plants. Larger plants also imply longer construction times (and hence larger indirect capital costs) and reduced flexibility for meeting uncertain electric power demands. Thus, there is renewed interest in the nuclear industry in examining reactors with electric outputs of 100 to 500 MW(e).^(13,14,15)

There is no physical or technological reason why tokamak reactors cannot be configured at these power levels. STARFIRE's power output was selected based on judgments regarding "typical" nuclear power plant sizes in the middle of the next century. These judgments may eventually be correct but there are nevertheless valid reasons today for examining smaller reactor outputs.

It is important to note, however, that there are important economies of scale that must be considered. Previous system studies^(16,17) have shown that there are relatively small penalties [e.g. 10 to 15% in terms of cost-of-energy (COE)] for reducing the plant from ~ 2000 MW(e) to 1000 MW(e). Further reductions from 1000 MW(e) to 400 MW(e) would increase the COE by about 30 to 45%^(7,17) while reductions from 1000 MW(e) to ~ 200 MW(e) would almost double the COE.⁽¹⁷⁾ These trends are similar to those found for fission reactors.

The development of 100 MW(e) tokamak reactors is probably unrealistic given the requirements for ignited or at least high Q operation, shielding and low recirculating power. However, operation at net electrical power outputs of 300 to 400 MW(e) is certainly feasible.⁽¹¹⁾ The key question is whether there are compensating factors for the higher costs due to economy of scale effects. Advocates of the smaller reactors argue that the economy of scale penalty can be offset by reduced engineering costs (smaller reactors could be modularized and replicated), reduced on-site construction time (factory built), and reduced containment and safety systems. In fact, some argue that developing truly passive or inherent safety systems is the key to improved fission and fusion economics. These potential benefits would substantially reduce the indirect capital costs of a fusion reactor. Our preliminary judgment is that tokamak reactors in the range of 600 to 800 MW(e) represent a good balance between achieving most of the benefits of economy of scale while still offering reasonable plant unit electrical outputs.

1.3.2 Improved Mass Utilization

It is clear that for fusion to compete with fission, a reduction in the direct capital cost of the tokamak is necessary.^(7,8) One of the keys will be improved mass power density, i.e., more power output per unit mass where the mass includes the first wall/blanket, magnets, shielding, and reactor support structure.⁽¹⁰⁾

As a reference point, STARFIRE has a fusion power core mass of about 25,000 tonnes and a fusion core volume of 8000 m³ with a net electrical output of 1200 MW(e) and a total thermal output of 4000 MW. Thus, the mass power density is about 50 kW(e)/tonne and the average fusion core power density is about 0.5 MW(th)/m³.

Recent systems studies results^(7,8) indicate that there is a significant reduction in direct capital cost and COE (~ 40%) if the power density is increased to about 2 MW(th)/m³. This corresponds to a reactor mass for STARFIRE of < 10,000 tonnes or about 100 kW(e)/tonne. Thus, one goal of further tokamak reactor studies is to increase the mass power density relative to STARFIRE by a factor of at least two. When one considers appropriate unit material and fabrication costs, it appears this could make fusion economically competitive with fission.^(7,9)

A key question, then, is what improvements in tokamak reactors might result in a higher mass power density. The dominant components in tokamak reactors, and in most fusion concepts, are the magnets and shielding. Tokamak improvements that could reduce magnet mass include:

- Achieving higher β (discussed further in Sec. 1.3.4 and Chapter 2) and using the higher β to reduce the magnetic field. For example, a β of about 20 to 25% would allow one to reduce STARFIRE's maximum magnetic field from 11 T to 6-7 T. Since magnet forces are proportional to B^2 , this would result in a substantial reduction in magnet mass.
- Reducing the bore of superconducting TF coils by wrapping them more tightly around the blanket/shield structure. This was a key feature of NUWMAK. This would be particularly attractive if ripple constraints could be relaxed and if the magnets are removed with the blanket/shield sectors for maintenance operations.

- Improved magnet design particularly with respect to operating at higher current density in the coils.

Opportunities for improvement in shielding include the following:

- Reduced vacuum pumping ducts (discussed further under impurity control) resulting in reduced penetration shielding. Penetration shielding can be comparable to bulk shielding in some designs.
- Improved (i.e., higher radiation dose) insulators in magnets, thus requiring less shielding.
- More reliance on remote maintenance with reductions in personnel access requirements in the reactor building.
- Use of less expensive shielding material. For example, the ANL DEMO study⁽¹⁰⁾ developed a relatively low-cost outboard shield of concrete, B_4C , lead and FE1422 structural material.

This last point is not a reduction in mass but a substitution of a less expensive material. This raises an important point: the use of simple figures-of-merit such as mass power density are useful but limited. They are only guidelines that can sometimes be misleading. For example, one should not increase the fusion core power density or increase the mass power density without considering factors such as:

- the material and component fabrication costs,
- possible compromising of passive or inherent safety features,
- possible increases in failure rates,
- possible reduction in access space for maintenance operations, and
- the need for very specialized components, such as high heat flux systems which may be quite limited with respect to material and design choices (e.g., only water-cooled copper).

1.3.3 Emphasis on Passive Safety

Fusion power has always been viewed as offering significant safety features. To exploit this feature, it is very desirable to be able to argue that a fusion reactor possesses inherent safety.⁽¹⁸⁾ The goal of inherent safety is to ensure that no major accident can occur that would have unacceptable public health consequences. More specifically, it means a facility designed with passive safety features such that the public is protected from any acute fatalities under all credible accidental circumstances. The concept of inherent safety can be extended to also include a facility designed with passive features such that no credible set of accidental circumstances could lead to severe economic consequences.

The issues regarding passive removal of the afterheat in the first wall and blanket are dependent on material choices and specific designs. However, it is clear that limitations will be placed on the first wall neutron power loadings, and hence the first wall and blanket power densities, in order to assure passive energy removal. Preliminary estimates in this report suggest that the wall loadings should be limited to 5 to 10 MW/m². Fortunately, these levels of wall loadings appear to be sufficient to obtain the desired level of mass power density.

1.3.4 Achieving Higher β

Perhaps the most important new development in tokamak physics is the potential for achieving higher β . The most straightforward approach to increasing β follows from a scaling relationship that seems to be consistent with most experimental results,⁽¹⁹⁾

$$\beta \propto \frac{I_p}{aB_T}$$

where I_p is the plasma current, a is the minor radius and B_T is the toroidal field. This would suggest that increasing the plasma current would increase β . There are, however, a number of significant disadvantages to increasing the plasma current:

- Larger I_p will make steady-state operation via current drive either infeasible or much less attractive.

- Larger I_p will increase disruption concerns.
- Larger I_p will increase plasma control, startup and shutdown concerns.

It is not likely that increasing the plasma current will result in attractive commercial fusion reactor concepts.

Another approach, and the one most likely to result in a more attractive tokamak reactor if it works, is based on the second stability regime.^(19,20) The approach may involve indenting the inboard side of the tokamak cross-section to form so-called "bean shaped" plasmas. Typical indentations range from 0 to 30% of the plasma radius. With such indentations and careful control of the plasma current profile (a possible disadvantage if too stringent), one can obtain stability against ideal ballooning modes and the internal kink mode. Stability against external kink modes will rely on wall stabilization. Beta values of 20% or greater may be achievable.

A major advantage of the second stability regime is that it does not require larger plasma currents and tends to favor larger aspect ratios. The lower plasma current would favor RF current drive and perhaps RF current drive could facilitate the control of the plasma current profile which may be necessary to achieve the second stability regime.

Some reactor aspects of bean-shaped plasmas have been examined.⁽²¹⁾ It is clear that a major issue is the poloidal field requirements to produce the bean-shaped plasmas, particularly the need for a "pusher" coil located at the inboard/mid-plane of the tokamak. Recent results⁽²²⁾ indicate that the second stability regime can be obtained without any indentation. It has also been suggested⁽²³⁾ that large aspect ratio ($A \sim 9$) tokamaks can operate in the second stability regime with circular plasma cross-sections.

1.4 TPSS Effort at ANL

In order to support the TPSS effort, ANL selected three major areas of study during FY-1985 which included (1) plasma engineering considerations of achieving higher β and steady-state current drive, (2) advanced impurity control concepts and (3) blanket concepts and tritium containment/processing.

The studies of higher β have concentrated on applying ideas of the second stability regime. Design implications include optimizing at lower magnetic

fields and higher aspect ratios as well as using RF current drive methods to achieve the necessary control of plasma current profiles. The studies of current drive have emphasized the application of fast waves (or high phase speed magnetosonic waves) and the relationship between high β plasma configurations and steady-state current drive. These results are presented in Chapter 2.

The impurity control studies have emphasized innovative design solutions to key problems including the application of "self-pumping" using either the entire first wall or the region near the limiters,⁽²⁴⁾ the use of advanced materials which produce self-sustaining low-Z surfaces,^(25,26) and the development of designs using non-water coolants. Additional analysis has also been carried out on plasma shutdown modes and low edge temperature regimes for pumped limiters. These results are reported in Chapter 3.

The blanket design and tritium control tasks (reported in Chapter 4) followed in the footsteps of the Blanket Comparison and Selection Study (BCSS).⁽²⁷⁾ The BCSS resulted in a high rating for liquid metal (particularly lithium) cooled blankets employing a vanadium alloy structure. The liquid metal blanket effort in TPSS concentrated on improved/simplified designs making use of lower magnetic fields (due to higher β), somewhat reduced neutron fluxes (due to decreased reactor output power) and the use of insulators in the blanket ducts to reduce MHD-generated pressures. In addition, a self-cooled blanket concept has been identified which incorporates Flibe and a vanadium alloy structure. Further work is also reported on helium-cooled, solid breeder designs using Li_2O and vanadium alloys. Special issues include safety analysis of afterheat effects, use of organic coolants and an assessment of the use of Be as a neutron multiplier.

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CHAPTER 2

PLASMA ENGINEERING AND MAGNET DESIGN STUDIES

CHAPTER 2

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2.0 PLASMA ENGINEERING AND MAGNET DESIGN STUDIES

2.1 Overview

Work at ANL throughout the past year has identified several areas in which the tokamak concept could be improved relative to the STARFIRE design. Many of these improvements derive from the possibility of operating in the second stability regime. In particular, we note that plasmas with toroidal betas in excess of twenty percent have been found theoretically stable to ideal MHD modes in high aspect ratio tokamaks with quite modest toroidal currents. In our opinion a tokamak reactor may be most attractive if it embodies the four following characteristics. First, high beta is essential to reducing cost, since considerable capital is invested in the large toroidal field magnets. Second, high aspect ratio ($A \geq 5$) will facilitate the use of efficient fast wave current drive and could simplify reactor maintenance operations. Third, low toroidal current (≤ 5 MA) will greatly reduce the cost of the equilibrium field coil (EFC) system and attendant startup power supplies and will reduce the consequences of a plasma disruption. Fourth, steady-state operation is likely to reduce costs and increase reliability relative to pulsed operating cycles.

The key to achieving these laudable reactor goals may well be rf current drive. At very high beta, tokamak equilibria require very broad, or even hollow, current density profiles, and it may be possible to tailor the current density in an rf-driven discharge by careful selection of the wave properties. Furthermore, a judicious choice of current density profile can also result in small toroidal current; in addition to the advantages cited above, this also helps by reducing the rf current drive power, which thus improves the reactor's power balance.

There is a strong interplay among the various tokamak improvements which were envisioned in this year's TPSS activity. Figure 2-1 schematically illustrates some of these areas of interaction. The possibility of high beta, second stability operation, in the box in the figure, permits low toroidal current, I_0 , for example. In turn this reduces the circulating power, P_{CD} , for rf-current drive, making steady-state operation economical. The synergistic benefit is that rf-current drive may be necessary in order to achieve

Fig. 2.1. Interrelated features of ANL-conceived improved tokamak reactor.

the second stability regime. A quantitative study of rf-current drive for this application is given in Sec. 2.2; we show from our preliminary work on this problem that a wide variety of equilibria can be created with the fast wave.

Other hallmarks of the second stability regime are that large aspect ratio and only a mildly shaped plasma cross section are needed to get high beta. These features, which are discussed in Sec. 2.3, are very attractive to reactor engineers. As the figure suggests, the resulting reactor may have simplified toroidal field (TF) coils which are nearly circular and an equilibrium field (EF) coil system which is relatively small and easy to maintain.

Of course the greatest benefit of high beta is the promise of reduced toroidal magnetic field, since the TF coils are a major capital expense for a tokamak. In Sec. 2.4 an investigation of superconducting TF coils is presented in which it is assumed that the maximum field at the coil is $B_M \approx 6$ T.

At this relatively low field the coil current density can be fairly high so the TF coils can be quite compact. Other benefits of low B_M include the possibility of using liquid lithium coolant. Related improvements in blanket design and impurity control are discussed in other chapters of this present report.

The integrated design of a tokamak reactor has not been attempted in the ANL work on TPSS in FY-1985. At present we are still developing our analytical tools for understanding how to implement concept improvements. However, a reference design for the ANL study has evolved in the course of the year. In Sec. 2.5 we summarize the features of this reference design.

2.2 Tokamak Equilibrium Generation by Narrow-Spectrum Fast-Wave Current Drive

2.2.1 Introduction

The theory of noninductive current drive was originally posed for a uniform, homogeneous magnetic field geometry for which a current drive efficiency can be readily calculated. This efficiency is the ratio of current density, j , to the drive power density, p^{QL} , absorbed and is a function of the electron temperature (T_e) and density (n) as well as the location in velocity space where momentum and energy are supplied by the driver.⁽¹⁾ A shortcoming of this earlier work is that the theory is most frequently applied to tokamaks, which inherently have nonuniform magnetic fields. The field inhomogeneity is the source of particle drifts which create additional current density. This circumstance complicates the interpretation of tokamak experiments where, for example, the total toroidal current is easily measured but is difficult to relate to the parallel current density generated along the field lines by non-inductive means. Additional difficulty arises when trying to properly account for the nonuniform distribution of the driver power density throughout the plasma. These limitations to current drive theory have recently been lifted,^(2,3) and we report here the first two-dimensional calculations of tokamak equilibria generated with a steady-state wave driver. The results are self-consistent in that the magnetic fields generated by the rf-current drive are the same fields which determine the wave propagation and damping.

We are particularly interested in rf-current drive for reactors, so our results are presented for a moderate-size tokamak of the INTOR size. At reactor densities ($n \gtrsim 10^{20} \text{ m}^{-3}$), the lower hybrid (slow) wave might not penetrate

to the plasma interior, so we focus our attention on the fast wave. The merits of this driver candidate have been discussed previously both at low frequency (near the ion cyclotron frequency, ~ 50 MHz)^(4,5) and at relatively high frequency ("lower hybrid" range, ~ 1 GHz),⁽⁶⁾ and experimental work has begun.⁽⁷⁾ We study a tokamak with a large aspect ratio, $A = 6.0$, which is theoretically capable of operation at high beta in the second stability region.⁽⁸⁾ For simplicity we consider an elongated, Dee-shaped plasma without any indentation. Calculations have shown this geometry to be favorable to ideal MHD stability at beta as high as twenty per cent, provided the current density profile achieves a special form.⁽⁹⁾ Many investigations have affirmed the importance of the current density profile to stability,⁽¹⁰⁻¹²⁾ and we want to investigate the possibility that fast wave current drive could provide the necessary profile control to maintain a steady-state high beta equilibrium.

In order to emphasize the technique of fast-wave current profile control, we have greatly simplified the problem. Thus, we ignore the bootstrap current,⁽³⁾ and we require pure steady state so there is no Spitzer (inductive) current. Wave damping is linear, and we assume the plasma contains a single ion species, deuterium. The dispersion relation⁽¹³⁾ is evaluated for wave angular frequencies ω in the range $\Omega_D \ll \omega \ll \Omega_e$, where Ω_D and Ω_e are the ion and electron cyclotron frequencies. The most prominent simplification is the choice of a very narrow wave power spectrum: a single $n_{\parallel}$ is taken to describe the wave spectrum. This choice permits a fast-running ray tracing computer calculation. A realistic antenna would, of course, radiate power in a band of wave lengths. Moreover, if we had the opportunity to arbitrarily select a power spectrum, we would expect to have greater flexibility in controlling the resulting equilibrium current profiles in finer detail. The present results, therefore, only indicate the type of gross control one has in fashioning tokamak equilibria. It is premature to analyze our equilibria for MHD stability until a multi-mode spectrum is included in the calculation, which is planned for FY-1986.

The calculation procedure is outlined in the next section, and this is followed by illustrations of our results for the reactor-size tokamak. A sensitivity study is then presented in which several model parameters are varied independently. We especially monitor the resulting rf-MHD quantities of interest to reactor design: beta, total toroidal current, and rf power. Of

course we desire a high beta and low rf-current drive power to minimize the capital cost and circulating power in a steady-state reactor. In addition, the toroidal current should be as low as possible (consistent with stability and confinement) in order to minimize the cost of the equilibrium field coils and power supplies and to minimize the effects of major disruptions. Our results indicate that fast-wave current drive may be employed to enhance the performance of tokamak plasmas. All units in this report are SI, except temperatures which are in keV.

2.2.2 Numerical Technique

We start with a short description of the calculation scheme. The density and temperature profiles are specified as given functions of the poloidal flux, ψ , where the two-dimensional ψ is the solution to the Grad-Shafranov equation with an initial guess for the diamagnetism, $F(\psi)$. The fast wave propagation and damping is computed by ray tracing, and the rf heating power density, $\langle p^{QL} \rangle$, is found for each flux surface. In addition, the wave vector evolution is followed, and the variation in wave phase speed, $\omega/k_{\parallel}$, is determined along the ray's path. This permits an evaluation of the quantity $\langle j_{\parallel} B \rangle / \langle B^2 \rangle \equiv G(\psi)$, where $\langle \rangle$ denotes the flux surface average, B is the total magnetic field, and $j_{\parallel}$ is the current density parallel to the field. Knowing $G(\psi)$ we can solve an ordinary differential equation to obtain the actual function $F(\psi)$ generated by the fast wave driver. This $F(\psi)$ replaces the initial guess, and the Grad-Shafranov equation is solved again for an updated flux function ψ . The ray tracing calculation is repeated; and the whole procedure is thus iterated until the solution converges.

2.2.2.1 MHD Calculation

The density profile of the electrons is chosen as

$$n(\psi) = n_c \tilde{\psi}^{\alpha_n}, \quad (2-1)$$

where $\tilde{\psi} \equiv (\psi - \psi_b) / (\psi_a - \psi_b)$, with ψ_a the poloidal flux at the magnetic axis and ψ_b the value at the (limiter) boundary. The ion density profile has the same form. The temperature profiles are

$$\text{and } \left. \begin{aligned} T_e(\psi) &= T_{e0} \tilde{\psi}^{\alpha T} \\ T_D(\psi) &= T_{D0} \tilde{\psi}^{\alpha T} \end{aligned} \right\} \quad (2-2)$$

for the electrons and ions, respectively. The total pressure is thus

$$p(\psi) = n(\psi) [T_e(\psi) + T_D(\psi)]/f_p \quad (2-3a)$$

$$= p_0 \tilde{\psi}^\alpha. \quad (2-3b)$$

The peak pressure is $p_0 = n_0 [T_{e0} + T_{D0}]/f_p$, where $f_p = 0.9$, allowing for a small impurity content. More elaborate pressure profiles could be chosen,^(8,11,14) but these simple forms are instructive for our purposes since we are not concerned with stability and because transport code predictions for reactor density and temperature profiles are not yet reliable. Note $\alpha = \alpha_T + \alpha_n$.

The diamagnetism is $F(\psi) = RB_t$, where R is the distance from the symmetry axis and B_t is the toroidal component of the magnetic field in the presence of plasma. In a vacuum F is a constant, $F_b = R_0 B_0$, by Ampere's Law; we take B_0 to be the vacuum field at the major radius, R_0 . High beta plasmas tend to be diamagnetic, so in general we will expect $F(\psi) < F_b$. The initial form of F is given by a simple analytic expression

$$F(\psi) = F_b (1 - \delta \tilde{\psi}^\gamma)^{1/2}; \quad (2-4)$$

and both F and its derivative, $dF/d\psi$, are determined numerically on subsequent iterations.

The axisymmetric equilibrium is fully determined with a knowledge of $p(\psi)$ and $F(\psi)$ for a specified boundary condition. In cylindrical coordinates, $\psi(R, Z)$ is the solution⁽¹⁵⁾ to

$$-4\pi^2 \left(\mu R^2 \frac{dp}{d\psi} + F \frac{dF}{d\psi} \right) = R^2 \nabla \cdot \left(\frac{\nabla \psi}{R^2} \right) \quad (2-5a)$$

$$= 2\pi R \mu j_t, \quad (2-5b)$$

where μ is the free-space permeability.

In the present work the boundary shape is given parametrically as

$$\begin{cases} Z = \kappa(R_0/A) \sin t \\ R = R_0 + (R_0/A) \cos(t + d \sin t) , \end{cases}$$

with $0 \leq t \leq 2\pi$.

We set $\psi_a = 0$ and require $\psi = \psi_b = \text{constant}$ at the boundary. This equilibrium equation is solved using a standard successive-over-relaxation technique. With a knowledge of $\psi(R, Z)$ the total magnetic field may be calculated,

$$\underline{B} = F\nabla\zeta + \nabla\zeta \times \nabla\psi/2\pi , \quad (2-6)$$

where $\nabla\zeta = \hat{\zeta}/R$, $\nabla\psi = \hat{\psi}2\pi R B_p$, and B_p is the poloidal field component. We define $\hat{\zeta}$ to be a unit vector parallel to the toroidal current density at the magnetic axis and $\hat{\psi}$ to be a unit vector outward normal to a flux surface. The poloidal direction is $\hat{\chi} \approx \hat{\zeta} \times \hat{\psi}$, so $B_t = \underline{B} \cdot \hat{\zeta}$ and $B_p = \underline{B} \cdot \hat{\chi}$. Several quantities of interest may now be computed. First, from Eq. (2-5b) we can find the toroidal current density, j_t , which we see is not constant on a flux surface. Next, the safety factor, which is a function only of ψ , is given by

$$q(\psi) = \frac{F(\psi)}{2\pi} \int \frac{dr \chi}{R^2 B_p} ,$$

where the integration path is along $\hat{\chi}$. We define $q(\psi_a) \equiv q_a$ and $q(\psi_b) \equiv q_b$. The total toroidal current is

$$I_t = \mu^{-1} \oint_{\psi_b} B_p dr \chi ,$$

where the integral is evaluated at $\psi = \psi_b$. The toroidal beta is

$$\beta_t = 2\mu\bar{p}/B_0^2 ,$$

where $\bar{p}$ is the volume-averaged pressure,

$$\bar{p} = \int p(\psi) V^{-1} d\psi / \int V^{-1} d\psi ,$$

and the differential volume is

$$V' \equiv dV/d\psi = \oint dr_{\chi} / B_p . \quad (2-7)$$

Additional equilibrium parameters which are calculated include the poloidal current density

$$j_p = - \frac{2\pi B}{\mu} \frac{dF}{d\psi} , \quad (2-8)$$

and poloidal beta, ⁽¹⁵⁾

$$\beta_p = 2\mu\bar{p} / \langle B_p^2 \rangle \Big|_{\psi_b} .$$

The flux surface average is given by

$$\langle Y \rangle \equiv \oint \frac{dr_{\chi}^Y}{B_p} \Big/ \oint \frac{dr_{\chi}}{B_p} = \oint \frac{dr_{\chi}^Y}{B_p} \Big/ V' .$$

At high β_p the magnetic axis, R_{mag} , where $\psi = \psi_a$, is pushed outward, away from $R = R_0$. The location of R_{mag} is important in a reactor since it affects such concerns as the poloidal neutron flux distribution on the first wall. At each iteration in the code, the $\psi(R,Z)$ solution and these sundry MHD quantities are recalculated.

2.2.2.2 Ray Tracing and Current Generation

The ray path is calculated in cylindrical coordinates, although we are only interested in the trajectory's projection into the R,Z plane. The wave is launched near the plasma edge, at a density just sufficient to permit wave propagation, and the ray path and wave vector ($\underline{k}$) evolution are followed with a predictor-corrector routine. The wave frequency, parallel index of refraction ($n_{\parallel} = c\underline{k} \cdot \underline{B}/\omega \equiv ck_{\parallel}/\omega$), and poloidal index ($n_p = c\underline{k} \cdot \hat{\chi}/\omega$) are specified at the start, and the ray's progress is calculated from a WKB analysis of the spatial variations in the dielectric tensor, $\underline{K}(\underline{r}, \omega, \underline{k})$. ⁽¹³⁾

At each time step along the ray path the $\underline{K}$ tensor must be evaluated using the local plasma properties of the magnetic field and the densities and temperatures for the various species. This is accomplished with a bicubic spline interpolation on the $\psi(R,Z)$ grid to determine the local value of ψ ; from this the plasma quantities are evaluated using Eqs. (2-1), (2-2), and (2-6). The capability of ray tracing with arbitrarily shaped flux surfaces is of course essential to our problem.

Along the ray trajectory the imaginary part of the $\underline{k}$ vector is determined in order to compute wave damping and the resulting plasma heating. Electrons are heated by Landau damping and ions by high harmonic cyclotron damping. In the interests of efficient current drive we desire to minimize ion damping. Since we typically choose a wave frequency above the tenth deuterium cyclotron harmonic, the ion damping is usually negligible. However, at high ion temperatures ($T_{D0} \gtrsim 20$ keV) damping by the ions can start to compete with that by the electrons. It has been suggested^(4,5) that this ion cyclotron damping might be avoided in a large aspect ratio tokamak by operating at $\omega \approx 2.5 \Omega_D|_{R_0}$; for $A = 6$ it is possible to displace the cold ion resonance surfaces outside the plasma at such a low frequency. This low-frequency option has not been studied in our present work. Another concern in a reactor plasma⁽¹⁶⁾ is that the highly energetic alpha particles may appear unmagnetized and strongly absorb the wave via a perpendicular Landau resonance. This parasitic absorption might be avoided if electron damping is sufficiently strong to achieve wave damping in one or two passes through the plasma.⁽⁵⁾ Our code, which can follow the ray through multiple reflections, suggests that such strong electron damping can indeed be achieved. Detailed study of alpha particle damping is, however, beyond the scope of our present work.

The ray is started with a total power $P = P_{\text{edge}}$. Its fate is monitored fairly frequently initially, as the plasma dispersion properties vary fairly quickly along its path in the edge region. The radial group velocity slows as the wave penetrates to higher density, and the time steps are accordingly permitted to increase. At each time step the wave power decrement, ΔP , and change in flux, $\Delta\psi$, are computed, and, with the aid of Eq. (2-7), the electron heating power density is found:

$$\langle p^{QL} \rangle = f_e \Delta P / (V \Delta\psi) ;$$

f_e is the damping power fraction to the electrons. Likewise, at each time step the value of $k_{\parallel}$ is computed, and, from the local value of T_e , the ratio of parallel phase speed to electron thermal speed is found: $u = \omega/k_{\parallel} v_e$, where $v_e = \sqrt{T_e/m_e}$. From this we can evaluate the normalized ratio of current density to power density in a uniform medium,^(1,17)

$$\tilde{j}/\tilde{p} = 2 + 1.4 u^2 + (8/u) , \quad (2-9)$$

which assumes linear Landau damping in a plasma with $Z_{eff} = 1$. The quantity $\tilde{j}/\tilde{p}$ is related to the flux surface average $\langle j_{\parallel} B \rangle$ by the expression^(2,3)

$$\langle j_{\parallel} B \rangle / \langle B^2 \rangle = C \frac{T_e}{n} \left[\frac{\tilde{j}/\tilde{p}}{B} \right]_{\chi_r} \langle p^{QL} \rangle \equiv G(\psi) , \quad (2-10)$$

where $C = 19.1 \times 10^{18} / \ln \Lambda$, $\ln \Lambda$ is the Coulomb logarithm, and the bracketed expression is evaluated along the ray path. Contributions to $G(\psi)$ accumulate as the ray turns and makes multiple passes through the flux surfaces. We typically terminate the ray tracing once $P/P_{edge} = 1 \times 10^{-4}$.

In the code a value of $G(\psi)$ is assigned to a discrete number of points in ψ . This is accomplished by dividing $\tilde{\psi}$ into a large number of bins and selecting sufficiently small time steps such that $G(\tilde{\psi})$ varies smoothly. Finally, the pressure gradient and $\langle B^2 \rangle$ are determined at each point in ψ , and the diamagnetism function is found⁽³⁾ by solving the equation

$$\frac{1}{u} \frac{dF}{d\psi} + \left[\frac{1}{\langle B^2 \rangle} \frac{dp}{d\psi} \right] F + \frac{G}{2\pi} = 0 , \quad (2-11)$$

with the boundary condition $F(\psi_b) = F_b$. The numerical solution to Eq. (2-11) provides the $F(\psi)$ for Eq. (2-5a), starting the next iteration in the calculations.

2.2.2.3 Convergence

The functions $F(\psi)$ and $dF/d\psi \equiv F'$ are evaluated for convergence. At low β_t , $F \approx F_b$; however, F' might be large at certain values of ψ , and both of these must be accurately known in order to solve Eq. (2-5a). Typically these functions converge to relative errors $\Delta F/F$ and $\Delta F'/F'$ of 10^{-2} very fast, need-

ing only two or three iterations between the MHD and ray tracing computations. Errors as small as 10^{-3} require five to ten iterations, and by this point the parameters of reactor interest [I_t , β_t , $q(\psi)$, etc.] have also converged to three significant figures. Better convergence, as good as 10^{-5} , is difficult to attain. This is presumably due to the finite $\tilde{\psi}$ grid and is related to the sensitivity of $G(\tilde{\psi})$ near the magnetic axis to the initial history of the ray, launched at the edge.

The time steps are multiples of the initial step, H , in units of inverse wave frequency. We select $H = 0.1$ as the best initial time step since smaller values do not increase the accuracy of our results. The number of points in $\tilde{\psi}$ at which F is evaluated, N_ψ , affects the running time of both the MHD and the ray tracing routines so this choice must also be carefully made. We found $N_\psi = 101$ is adequate; larger N_ψ does not improve accuracy, whereas smaller N_ψ does not significantly reduce running time.

As shown by Eqs. (2-9) and (2-10), the spatial dependence of current generation is a function of the wave heating profile, the spatial variation of $u = \omega/k_\parallel v_e$, and the electron temperature and density profiles. With a single $n_\parallel$ in the wave spectrum, $G(\tilde{\psi})$ peaks fairly strongly around one flux surface, and it is difficult to obtain a very broad G profile which is large at both the magnetic axis and the boundary. In consequence, parallel current density, which is roughly proportional to G , tends to be quite small either at $\tilde{\psi} = 1$ or $\tilde{\psi} = 0$, and this tends to result in a very large safety factor at that location. Since the MHD routine cannot handle these extreme cases we employ a smoothing technique which replaces $G(\tilde{\psi})$ at each $\tilde{\psi}$ point with the average of $G(\tilde{\psi})$ over several points around $\tilde{\psi}$. The main effect is to spread G and raise its value at the endpoints. Of course in a real plasma we might expect such spreading for various reasons -- finite width to the spectrum, low-grade plasma turbulence and wave scattering -- so this mathematical artifice has a physical justification. Several studies were done with averaging over a different fraction of the $\tilde{\psi}$ range, and it appeared that averaging over 20% of the total points would reliably permit convergence. The actual $G(\tilde{\psi})$ is shown for an example in Fig. 2-2 as the dotted curve, and it is seen that the averaged $G(\tilde{\psi})$, the solid curve, only differs significantly near the endpoints. We expect this particular difficulty with the present code will vanish when a multi-ray calculation is implemented.

Fig. 2-2. Converged solution for G (in units of 10^5 A/Wb) for the standard equilibrium, versus normalized poloidal flux. The boundary is $\tilde{\psi} = 0.0$, and the magnetic axis is $\tilde{\psi} = 1.0$. The dotted curve is the actual solution from the ray tracing subroutine, whereas the solid curve is the averaged function used as input to the MHD routine.

The final equilibrium is independent of the initial guess for $F(\psi)$ given by Eq. (2-4). We tested various values of δ and γ corresponding to initial equilibria with a wide range of β_t (0.05 to 0.12) and safety factors ($1.7 < q_b < 6.0$) and found the code always converged to the same solution. The code running time is also uncorrelated with the initial equilibrium choice; typically the code requires ~ 15 s of time per iteration on the Cray 1 and is run for ten to fifteen iterations.

2.2.3. Fast Wave Equilibria

All the equilibria studied have $R_0 = 5.25$ m, $A = 6.0$, a mild elongation, $\kappa = 1.6$, and strong triangularity, $d = 0.5$. The vacuum field is $B_0 = 3.66$ T, so $F_b = 19.1$ T-m. The input variables include the plasma peak density and temperatures, n_0 , T_{eo} , and T_{Do} , and the profile exponents, α_n and α_T , as well as the wave's frequency, $n_{||}$, and P_{edge} . We always set $n_{pol} = 0$ and start the ray at the outboard midplane.

The standard equilibrium, against which we will compare others, has $n_0 = 2.0 \times 10^{20} \text{ m}^{-3}$, $T_{eo} = 18 \text{ keV}$, $T_{Do} = 12.5 \text{ keV}$, $\alpha_n = 1.05$, and $\alpha_T = 0.35$, so the peak pressure is $p_0 = 1.08 \text{ MPa}$ and the pressure profile is fairly broad with $\alpha = 1.4$. The initial guess for $F(\psi)$ has $\gamma = 1.7$ and $\delta = 0.147$ which results in an initial equilibrium with $\beta_t = 0.0813$, $I_t = 3.55 \text{ MA}$, and a monotonic $q(\psi)$ with $q_a = 1.00$, and $q_b = 1.73$. We select a wave with $\omega/2\pi = 400 \text{ MHz}$, $n_{\parallel} = 2.4$, and $P_{\text{edge}} = 50 \text{ MW}$. By the ninth iteration the solution converges to an equilibrium with $\beta_t = 0.0786$, $I_t = 2.45 \text{ MA}$, and a nonmonotonic q profile. The final $\psi(R,Z)$, shown in Fig. 2-3, differs from the initial guess in the variation of V' with ψ . For example, the magnetic axis has shifted from 5.47 m to the final value, $R_{\text{mag}} = 5.52 \text{ m}$; and the initial $\psi_b = 9.83 \text{ Wb}$ has been reduced to $\psi_b = 8.70 \text{ Wb}$, corresponding to the lower I_t of the converged solution. The ray trajectory in (R,Z) on the final ψ is shown in Fig. 2-4. In order to achieve strong damping, with single pass absorption, the ratio $u = \omega/k_{\parallel} v_e$, which determines the strength of electron Landau damping,⁽¹³⁾ must be approximately unity. However, this limits the current drive efficiency. For this standard case, the electron power density, $\langle p^{QL} \rangle$, peaks slightly off-axis, at $\tilde{\psi} \approx 0.8$, where $u \approx 2.1$. From Eq. (2-9) we find $\tilde{j}/\tilde{p} \approx 12$ at $\tilde{\psi} = 0.8$. In comparison, $\tilde{j}/\tilde{p}$ is larger near the edge ($\tilde{j}/\tilde{p} \approx 20$ with $u = 3.3$ at $\tilde{\psi} = 0.1$) but $\langle p^{QL} \rangle$ is much smaller, by orders of magnitude. The important function G , which is given by Eq. (2-10) and determines the equilibrium, is shown in Fig. 2-2 for this standard case. In this example the wave/plasma parameters have been chosen such that 99% of the wave power is absorbed by the time the ray arrives at $\tilde{\psi} = 0.97$, which accounts for the steep dropoff of G near the magnetic axis. We note that at this frequency and ion temperature less than $\sim 0.1\%$ of the power is lost to the deuterons.

The diamagnetism converges to a solution which is given by

$$F(\psi) = F_b \left[1 - \delta \tilde{f}(\tilde{\psi}) \right]^{1/2},$$

where the normalized function $\tilde{f}$ is displayed in Fig. 2-5a for our standard case; for this example the measure of the diamagnetic well depth has increased slightly from the initial guess to the final value $\delta = 0.164$. The function F' is given by the expression

$$FF' = \delta F_b^2 \tilde{f}'(\tilde{\psi}) / \left[2(\psi_b - \psi_a) \right],$$

Fig. 2-3. Converged solution for (a) contours of constant poloidal flux and for (b) normalized flux, $\tilde{\psi}$, versus R , at $Z = 0$, for the standard equilibrium.

Fig. 2-4. Poloidal projection of ray trajectory; vertical axis is compressed relative to horizontal axis.

where $\tilde{f}$ is shown in Fig. 2-5b. Rearranging Eq. (2-11) we find that F' has two components. The p' term is a positive contribution to F' for confined plasma ($p' < 0$) whereas the G term is a negative contribution to F' . Thus, in Fig. 2-5b, we see F' tends to be depressed where $G \sim \langle j_{\parallel} B \rangle$ peaks, in this example, at $\tilde{\psi} = 0.8$. In turn, the structure of Eq. (2-5b) shows that the contributions to j_t are also of opposite sign: p' produces a positive term while F' results in a negative quantity. The net result, in Fig. 2-6a, is that j_t may have regions of reversed sign. This effect is generally seen^(9,15) in high beta, low current equilibria ($\beta_p = 9.28$ in our example). Near the locale $R = 4.8$ m, $Z = 0.0$ m, where $j_t \rightarrow 0$, we find the current density flows in a purely poloidal direction. The reversal at $R < 4.8$ m is due to the Pfirsch-Schlüter current flowing along the field lines.⁽³⁾ The other noteworthy feature in Fig. 2-6a is the double peak in j_t , which clearly arises from the fact that the wave-driven current ($\sim G$) does not peak at the magnetic axis. Due to this hollow feature in j_t the safety factor is not a minimum at the magnetic axis; see Fig. 2-6b. As mentioned earlier, this equilibrium may well be unstable. However, our intention at this point is only to explore the variety of equilibria which can be created with a single $n_{\parallel}$ spectrum. Stability, which depends on details of the $F(\psi)$ and $p(\psi)$ profiles, may not be achievable unless a broader,

Fig. 2-5. Solution for diamagnetism F versus ψ : (a) degree of diamagnetism, where $F = F_b[1 - \delta\tilde{f}]^{1/2}$; (b) gradient of F , given by $FF' \propto f$.

Fig. 2-6a. Normalized toroidal current density for the standard case, along the midplane ($Z = 0$).

Fig. 2-6b. Safety factor for the standard equilibrium:
 $n_0 = 2.0 \times 10^{20} \text{ m}^{-3}$, $T_{eo} = 18.0 \text{ keV}$, $T_{Do} = 12.5 \text{ keV}$, $\alpha_n = 1.05$, $\alpha_T = 0.35$, $\omega/2\pi = 0.4 \text{ GHz}$, $n_{||} = 2.4$, and $P_{edge} = 50 \text{ MW}$.

properly selected spectrum can be employed. In the survey of single $n_{||}$ equilibria which follows we independently vary the input parameters to our problem, first the target plasma properties and finally the wave characteristics.

We first consider varying the peak pressure by changing the peak density. All other parameters are held fixed at the values of our standard case ($T_{eo} = 18$ keV, $\alpha_n = 1.05$, $\alpha_T = 0.35$, $\omega/2\pi = 400$ MHz, $n_{||} = 2.4$, $P_{edge} = 50$ MW) except $T_{Do} = 42.9$ keV. One might expect the peak and average betas to rise as the peak pressure is increased, and this is clearly seen in Fig. 2-7. It is striking, however, that the ratio I_t/P_{edge} decreases as β_t increases. This is mainly due to the inverse relationship of local current drive efficiency with density in Eq. (2-10). Another effect of varying density is shown by the current profile and safety factor insets in the figure. At low density the Landau damping is relatively weak, with only 86% of the P_{edge} absorbed on the first inward pass. The j_t profile peaks on axis and $q(\psi)$ is monotonic. As n_o increases most of the power is absorbed before the ray reaches the magnetic axis, and j_t has double peaks, away from the axis. Consequently $q(\psi)$ becomes nonmonotonic. An additional effect at this high ion temperature is an increase of deuterium damping as n_o increases, which further degrades I_t/P_{edge} . An important inference drawn from Fig. 2-7 is that increases in equilibrium β_t do not necessarily require increases in I_t/P_{edge} . In fact, for a reactor it is most encouraging that I_t may be minimized while, for a given P_{edge} , β_t might be pushed to high values.

The difficulty of associating trends in I_t/P_{edge} with equilibrium β_t values is that both quantities are dependent on profile effects. This is seen graphically in Fig. 2-8, where all parameters and peak densities and temperatures are constant but $T_e(\tilde{\psi})$, $T_D(\tilde{\psi})$, and $n(\tilde{\psi})$ are varied in width (with $\alpha = \alpha_n + \alpha_T = \text{constant}$). As the temperature profile broadens and density narrows, the local current density peaks reside in a surface region which becomes increasingly favorable to current drive: higher surface temperature and lower surface density. The effect is two-fold. First, larger j_t results [Eq. 2-10], which leads to an increase in I_t . More importantly, the increase in j_t near the limiter permits a larger pressure gradient to exist at the limiter. This, of course, raises pressure everywhere inside the plasma and increases β_t . This type of result supports the rationale of the rf-driven equilibrium proposed for STARFIRE.⁽¹⁸⁾ That is, a hollow current density minimizes the

Fig. 2-7. Equilibrium beta and toroidal current for various peak densities and pressures; all other parameters are those of the standard case, except $T_{D0} = 42.9 \text{ keV}$. Toroidal current density (labelled "j") and safety factor, shown in the insets, vary along the curves.

Fig. 2-8. Equilibrium beta and toroidal current for differing temperature and density profiles at a fixed pressure profile ($\alpha = 1.4$). All other parameters are those of the standard case.

I_t/P_{edge} requirement and puts current density near the limiter where it is most useful for sustaining large plasma pressure.

In another sequence the pressure profile width was varied with $P_{\text{edge}} = 60 \text{ MW}$, $T_{\text{eo}} = 15 \text{ keV}$, $T_{\text{Do}} = 15.5 \text{ keV}$, and the other parameters at their standard values, while the ratio α_n/α_T was kept constant. As the pressure becomes narrower (α increases from 1.4 to 2.4) with fixed p_0 the average pressure drops, and, accordingly, β_t decreases from 0.0676 to 0.0377. At the same time I_t/P_{edge} remains nearly constant, at 0.0395. This shows that broad pressure profiles may be desirable for high β_t equilibria.

The variation of I_t/P_{edge} with T_{eo} is nearly linear, as shown in Fig. 2-9. This is a reasonable expectation, based on the functional dependence in Eq. (2-10). Also, β_t increases with T_{eo} , even though p_0 is held constant. As before, peaks of j_t , localized away from the plasma center, increase, and support higher local pressure gradients, broadening the pressure profile to yield higher β_t .

Figure 2-10 shows that, for a given target plasma, slower phase speeds produce higher β_t . Again this is associated with the shift of j_t to the plasma periphery. The variation with wave frequency is given in Fig. 2-11. At the highest frequencies the ray trajectory does not bend sharply towards the magnetic axis, and in consequence j_t is concentrated nearer the outer flux surfaces, which results in higher β_t .

The final illustration, Fig. 2-12, displays the variations with wave power for the standard case with different electron temperatures; ($T_{\text{eo}} + T_{\text{Do}}$) is kept constant so p_0 is constant. To a great extent I_t varies linearly with P_{edge} , as expected from Eq. (2-10). The increase of β_t with P_{edge} likewise is due to increases in j_t which support the larger local pressure gradients.

2.2.4 Discussion

The power of this two-dimensional current drive calculation is that it self-consistently converts the local $\tilde{j}/\tilde{p}$ computed by Fisch and Karney⁽¹⁷⁾ into global quantities of interest to a reactor designer. Numerical ray tracing on specified temperature and density profiles permits an accurate assessment of the spatial rf-heating and the resulting parallel current density generation.

Fig. 2-9. Equilibrium beta and toroidal current versus electron temperature; density is held constant ($n_0 = 2.0 \times 10^{20} \text{ m}^{-3}$) and T_{D0} is adjusted to keep the pressure constant at $P_0 = 1.08 \text{ MPa}$. All other parameters are those of the standard case.

Fig. 2-10. Equilibrium beta and toroidal current versus the parallel index of refraction; all other parameters are those of the standard case.

fig. 2-11. Equilibrium beta and toroidal current for various fast wave frequencies; all other parameters are those of the standard case, except $T_{e0} = 150$ keV, $T_{D0} = 15.5$ keV, and $P_{\text{edge}} = 60$ MW. Toroidal current density is shown in the insets.

Fig. 2-12. Equilibrium beta and toroidal current versus edge power at different electron temperatures; density is held constant ($n_0 = 2.0 \times 10^{20} \text{ m}^{-3}$) and T_{D0} is adjusted to keep the pressure constant (at $p_0 = 1.08$ MPa). All other parameters are those of the standard case.

By solving for MHD equilibrium the code includes diamagnetic currents, and thus the toroidal current and beta are accurately determined in a torus of arbitrary aspect ratio and cross-sectional shape.

The local theory of wave-driven currents, embodied in Eq. (2-10), suggests that plasma current generation increases with electron temperature and rf power and decreases with electron density, and these predictions appear to be verified when applied to the equilibrium toroidal current (Figs. 7, 9, and 12). However, profile effects, which are difficult to assess with a local theory, seem to play an important role in determining the equilibrium. We found by following the fast wave trajectory, for example, that $\tilde{j}/\tilde{p}$ may be largest near the boundary whereas $\langle p^{QL} \rangle$ may peak near the magnetic axis. Moreover, if higher phase speeds are chosen such that $\tilde{j}/\tilde{p} \geq 30$ at the magnetic axis, then $\langle p^{QL} \rangle$ may decrease so much that single-pass absorption cannot occur and competition from other power sinks (ions, wall absorption) will be a concern. We also found (Fig. 2-1) that a single- $n_{\parallel}$ spectrum peaks $G(\tilde{\psi})$ to the extent that substantial $j_{\parallel}$ cannot be generated at both the axis and the edge region simultaneously. Thus, in order to obtain fairly conventional $q(\psi)$ profiles, some width to the spectrum may be desirable.

Profile effects become particularly important whenever the current density peaks away from the magnetic axis. Good illustrations of this are Figs. 2-8 and 2-11 which show that beta generally increases as the peaks of j_t increase [with broader $T_e(\psi)$] or move further towards the periphery (at higher frequency). Heuristically we can view the current density as a retaining wall; since $\nabla p = \underline{j} \times \underline{B}$ in equilibrium, then the pressure (beta) contained by the peaks of the current density mounds up higher as the peak of $\underline{j}$ is increased or moved towards the boundary. Figure 2-7 shows fairly dramatically the need for calculating the current-drive equilibrium with the proper two-dimensional profiles, for we find that beta can increase even as I_t decreases under certain circumstances. This is a desirable trend for reactor design, and we feel that hollow toroidal current density profiles deserve further consideration for rf-driven tokamaks.⁽¹⁸⁾

In conclusion, we are encouraged that a wide range of equilibria have been found with a single-ray, fast-wave current drive calculation. This offers the hope that tokamak stability may be enhanced relative to inductive current drive by a clever selection of wave driver parameters. A multi-ray

calculation with a tailored spectrum and a detailed stability analysis is required in the future. We note, however, that there are already reports⁽¹⁹⁾ of experimental success in modifying tokamak MHD behavior with rf current drive. Thus, the motivations for studying rf current drive clearly extend beyond the hope of achieving steady-state operation.

2.3 Second Stability Regime

It is in the area of plasma equilibrium and stability, as represented by higher β , that one can envision one of the greatest improvements to existing reactor designs. Even though all of the empirical evidence for present tokamaks supports the fact that β is limited by a relation of the form:⁽²⁰⁻²³⁾

$$\beta_t = C_0 \frac{I(\text{MA})}{a(m)B(T)},$$

with the constant C_0 being on the order of 3.5, commonly known as the "Troyon Limit", there are theoretical indications of a "Second Stability" region with much higher values of β .⁽²⁴⁻²⁷⁾ The situation is somewhat analagous to that regarding steady-state, rf current drive when STARFIRE⁽²⁸⁾ was undertaken. That is, there was at the time no concrete evidence that a plasma current could be started nor maintained by noninductive current drive, but to do so would be an essential improvement for a reactor. The incorporation of such an innovative concept in STARFIRE led to increased interest in the topic, with the result that rf current drive has now been demonstrated and appears to be a viable feature for a reactor. In the same sense it is reasonable to investigate the ramifications of reactor operation in the second stability region, and if these ramifications are, as expected, very favorable, they will provide an impetus for further research in this area. In any event there is enough theoretical support for a second stability region to warrant its inclusion in an innovative type of reactor study such as ANL's TPSS effort.

Perhaps the most extensive investigation of second stability region equilibria and stability has been done at the Princeton Plasma Physics Laboratory in connection with PBX.⁽²⁹⁾ These studies center around equilibria which are indented on the inboard side. The ballooning mode stability was studied by Chance et al.,⁽¹¹⁾ the analysis was extended to the $n = 1$ kink by Manickam et al.,^(14,30) and a more complete survey of such equilibria was presented by

Grimm et al.⁽⁸⁾ The overall results of these studies are that as far as internal modes (those modes studied by calculations with a conducting boundary at the plasma edge) are concerned, access to the second stability region exists for a variety of plasma parameters. This access improves with (1) increased plasma shaping, (2) increased aspect ratio, and (3) increased safety factor. (It can be recalled that the conventional, first stability region stability becomes worse for high aspect ratio and safety factor.) The PBX device was proposed, modified from PDX, and run based on these studies. There is as yet no evidence that PBX has achieved equilibria in the second stability region, although it has achieved higher values of β than PDX did. On the other hand, PBX is not the optimum configuration for getting into the second stability region.

The one feature of second stability region equilibria that is possibly of most concern is that they are in general not stable to external kink modes, that is those modes that appear in calculations with the conducting boundary significantly removed from the plasma. In fact, the optimization studies performed at GA Technologies, in which the cross section shape that gives the highest stable β is determined subject to various constraints, show that the optimized cross section is a D-shape if external modes are included and a bean shape if only ballooning modes are considered.^(31,32) On the other hand, there is no conclusive evidence that external kink modes have been experimentally observed, even where they have been predicted. The present stability codes do not treat the realistic case of a wall of finite thickness and finite conductivity near the plasma, and all experiments have conducting structure near the plasma boundary. PBX has in fact been designed to have passively stabilizing structure close to the plasma to help prevent these modes. A reactor will have on the order of a meter of metal structure in the first wall/blanket/shield with a skin time on the order of one second near the plasma. In addition, beryllium, which is both a good conductor and a good neutron multiplier, can be utilized in the blanket near the first wall to enhance the conductivity. These arguments indicate that there is a realistic possibility of suppressing the unstable external modes that appear to be a feature of second stability equilibria.

A second undesirable feature of the types of second stability region equilibria studied in connection with PBX is that they have large indentations

on the inboard side if they are to provide access to the second stability region. These configurations require a "pusher coil" on the inboard side to achieve the required indentation. This coil is large, has a large amount of current, and is in a very undesirable position for a reactor. In addition, there is doubt that the required indentation can be achieved if the coil is sufficiently far from the plasma to be adequately shielded. It is not a desirable engineering feature of a reactor, and may nullify the benefits of the improved β . It is desirable to find second stability region equilibria that do not require such shaping, such as the high- β , D-shaped equilibria presented by Manickam.⁽³³⁾

One way to avoid a pusher coil is to go to a high aspect ratio. Second-stability equilibria generally improve with increasing A. In particular, less shaping is necessary to access the second stability region.⁽³⁴⁾ Since conventional equilibria favor low A, most reactor designs have had low aspect ratios ($A = 4$ or less), so that the benefits of high A have been little investigated. A major benefit is the better utilization of the toroidal field.

Since the toroidal field falls off as $1/R$, at high A there is less percentage drop across the blanket/shield from the peak field position. Since the power scales as B_t^4 , this can be a significant effect. In fact, because of the trade-off between the decrease of β with A and the increased toroidal field in the plasma for a fixed field at the coils, the cost of a reactor^(35,36) such as STARFIRE is relatively insensitive to A. One would thus expect the cost of a second stability region reactor to decrease sharply with A. In addition, by opening up the region in the center of the torus, maintenance should become easier for a high aspect ratio reactor. This affects the plant availability, one of the most important cost parameters. These and other similar considerations lead to the conclusion that one of the major benefits of the second stability region is the fact that it favors a high aspect ratio.

An interesting feature of second stability equilibria is that even though the numerical calculations have indicated the minimum β for the onset of the second stability region, they seem unable to determine the upper limit of β for this region. One, of course, does not want to design a reactor at the minimum value of β ; he wants to use the maximum value possible. The upper limits of the equilibria that have been investigated seem to be determined by

the inability of the code to calculate equilibria with still higher values of β , not by the stability of the equilibria. In addition, these upper limits seem to be determined by the degree to which the code has been "tuned", not by physics.

The upper limits so found are rather unclear as it becomes a matter of judgment as to when the results are no longer numerically meaningful as the limits are approached. The lack of information about the maximum beta in the second stability region makes it difficult to parametrically optimize a reactor configuration.

For the purposes of having a reference for this study, a second-stability region, nonindented equilibrium with the parameters given in Table 2-1 has been calculated by Miller and Manickam at Princeton.⁽³⁷⁾ This preliminary choice is not considered to be the optimum choice, but it does represent the type of features to be expected. The equilibrium is stable to ideal, internal modes. It has a somewhat large elongation, a triangularity that should not be difficult to achieve, and a very broad pressure profile. The current density profile is slightly negative on the inboard edge and is very sharply peaked at the outboard edge. Consequently, it may not be physically reasonable. The equilibrium is near the pressure limit for which an equilibrium can be found with the given geometry and profile parameters.

Table 2-1. Preliminary Reference Equilibrium

Quantity	Symbol	Units	Value
Aspect ratio	A		6.0
Elongation	κ		2.0
Triangularity	d		0.25
Major radius	R_o	m	5.25
Magnetic axis	R_{mag}	m	5.56
Vacuum toroidal field @ R_o	B_{to}	T	3.81
Toroidal beta	β_t		0.19
Plasma current	I_p	MA	4.3
Safety factor (limiter)	q_b		5.05
Safety factor (axis)	q_a		1.10

2.4 Impact of Reactor Parameters on TF Coil Design

In order to increase the mass utilization in a tokamak reactor we would desire low TF magnetic fields, which would, of course, be possible with sufficiently high beta operation. Further reduction in reactor mass would be obtained if the nuclear shielding around the blanket could be made thinner. This could adversely affect the TF coil operation, and hence the shielding issue is an optimization question of some import to reactor design. In the course of the TPSS activity we would therefore like to design the TF coils for various postulated conditions in order to understand the design tradeoffs. We note in particular that recent studies^(38,39) indicate that superconducting windings in TF coils can accept much higher heat loads than have previously been considered and simultaneously can be designed with higher than conventional current densities. Table 2-2 lists three postulated nuclear environments, corresponding to differing amounts of shielding. This section outlines the procedure used to identify the winding characteristics for a given maximum field and nuclear environment. Our preliminary indications are that TF design above ~6 T will be impossible for the worst postulated radiation dose, but, if second stability operation does permit maximum fields of ~6 T, then it may be possible to reduce shielding and reduce the reactor mass.

Table 2-2. Nuclear Responses in TF Coils (NbTi-Cu Coils)*

Insulator dose (rads)	10^{10}	10^{11}	10^{12}
Nuclear heating in the winding (MW/cm ³)	6×10^{-2}	0.7	7
Fast neutron fluence (n/cm ²)	3×10^{18}	4×10^{19}	4×10^{20}
dpa	2×10^{-3}	2×10^{-2}	2×10^{-1}
Blanket/shield thickness (cm)	105-126	90-111	75-96
	15		15

- * -- BCSS, STARFIRE, MARS, and INTOR
 -- 5 MW/m² neutron wall loading
 -- 150 MWy/u² D-T exposure at the first wall
 -- Steel-type shield with Li/V or LiAlO₂/Be/H₂O

2.4.1 Baseline Radiation Load and TF Configuration

A very aggressive level of radiation will be examined in this exercise. In essence, we pose the following question: "Suppose an insulating material can be found that will survive 10^{12} rads absorbed over the life of the machine, which is assumed to be 40 full-power years (fpy); can the other consequences of this level of radiation, namely 7 mW/cm^{-3} peak heat load in the winding pack, $4 \times 10^{20} \text{ n/cm}^{-2}$ fast neutron fluence, and 0.2-dpa damage to the copper stabilizer of the conductor, be tolerated?" Some immediate consequences of this scenario can be noted. Figure 2-13 gives part of the evidence suggesting that none of the Al5 compound superconductors will survive neutron fluences greater than $\sim 10^{19} \text{ n/cm}^{-2}$. Niobium-titanium conductors probably will be useable at these levels; however, we shall see that this presents a non-trivial problem for heat removal because of the much lower critical temperature, T_c , of this alloy compared to the Al5 compounds. The result is lower attainable maximum fields.

Force cooled conductors will be stipulated in this study because active cooling will be needed to handle the high peak heat loads, and because cable-in-conduit, force-cooled conductors are expected to suffer much less degradation of stability due to radiation damage to the copper stabilizer. But in this type conductor, some temperature margin between inlet helium temperature and the current sharing temperature of the superconductor must be used in removing the heat and some must be maintained to stabilize the conductor against perturbations that may be imposed during operation of the magnet. It will be shown that a maximum field of $\sim 6 \text{ T}$, with quite reasonable winding pack current density, can be supported while maintaining adequate stability and the ability to protect the coils in the event of a quench to end of the machine life.

The TF coil parameters for this study are given in Table 2-3, except that the severe radiation level (10^{12} rads) is considered. We study the nominal 6-T coil (with 6.21 T at the windings).

The number of coils in the TF set affects field ripple at the outer plasma edge, machine access, ease of assembly and disassembly, and (to some extent) protection. To illustrate the effect of the number of coils on field ripple, a winding pack current density $J_{\text{pack}} = 60 \text{ A/mm}^{-2}$ and a winding build of 0.2 m was assumed, and the calculations in Fig. 2-14 were performed. For 16 coils with the assumed winding geometry, the ripple is 4.4% peak-to-peak.

Fig. 2-13. Effects of neutron damage on the critical current of several technical superconductors.

Table 2-3. TFC Specifications: Thick Shield

	Unit	Variable	Value
Plasma major radius	m	R_O	5.250
Plasma minor radius	m	a	0.875
Inboard blanket/shield/vacuum	m	Δ^i	1.230
Outboard blanket/shield vacuum	m	Δ^O	1.930
Inboard shield/TFC interface	m	R_m	3.140
Outboard shield/TFC interface	m		8.055
Plasma elongation		κ	2.0
Inboard leg (approx.)	m	R_1	2.895
Outboard leg (approx.)	m	R_2	8.305
Half height (approx.)	m	H_{max}	3.420
Location of maximum height	m	R_{Hmax}	4.885
Perimeter	m	L	19.4
Stored energy in TFC ($B_M = 6$ T)	GJ	U^{TFC}	5.1
Mass of TFC	Mg	M^{TFC}	800
Shield mass	Mg	M^S	1690
Blanket mass (drained)	Mg	M^B	240
Stored energy in EFC	GJ	U^{EFC}	≤ 0.2
Number of TFC		N^{TFC}	16
Ripple (% pp @ $R_O + a$)		δ	≤ 3.5
Insulator dose	rads		10^{10}
Nuclear heating in winding	MW/cm ³		0.06
Total nuclear heating @ 4 K	kW		~ 1.5
Total conduction loss @ 4 K (approx.)	kW		~ 8

Fig. 2-14. Toroidal field ripple at the outer plasma edge versus number of coils for having $J_{\text{pack}} = 60\text{-A/mm}^{-2}$ and 0.2-m build.

The number of coils has the strongest influence, but spreading the windings might be expected to reduce the ripple somewhat. To get a feel for the magnitude of this effect, an additional 16-coil example was calculated. In this case, the same 0.2-m winding build was used, but J_{pack} was reduced to 40 A/mm^{-2} and the winding spread laterally to compensate. Figure 2-15 shows a comparison of the assumed winding shapes and lists the corresponding field ripple values. From Fig. 2-14 it is apparent that if field ripple no higher than 2 to 3% can be tolerated, there may be no fewer than 18 coils, unless other means of suppression techniques are used. Our goal is to achieve 3.5% ripple, either by moving the outboard coil leg ~20 cm further out, or by adding ferromagnetic shims.

$$N_{\text{coils}} = 16$$

Fig. 2-15. Examples to illustrate the effect of reducing J_{pack} and spreading the windings on field ripple at the outer plasma edge.

2.4.2 Winding Pack Design

Particular features of the winding pack are often a matter of designer's choice. A successful design results when the strong features of a particular type of design are emphasized and the weak features compensated. We begin this design with a cable-in-conduit conductor (CICC). The sheath provides strong containment of the helium coolant and also adds structure at the source of the electromagnetic forces. There are benefits for maximizing the overall current density of a winding pack based on this type of conductor if the sheath is used effectively in these roles. If it is not used effectively it becomes a penalty to the obtainable pack current density.

The CICC also allows for solid insulation between turns of conductor and thus the opportunity for higher allowable turn-to-turn and terminal voltages. It is not possible to fully optimize the fractions of steel and insulation in the winding pack without detailed knowledge of the mechanical loads, internal pressures, fault conditions, etc.; but experience from previous designs where

manufacturability, tensile load sharing, and support of centering loads by the conductor sheath were primary considerations suggesting that 20 to 25% of the winding pack should consist of steel and 15 to 20% insulation. We choose $f_{\text{steel}} = 22\%$ and $f_{\text{insulation}} = 15\%$ for this study. The remaining 63% consisting of helium, superconductor, and copper stabilizer (aluminum has been shown to be inferior in a high radiation environment),⁽⁴⁰⁾ must be tailored to the requirements of heat removal, protection, and stability.

Heat Removal

Heat is removed in a CICC by introducing single-phase helium at some temperature T_{in} and forcing it through the conductor at a sufficient flow rate that the maximum temperature in the flowpath does not compromise the stability required. Of course the flow cannot be increased without limit because frictional losses become significant, and any further increase in flow gives an increase in the maximum temperature in the flowpath.^(38,41) The alternative is shorter, more numerous flowpaths with the consequence of more complex manifolding for injection and removal of the helium. Good advantage can be taken of the high heat capacity of helium in the pressure range just above the critical pressure, e.g., in the range from ~300 to 500 kPa, both for steady heat removal and for stabilization.⁽³⁸⁾ Shorter channels that allow for sufficient flow, but with low enough pressure drop to stay in this range over an entire flowpath, are an attractive design option.

To test our ability to adequately remove the heat, some assumptions must be made about the distribution of heat in the windings. First, even with shielding of uniform thickness between the plasma and the circumference of the TF coils, the circumferential distribution of heat will not be uniform. This is because the outboard legs of the TF coils represent only a fraction of the total cross section intercepting the radiation of the outer plasma surface and because they are further from the plasma than are the inboard legs. The combination of effects should reduce the radiation heat load at the midplane of an outboard TF coil leg by one-tenth when compared to the maximum at the midplane of the inboard leg. Second, calculations with "typical" materials of a winding pack suggest that heating rates should fall off exponentially with depth into the pack with a characteristic (e-folding) depth of 9 to 10 cm.⁽⁴²⁾

These assumptions can be combined in the following simple form for an approximate distribution of heating in a TF coil that will be useful for estimating purposes:

$$\dot{q}_n(r, \theta) = \dot{q}_{\max} \exp[-(r - R)/0.095] \left\{ \sin(\theta/2) + (1/10)[1 - \sin(\theta/2)] \right\},$$

where R is the equivalent inner radius of a "dee" coil topologically deformed into a circle, and the angle θ is measured from a radius through the midplane of the outer leg.

From this distribution we estimate that the average heat load into a turn of conductor in the innermost layer is

$$\langle \dot{q}_n \rangle = 0.67 \dot{q}_{\max} \approx 4.7 \text{ kW/m}^3.$$

Later, when a determination of the useable winding pack current density has been made, and the winding pack dimensions have been set, we can use the same distribution function to obtain an approximation of the total radiation heat load per coil. However, of more immediate interest is whether the average heat load per turn on the inner layer of the winding can be accommodated. Using the approximate techniques of Ref. 38, it can be shown that this load can be absorbed with a temperature rise of <0.4 K in that turn by appropriate manifolding to provide the required helium flow. The conductor must be rather "open", however, and a minimum void fraction of 40% in the cable space of the CICC is suggested to preclude too much pressure loss.

Stability

At a particular current density and field, the stability margin provided by a CICC can be expressed in the form

$$\Delta H = S_{\text{He}} \frac{f_{\text{He}}}{f_{\text{cond}}} (T_{\text{cs}} - T_b),$$

where S_{He} is the effective volumetric heat capacity of the helium between the initial bulk fluid temperature T_b and the current sharing temperature T_{cs} of

the superconductor. The ratio of the helium fraction f_{He} to conductor fraction f_{cond} in the cable space merely refers to the sudden absorbed energy that will just quench the conductor to a unit volume of conductor. The above relation is essentially a statement of experimental observation that, to first order, the stability of a CICC is derived from the heat capacity of the interstitial helium over the temperature range that the conductor is superconducting.⁽⁴³⁾

The temperature dependence of the critical current density of a superconductor at the operating field is sufficiently linear over the range of interest that it can be fully represented for our purposes by the intercepts J_{e0} and $T_c(B)$. The above can then be cast in the dimensionless form,

$$\Delta h = \frac{\Delta H}{S_{\text{He}} T_c} = \frac{(1 - f_{\text{cond}})}{f_{\text{cond}}} \left[\frac{(T_c - T_b)}{T_c} - \frac{J}{f_{\text{cond}}(1 - f_{\text{Cu}})J_{Co}} \right],$$

where J is the current density inside the cable space of the CICC and f_{Cu} is the fraction of copper in the conductor strands (note that $J_{\text{pack}} = 0.63 J$ for the fractions of steel and insulation assumed). In this form the dimensionless stability parameter Δh has the properties of the coolant factored out, leaving only those parameters describing the conductor and the operating conditions.

Note that the above relation does not contain the resistivity of the copper stabilizer since, to first order at least, the stability margin is observed to be independent of it.⁽⁴⁴⁾ Rather, at a given current density, the conductor design offering the maximum stability is established by a tradeoff between adding more superconductor and adding more helium. The amount of superconductor, of course, depends on the amount of copper contained in the conductor strands (for reasons other than direct stabilization). The optimum tradeoff thus depends on this fraction of copper, as can be seen by examining contours of Δh in the $(f_{\text{cond}}, f_{\text{Cu}})$ plane. A typical mapping is shown in Fig. 2-16 with the locus of optimum f_{cond} for fixed f_{Cu} . There are, of course, limits to the amount of copper that can be used.

Fig. 2-16. A typical mapping of the stability parameter Δh in the $(f_{\text{cond}}, f_{\text{Cu}})$ plane. For the case displayed, $B = 6.21$ T, $J = 68$ A/mm², $T_b = 4.9$ K, $J_{\text{Co}} = 5200$ A/mm², and $T_c = 6.53$ K. The locus of optimal f_{cond} for given f_{Cu} is shown for clarity.

Minimum Copper Fraction

The processing of NbTi into practical superconducting wire requires a minimum amount of copper to be co-drawn with it. The limit is not absolute, but very few manufacturers regularly make good wire with less than 50% copper. We will take this number as a minimum for this study, recognizing that other considerations will probably call for higher fractions anyway.

Effect of Damage to Copper Stabilizer

The proposed level of radiation will cause an increase in the residual resistivity of the copper matrix according to a relation of the following form:

$$\Delta\rho_0 = s[1 - \exp(-iD/s)] ,$$

where s is the saturation level and i is the saturation rate. Sawan⁽⁴⁵⁾ gives $s = 3 \text{ n}\Omega\cdot\text{m}$ and $i = 720 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$ while Klabunde, et al.⁽⁴⁰⁾ gives $s = 4 \text{ n}\Omega\cdot\text{m}$ and $i = 649 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$. We use $s = 3.46 \text{ n}\Omega\cdot\text{m}$ and $i = 696 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$ as a compromise. Magnetoresistivity is accounted for by the relation⁽⁴⁶⁾

$$\rho(B) = \rho_0 \left[1 + 0.0339(B/\rho_0)^{1.07} \right] ,$$

where ρ_0 includes the effects of radiation damage.

Limiting Current

The limiting current density in a CICC is the value below which full stability is available even with initially stagnant helium inside,⁽⁴⁷⁾ and as such may be viewed as a good indicator that heat transfer from the cable to the helium is good, even without being augmented by net flow. Therefore, choosing a conductor configuration for which the limiting current density J_{lim} exceeds the design value for the cable space current density J amounts to choosing a design that is not limited by heat transfer. Towards the end-of-life, as copper damage accumulates to reduce J_{lim} , the loss will be offset by the helium flow already required for heat removal. In Fig. 2-17, contours of the stability parameter have been overlaid with contours of $J_{lim} = J$ for both initial and end-of-life stabilizer resistivities. In the present study we will observe the constraints on conductor configuration (i.e. f_{cond} and f_{Cu}) due to the former and ignore those due to the latter.

Protection

There are two major consequences that must be considered when providing for the protection of a CICC coil in the event of a quench: maintaining a safe maximum pressure inside the conduit and maintaining a safe maximum hot spot temperature of the conductor. An experimentally based, worst-case prediction shows that the former depends strongly on distance between flow connections.⁽⁴⁸⁾ To facilitate removal of the radiation heat loads, that distance will necessarily be short, and high quench pressure should not be a

Fig. 2-17. The mapping of the stability parameter for the conditions of Fig. 2-16 with contours of $J = J_{lim}$ added. Curve (A) is the locus of optima from Fig. 2-16. Curve (B) is the contour for $J = J_{lim}$ with the same copper and field but damage of 0.2 dpa.

severe problem. Maximum hot spot temperature during a quench is more crucial and will in fact be the dominant limitation on the winding pack current density for the present case.

The upper limit to the cable space current density in terms of the allowed maximum hot-spot temperature can be determined from the following expression:

$$J_{\text{prot}} \leq \sqrt{(2/\tau)} \left[(1 - f_{\text{cond}}) f_{\text{cond}} f_{\text{Cu}} \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{He,init}} c_{v,\text{He}}}{\rho_{\text{Cu}}} dT \right. \\
+ f_{\text{Cu}}^2 f_{\text{cond}}^2 \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{Cu}} c_{\text{Cu}}}{\rho_{\text{Cu}}} dT \\
\left. + (1 - f_{\text{Cu}}) f_{\text{Cu}} f_{\text{cond}}^2 \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{s.c.}} c_{\text{s.c.}}}{\rho_{\text{Cu}}} dT \right]^{1/2},$$

where μ_i and c_i are the density and heat capacity, respectively, of a particular component (helium, copper, or NbTi) of the CICC, and ρ_{Cu} is the electrical resistivity of the copper in the conductor. The dump time constant τ is given simply by

$$\tau = 2 E_d / V_d I_{\text{op}},$$

where E_s is the stored energy per TF coil, V_d is the dump voltage across the terminals of an individual coil, and I_{op} is the operating current. Experiments simulating a fully quenched CICC (Ref. 48) have shown that the initial density of the helium and the constant volume heat capacity of the helium (essentially constant and approximately equal to $3.1 \text{ kJ/kg}^{-1} \cdot \text{K}^{-1}$) are the appropriate choices for the values in the first term of the above equation (as indicated).

We are free now to make several choices that are important to our estimation of the protection limit to J . The values we choose for T_{max} , V_d , and I_{op} reflect our personal assessments of the current state-of-the-art and future trends in the technologies impacted by these choices. Selecting $T_{\text{max}} \approx 100 \text{ K}$ ensures that differential thermal expansion of the coil components in the event of a quench will be negligible. A maximum coil terminal voltage $V_d \approx 5 \text{ kV}$ should be accommodated easily by a CICC design that provides solid insulation devoid of helium between conductor turns. Coil current $I_{\text{op}} \approx 30 \text{ kA}$ will ease the protection problem without penalizing the design with a too high refrigeration load (in comparison to that from the radiation heating) or an

intractably large and difficult-to-wind conductor (especially if the operating current density can be kept high). These choices are all conservative in that they can all be pushed higher by innovative development (translate - research dollars), but first let us examine the consequences of these choices.

In Fig. 2-8 the contours of $J = J_{\text{prot}}$ are overlayed on contours of the stability parameter and the other limits already discussed. The lower corresponds to undamaged copper (i.e. RRR = 100 and magnetoresistance appropriate to the operating field), the next corresponds to copper damage of 0.005 dpa (1-fpy operating); and the highest to 0.2 dpa (40 fpy). With the design philosophy of selecting the conductor configuration giving the maximum stability consistent with all other limits and constraints, the conductor design proceeds as follows: Select a trial value of J and produce a mapping of the stability parameter such as that in Fig. 2-18. Move from the upper right to the lower left along the locus of optimum f_{cond} (increasing stability) until the contour defining some constraint is met. Move down along this constraint contour so long as stability continues to increase or until another constraint is met. Then move down along the next contour. Eventually, either a maximum available stability or the minimum copper fraction will be reached. If the resulting stability margin is higher than needed, a higher value of J can be chosen and the selection process repeated.

This selection process is easy to automate and Figs. 2-19, 2-20, and 2-21 show the results of varying the cable space current density J and extracting the corresponding values of f_{Cu} , f_{cond} , and Δh . The resultant copper fraction remains acceptable over the entire range examined. However, each trajectory eventually exceeds the maximum allowed conductor fraction (remember, we had decided to maintain 40% void to alleviate heat removal). A minimum acceptable stability parameter has also been passed (we have somewhat arbitrarily chosen $h = 0.066$ corresponding to a stability margin of 300 mJ/cm^{-3} of conductor).

As might be expected, the trajectory in each case corresponding to the highest copper damage gives the lowest allowable current density. The lowest limit to cable space current density by this design methodology is $J = 59 \text{ A/mm}^2$ ($J_{\text{pack}} = 37 \text{ A/mm}^2$) corresponding to the intersection of the limit on f_{cond} and the trajectory for a copper damage of 0.2 dpa. It can be seen by inspection that the portion of the trajectory at the intersection corresponds to points on the boundary for $J = J_{\text{prot}}$. The same is true for the trajectory

Fig. 2-18. The mapping of the stability parameter for the conditions of Fig. 2-16 with contours of $J = J_{\text{prot}}$ and minimum copper added. Curves (A) and (B) are again the locus of optima and $J = J_{\text{lim}}$ with undamaged copper, respectively. Curve (C) is the minimum acceptable copper fraction, 50%. Curve (a) is the contour for $J = J_{\text{prot}}$ with undamaged copper. Curves (b) and (c) were calculated with copper damage of 0.005 and 0.2 dpa, respectively.

Fig. 2-19. Variation of optimum copper fraction f_{Cu} with cable space current density J . Curves (a), (b), and (c) correspond to undamaged copper, 0.005 dpa and 0.2 dpa, respectively.

Fig. 2-20. Variation of optimum conductor fraction f_{cond} with cable space current density J . Curves (a), (b), and (c) correspond to undamaged copper, 0.005 dpa and 0.2 dpa, respectively.

Fig. 2-21. Variation of maximum obtainable stability parameter Δh with cable space current density J . Curves (a), (b), and (c) correspond to undamaged copper, 0.005 dpa and 0.2 dpa, respectively.

for copper damage of 0.005 dpa (but not for the undamaged copper trajectory). Thus we see that for the levels of radiation being examined in this study, the winding pack current density is simultaneously being limited by considerations of heat removal and protection. Stability and minimum copper fraction are not primary constraints.

2.4.3 Summary and Conclusions

Table 2-4 lists the important TF coil and conductor characteristics obtained from this study. The following general conclusions can be made:

- (1) The fast neutron fluence of $4 \times 10^{20} \text{ n/cm}^2$ precludes the use of Nb_3Sn and other Al5 superconductors, and may preclude the consideration of fields much higher than 6 T.
- (2) At the "6-T" level considered here, accepting 7 MW/cm^3 radiation heating appears workable even with NbTi. However, this rate of nuclear heating may require 25 to 30 MW of electric power to operate

the cryogenic system which removes this heat. This represents considerable circulating power for a reactor producing only a few hundred megawatts electric and is a strong impetus to increase the nuclear shielding of the TF coils.

- (3) A decision to anneal out the copper damage at 1-fpy intervals should allow the pack current density to be about 15% higher.
- (4) The minimum number of coils based on acceptable field ripple appears to be 18 unless alternate ripple reduction techniques are found.
- (5) Even with 18 coils, the stored energy per coil, together with the anticipated radiation damage to the copper, makes coil protection a critical issue in setting the winding pack current density.

Table 2-4. TF Coil and Conductor Summary

<u>TF Coil Characteristics</u>	
$B_{\max}$	6.21 T
T_{inlet}	4.5 K
T_{outlet}	4.9 K
E_s	273 MJ/coil
V_d	5 kV/coil
I_{op}	30 kV
$T_{\text{max,quench}}$	100 K
J_{pack}	37 A/mm ⁻² (0.2 dpa) 43 A/mm ⁻² (0.005 dpa)
<u>Conductor Characteristics</u>	
Type	Cu:NbTi, cable-in-conduit conductor (CICC)
f_{insul}	0.15
f_{steel}	0.22
f_{cond}	0.38
f_{He}	0.25
J_{co}	5200 A/mm ⁻² at 6.21 T
T_c	6.53 K at 6.21 T
d_w	0.7 mm
I/I_c	0.31 (0.2 dpa) 0.33 (0.005 dpa)

2.5 Reference Reactor

We selected an aspect ratio $A = 6.0$ for this study for several reasons. First, it appears easier to attain the second stability region at large A ,⁽⁸⁾ and, of course, the R^{-1} dependence of toroidal field argues for large A designs to reduce the maximum field needed. Secondly, the fast wave at low frequencies can be used without thermal ion cyclotron damping,⁽⁵⁾ provided $A \geq 5$. Also, it might be easier to build and maintain the torus at larger A than for conventional designs. In addition, there may be new flexibility in design optimization (e.g. coolant and cryogenic headers and manifolding in the central hole) at large A . On the other hand, A may not be increased so far that the minor radius is smaller than the minimum needed for good confinement. Our choice appears a priori to provide a reasonable compromise among those considerations.

The major radius was chosen to be $R_0 = 5.25$ m. Based on INTOR studies, this may be close to the minimum size to achieve ignition in a device with a large aspect ratio. At low wall loads this torus will provide ≤ 300 MW net electric power and would represent a near-term tokamak reactor. At the same R_0 and A but higher wall loads this device could be a STARFIRE-class reactor, producing in excess of 1000 MW electric power.

The choice of beta (β) is most arbitrary at the present moment since the upper limit to β is quite uncertain in the theory of the second stability regime. A cursory study of the benefits of high beta was performed for the reference reactor. Assuming fusion power $P_f = 866$ MW, there is a great incentive to achieve $\beta > 0.10$; for example, $\beta = 0.05$ requires a maximum toroidal field $B_M = 14.5$ T, whereas $\beta = 0.25$ only needs $B_M = 6.5$ T. Assuming TF magnet costs of 1.3¢/Joule, the $\beta = 0.05$ reactor would need TF coils totaling over \$300 M, while the $\beta = 0.25$ reactor has coils costing only \$65 M. High beta also improves the prospects for liquid-metal coolants. Thus, at $\beta = 0.05$ we would expect MHD pressure drops of ~ 2.5 MPa, while at $\beta = 0.25$ this reduces to ~ 0.5 MPa for the inboard leg of a lithium-cooled blanket. Based on these observations and calculations of stable equilibria as high as $\beta = 0.40$,⁽⁸⁾ we selected a reference value of $\beta = 0.25$.

An effort was made to design a pusher coil to achieve indented, bean-shaped equilibria, which theoretically provide access to the second stability regime. We could not design an attractive field shaping system to achieve

indentations of $i \geq 0.2$, because the coils would be very large and difficult to shield, due to their proximity to the inboard first wall. Fortunately, extensive MHD studies have shown that indentation may not be necessary to achieve second stability,⁽⁹⁾ and we feel that our reference equilibrium will require only a simple equilibrium field coil (EFC) design. We have selected a moderately large elongation ($\kappa = 2.0$) and moderate triangularity (in the range $d = 0.25-0.50$). Future studies will address the possibility that κ may be further reduced; we note, for example, that ballooning modes stabilize even for circular plasmas when $q_a > 1.5$.⁽⁴⁹⁾ Kink modes also tend to stabilize as q_a is raised above unity.^(8,30) Thus we expect that keeping the safety factor fairly high may be the most desirable method of achieving stable high beta operation rather than forming highly shaped plasmas. For the equilibrium described in Sec. 2.3 the toroidal current is $I_0 = 4.3$ MA for the reference reactor with $B_0 = 3.8$ T (corresponding to $P_f = 1270$ MW). Raising the safety factor to $q_a \approx 1.2-1.5$ should result in still smaller I_0 , although we will desire to keep $I_0 \geq 7$ MA/A in order to assure good fast alpha particle confinement.⁽⁵⁰⁾ For such an equilibrium we expect a modest EFC system, and we estimate its stored energy will be $U^{EF} \approx 0.2$ GJ, about 1/50th of the STARFIRE value.

We note that a conducting first wall will help stabilize external kinks and we are hopeful that the small scrape-off distance ($\Delta_v \approx 0.08$ m) in the reference design will accomplish this. The liquid-metal breeding blanket will also aid in kink stabilization.

In order to achieve steady-state operation with ideal and resistive MHD stability and $q_a > 1$ it will be necessary to have pressure and current density profile control. As presented in Sec. 2.2, we propose to use the fast wave current drive technique to achieve this. This driver is recommended on several counts: the frequency ($\sim 50-400$ MHz) is compatible with ICRH or ion Bernstein wave heating to ignition; compared to the slow wave there is no accessibility limitation; the convenient use of high phase speeds ($\omega/k_{\parallel} v_e > 1$) avoids trapped electron effects; at high electron temperatures, T_e , transit time magnetic pumping will augment the Landau damping-driven current density; and the hardware is standard and promises reasonably high electric-to-rf power conversion efficiency. Preliminary studies have shown that alpha particle damping will not be a serious problem for the fast wave. An outstanding issue

at this time is whether the optimum coupler at these frequencies will be loop antennas or ridged, slanted, or re-entrant waveguide arrays.

The electron temperature remains as an important parameter to consider. Generally with rf-driven reactors one desires to operate at $T_e \approx T_i$ above the optimum for fusion power density, in order to reduce the current drive power (at lower electron density, for fixed β). Of course, in order to keep a constant P_f at higher temperatures one must increase the toroidal field strength. In varying T_e and T_i there is thus a tradeoff between the rf-current drive system cost and the TFC system cost. For the current driver power found in Sec. 2.2 and assuming typical rf-system and TFC capital costs we find the optimum $T_e \approx 19$ keV. The total costs are relatively flat over a large range, $14 \text{ keV} \lesssim T_e \lesssim 22 \text{ keV}$.

The reference reactor parameters are summarized in Table 2-5, based on calculations with the Tokamak Reactor Analysis Code (TRAC II). Figure 2-22 displays the reduced size of the TPSS design, in contrast to STARFIRE. The thermal efficiency assumes a high temperature vanadium/lithium blanket, which represents significantly better thermodynamic performance than was possible with STARFIRE. By varying the maximum field strength, B_M , the power density and wall load are varied in a torus of fixed size. Thus the table displays a variety of possible reactor performance levels, depending on the neutron wall loading (W_n) deemed appropriate. If the upper range, $W_n \approx 8.0 \text{ MW/m}^2$, is attainable without undue penalty in availability and safety then performance becomes very impressive.

Reactor mass is reduced and maintenance simplified by opting for a minimum thickness blanket/shield with wrap-around TF coils. Component mass is estimated and given in Table 2-3; we expect the total reactor mass to be ~ 4000 Mg. Depending on the neutron wall load selected, we find the mass power density can be quite high, well in excess of 100 kW electric per ton. The achievement of such a compact design, with a close-fitting first wall and thin blanket/shield will depend on improvements in other areas, such as impurity control. The tokamak improvements in these areas are described in the chapters which follow.

Table 2.5. Reference Reactor

Quantity	Variable	Units	Value						
Aspect ratio	A		6.0						
Elongation	κ		2.0						
Triangularity	d		0.5						
Beta	$\bar{\beta}_t$		0.25						
Inboard blanket/shield	$\Delta_{B/S}$	m	0.95						
Outboard blanket/shield	$\Delta_{B/S}$	m	1.65						
Vacuum scrapeoff	Δ_v	m	0.08						
Major radius	R_0	m	5.25						
Minor radius	a	m	0.875						
Plasma volume	V_p	m ³	150.						
First wall area	A_w	m ²	299.						
Blanket neutron energy multiplier	M_B		1.2						
Thermal efficiency	η_{th}		0.45						
Current drive efficiency	η_d		0.55						
Pressure profile	α		1.4						
Density profile	α_n		0.3						
Beryllium fraction	n_{Be}/n_{D-T}		0.04						
Thermal power	P_{th}	MW	1055	1263	1559	1735	2307	3579	
Fusion power	P_f	MW	845	1024	1275	1425	1913	3000	
Alphas power	P_α	MW	169	205	255	285	383	600	
Neutron power	P_n	MW	676	819	1020	1140	1530	2400	
Neutron wall load	W_n	MW/m ²	<u>2.26</u>	<u>2.74</u>	<u>3.40</u>	<u>3.80</u>	<u>5.13</u>	<u>8.03</u>	
Plasma power density	P_f	MW/m ³	5.63	6.83	8.50	9.50	12.8	20.0	
Electron temperature	$\bar{T}_e$	keV	19.0	19.0	19.0	19.0	19.0	19.0	
Ion temperature	$\bar{T}_i$	keV	26.1	26.0	25.9	25.8	25.6	25.4	
Electron density	$\bar{n}_e$	10 ²⁰ m ⁻³	1.49	1.61	1.77	1.86	2.13	2.63	
Deuterium density	$\bar{n}_D$	10 ²⁰ m ⁻³	0.562	0.620	0.692	0.731	0.852	1.07	
Vanadium fraction	$\bar{n}_V/\bar{n}_{D-T}$	10 ⁻¹	42.7	29.9	21.3	18.0	8.5	0.67	
$n_D \tau_D^E$		10 ²⁰ s/m ³	2.5	2.5	2.5	2.5	2.5	2.5	
τ_D^E		s	4.4	4.0	3.6	3.4	2.9	0.95	
τ_e^E		s	0.7	0.6	0.5	0.5	0.4	0.14	
Alpha fraction	$\bar{n}_\alpha/\bar{n}_{D-T}$		0.035	0.035	0.035	0.035	0.034	0.034	
Plasma current	I_0	MA	3.61	3.78	3.99	4.10	4.41	4.93	
Field on axis	B_0	T	3.46	3.63	3.83	3.93	4.23	4.73	
Field on TFC	B_M	T	5.43	5.69	6.00	6.17	6.65	7.43	
CD power (FWCD)	P_d	MW	72.	73.	80.	82.	88.	99.	
Gross electric	P_g	MW	475.	568.	702.	781.	1038.	1611.	
MHD pumping	P_{MHD}	MW	~1	~1	~1	~1	~1	~2	
Net electric	P_{net}	MW	332.	417.	540.	613.	854.	1393.	
P_f/P_d	Q		11.7	13.5	15.9	17.4	21.7	30.3	
Net thermal efficiency	P_{net}/P_{th}		0.31	0.33	0.35	0.35	0.37	0.39	

Reactor Design

Fig. 2-22. Comparison of improved tokamak developed in the ANL TPSS effort with STARFIRE.

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CHAPTER 3

IMPURITY CONTROL

CHAPTER 3

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3. IMPURITY CONTROL

3.1 Introduction and Summary

The TPSS effort in impurity control focused on innovative systems that suggested substantial improvements to tokamak reactor attractiveness. The main concepts examined were lithium cooled, self-pumped limiters. An initial engineering design, plasma engineering and neutronics analysis, and a materials assessment were carried out for the self-pumped systems. Much of the engineering analysis is applicable to any lithium cooled limiter.

The self-pumped limiter uses vanadium or certain other materials (Ni, Fe, Ta, etc.) to selectively trap impinging helium from the plasma, *in-situ*, on the surface. No vacuum ducts or pumps are used (except for a very small startup system). Vanadium or other trapping material is added to the surface at an average rate of 3.3 times the α production rate. Trapping material can be added by injecting pellets into the edge or scrapeoff plasma where it is ablated and transported to the trapping surface.

Although there are clear uncertainties, due to the lack of reactor relevant data, the self-pumped limiter appears promising for improving the tokamak reactor. These improvements are a cost savings, of the order of 125 M\$ for a STARFIRE size reactor (~ 100 M\$ in reduced shielding costs and ~ 25 M\$ in reduced tritium system costs), a substantial increase in the mass utilization factor, a long (10 year) limiter lifetime, a reduction in tritium processing and inventory, and the elimination of several reactor components.

An assessment was made of key trapping material parameters. We estimate an average tolerable helium concentration of 30 at% and a maximum operating temperature of 0.7 of the melting point. Self-sputtering calculations indicate that the presence of helium lowers the self-sputtering coefficients for a given energy. For vanadium containing 30% helium, the unity-self-sputtering coefficient energy threshold is raised to ~ 1500 eV from ~ 1000 eV for the pure material. This causes a corresponding relaxation in the plasma edge temperature requirements. In general, an edge temperature of $\lesssim 100$ eV would be sufficient to minimize erosion on the medium to high-Z materials considered.

Two embodiments of the self-pumped limiter appear most promising, a first wall limiter and a slot limiter. The first wall limiter combines the func-

tions of first wall and limiter into a single wall, shaped to conform to the plasma edge flux. The limiter is made of vanadium structural alloy. Helium trapping occurs on a 300 m^2 surface to which pure vanadium is added at a rate of 1.5 mm/yr (for the $q_N = 2.5 \text{ MW/m}^2$ case). Vanadium can be added to the plasma scrapeoff zone, and/or edge region, without apparent deleterious effect for non-neoclassical impurity transport.

The slot self-pumped limiter consists of a $\sim 60 \text{ m}^2$ front face, two leading edges, and a slot region. The limiter is toroidally continuous, and can be located at various poloidal locations, e.g., the outer midplane. Helium trapping is done in the slot region to which trapping material is added. Most of the heat flux falls on the front face. The slot system essentially separates the heat removal from the particle removal. A lithium cooled, tantalum structural limiter was designed. Trapping materials of vanadium, nickel, or iron appear suitable. The trapping material is added via pellet injection in the slot plasma where it is ionized and transported to the slot surfaces. A potential advantage of the slot system is that the main heat load is separated from the trapping sites. Also, the material injection is performed away from the main plasma.

Both limiter designs use a combination of high velocity lithium flow and a reduction in the length of heated sections in the high surface flux regions. The combination of these two characteristics results in reduced coolant residence time in the high heat flux region which is essential for liquid-metal cooled first wall/ blanket and limiter. Both designs employ electrical insulators with a laminated structure to reduce the pressure drop to acceptable levels.

The lifetime of the self-pumped limiters was assessed. The lifetime of the first wall exceeds the design goal of 10 years, at a neutron wall loading of 2.5 MW/m^2 . A long life integrated structure combining the functions of impurity control, first wall, and blanket components is therefore possible, which may have significant implications for tokamak reliability. For the slot limiter, thermal stress was found to be the most limiting factor. A 10 year lifetime was also found to be possible with the slot limiter using a total of 3 cm of trapping material on each of the two slot surfaces.

The following sections of Chapter 3 cover the various aspects of the TPSS impurity control studies. Section 3.2 describes the general features of self-

pumping and the systems considered for this study. Section 3.3 discusses plasma engineering considerations for self-pumping. These include the requirements and effects on the plasma of trapping material injection, edge temperature requirements, and shaping and heat flux calculations. Section 3.4 describes the neutronics analysis for the liquid lithium TPSS design with self-pumped limiters. This covers the topics of tritium breeding and shielding requirements. Section 3.5 describes the engineering design for the first wall limiter and slot limiter systems. Thermal hydraulics, stress and MHD analysis are described. Section 3.6 discusses materials related issues particularly sputtering coefficients of helium saturated material, hydrogen permeation and retention rates, and temperature limits for helium trapping.

3.2 Self-Pumped Limiter Concepts

The self-pumping concept was proposed⁽¹⁾ as a means of simplifying impurity control in a fusion reactor. The basic concept is to remove helium in-situ by trapping in freshly deposited metal surface layers of a limiter or divertor. A key requirement is for the deposited material to trap helium much better than hydrogen. It has been demonstrated experimentally that nickel preferentially traps helium⁽²⁾ and that several other materials (iron, vanadium, niobium, molybdenum, and tantalum) are believed to be capable of preferential trapping. The selective trapping in certain metals is a result of the negligible solubility of helium in the lattice. The injected helium will diffuse through the lattice until it reaches a nearby trapping site where it can come out of solid solution. Hydrogen, on the other hand, remains in solid solution until it diffuses to the surface and escapes. Other plasma contaminants, notably oxygen, can be removed by the self-pumping system by chemically combining with the deposited metal.

Protium, formed by the D-D reaction, also needs to be removed. It has been calculated (see Section 3.6) that diffusion into the coolant, is more than adequate to remove the protium.

Benefits of the self-pumping system are the elimination of vacuum ducts, pumps, and penetration shielding (except for a very small startup system), and the reduction of tritium recycle and refueling. In addition, a self-pumped system may perform better and last longer than alternative systems such as a pumped limiter. Better performance could result, for example, from the potentially higher removal efficiency, ~ 25% compared to ~ 5%, for pumped limiters.

Because of the potential benefits for the tokamak reactor the self-pumped system was selected for examination for the TPSS study. A limiter was chosen for the study instead of a divertor because it has certain reactor advantages (reduced costs and less mechanical complexity), however, we anticipate studying a self-pumped divertor in the future.

An optimistic goal was set for the impurity control system for TPSS that the system last as long as the first wall/blanket. For the case of a reactor with ~ 900 MW fusion power ($q_n = 2.5 \text{ MW/m}^2$), this lifetime is 10 calendar years, at 75% availability. Achieving this goal would mean that there would

be no scheduled replacements or maintenance of tokamak core components for 10 years.

There are a number of requirements for the self-pumping material; these are discussed generally in Ref. (1). Two key requirements pertaining to lifetime are (1) the fraction of helium capable of being trapped in the material, and (2) the maximum surface temperature at which the material will still trap helium.

Based on extrapolation from limited experimental data, and from theoretical arguments (See Section 3.6), we have estimated these parameters as an average trapping fraction of 30% and a maximum temperature of 0.7 of the melting point. These are optimistic but hopefully realistic values which need to be verified by experimental work. Based on these parameter values it appears possible to meet the design lifetime goal.

We have examined three embodiments of the self-pumped limiter. These are: (1) first wall/limiter; (2) slot limiter; and (3) localized-front face limiter. Basic characteristics of these designs are listed in Table 3.2-1. Further characteristics for the first two concepts are listed in Tables 3.2-2 and 3.2-3.

The first wall/limiter concept combines the functions of first wall and limiter into a single first wall structure. The wall is shaped in accordance with the outermost plasma flux surface. Trapping material is added to the plasma scrapeoff or edge region where it is transported to the wall. The entire wall area is used for helium trapping.

The slot limiter is similar to a pumped limiter except that there are no vacuum ducts. The limiter is toroidally continuous and located at one poloidal location. Helium trapping is done in the slot region to which trapping material is added. Most of the heat flux is taken on the front face. The slot system essentially separates the functions of helium removal and heat removal. Also, material added to the slot plasma is more likely to be kept out of the main plasma.

The third concept studied is a localized front face limiter. This is a variant of the first wall/limiter except that the limiter area is a small fraction ($\sim 20\%$) of the first wall area. Trapping is done on the front face. π leading edges are used. The front face is shaped to gradually curve

TABLE 3.2-1. GENERAL FEATURES OF SELF-PUMPED LIMITER CONFIGURATIONS

	First Wall/Limiter	Slot Limiter	Localized Front Face Limiter
Front face area, m ²	300	60	60
Helium trapping site	Front face	Slot region	Front face
Trapping site area, m ²	300	120	60
Heat flux (radiation plus transport) on trapping site, MW/m ²	$.75 \times \frac{q_n}{q_o} \text{ (a)}$	$\lesssim 0.1$	$1.5 \times \frac{q_n}{q_o} \text{ (b)}$
Trapping material selection criteria:			
Importance of a) thermal properties	High	Low	High
b) neutronics properties	High	Moderate	Moderate
Structural material - typical	V alloy	Ta alloy	V or Ta alloy
Candidate trapping materials - typical	V	Ni, Fe, V	V, Ta, W
Feasible plasma edge temperature, eV	$\lesssim 100$	$\lesssim 100$	$\lesssim 100$
Structural thickness, front face, mm	2	2	2
Trapping site growth, mm/yr ^(c)	1.5	3.75	7.5
Advantages	Simple, integrated wall/limiter structure. Large area available for trapping.	Heat removal issue essentially separated from particle removal. Injected material more likely to be kept out of main plasma. Less sensitive to plasma edge conditions. More easily replaceable than first wall/limiter.	More easily replaceable, if necessary, than first wall limiter.
Disadvantages	Plasma contamination issue, sensitivity of shape to edge conditions, may interfere with RF system.	Leading edges make for more complex structure.	Limited trapping area with higher heat load than first wall/limiter.

(a) q_n = neutron wall loading, q_o = 2.5 MW/m².

(b) For 75% radiation fraction, 25% transport fraction.

(c) For vanadium, q_n = 2.5 MW/m², 75% availability.

TABLE 3.2-2. FIRST-WALL-SELF-PUMPING LIMITER - TYPICAL PARAMETERS

Parameter	Value
Type	Self-pumped limiter 100% of first wall coverage
Area	300 m ²
Structural material	V alloy
Self-trapping material	Vanadium
Coolant	Li
Final coating thickness	1.5 cm
Average surface heat load ^a (transport + radiation + nuclear)	0.9 MW/m ²
Final surface temperature	~ 0.7 T _{melt}
Impurity injection method	Scrapeoff and/or edge injection
Helium removal efficiency	25%
Lifetime ^a	10 yrs

^a For $q_n = 2.5 \text{ MW/m}^2$, 75% availability.

away from the plasma thus minimizing the heat flux. The motivation behind this system is that if frequent replacement is required it is easily done, compared to replacing the entire first wall.

Because of the large area of the first wall/limiter, neutronics effects are of key importance. Tantalum is not acceptable for this system because of its effects on tritium breeding, however it is acceptable for the smaller limiters. The range of desirable plasma edge temperatures is the same for each concept. In general a low edge temperature regime is desirable to minimize sputtering. Further details of the concepts and parameters in Tables 3.2-1 through 3.2-3 are discussed in the following sections of Chapter 3.

REFERENCES FOR SECTION 3.2

1. J. N. Brooks and R. F. Mattas, J. Nucl. Mater. 121 (1984) 392.
2. A. E. Pontau, W. Bauer, and R. W. Conn, J. Nucl. Mater. 93&94 (1980) 564.

3.3 Plasma Engineering

3.3.1 General Considerations

For a self-pumped impurity control system, the rate at which trapping (impurity) material needs to be added to the trapping surface is given by:

$$I_Z = \frac{I_\alpha}{f_\alpha}, \quad [3.3-1]$$

where I_Z is the current of trapping material, I_α is the α -particle production rate and f_α is the saturation helium concentration fraction in the material. The surface growth rate is given by:

$$d' = \frac{I_Z}{A\rho}, \quad [3.3-2]$$

where A is the surface area and ρ is the atomic density of the deposited material. Typically, we expect $\rho = 0.8 \rho_0$ where ρ_0 is the theoretical density, i.e. the deposited material will be 80% dense. We also estimate that thermal conductivity of the deposited material will be reduced, to $\sim 70\%$ of theoretical. For example, for vanadium with $P_F = 887$ MW ($q_n = 2.5$ MW/m²), $A = 300$ m², $f_\alpha = 0.3$, we have $I_\alpha = 3.15 \times 10^{20}$ s⁻¹, $I_Z = 1.05 \times 10^{21}$ s⁻¹ and $d' = 1.5$ mm/yr. The latter value is based on 75% availability.

Table 3.3-1 compares the growth rate, mass addition rate, and thermal properties of vanadium and other candidate trapping materials. As shown for vanadium, some 2000 kg per year is required. (This scales linearly with fusion power.) This amount of vanadium traps about 50 kg of helium. It should be noted that the amount of material used for self-pumping is comparable to conventional systems. For example, a divertor with ~ 1 cm thick collector plates would use similar amounts of material. For the self-pumped limiter the surface starts thin and is built up over time. When the limiter is removed, the trapping material could be purged of helium (by heating) and reused if desired.

Desirable qualities of the trapping material are a high melting point and good thermal conductivity. Vanadium, molybdenum, and tantalum are acceptable in this regard. Both nickel and iron have melting points that are too low for the first wall application but are acceptable for the slot system. (Tungsten

TABLE 3.3-1. PROPERTIES OF CANDIDATE TRAPPING MATERIALS

Material	Melting Point	Thermal Conductivity ^a	Surface Growth Rate ^b for $A = 120 \text{ m}^2$	Mass Utilization ^b
V	1900 C	22 W/mK	3.8 mm/yr	2110 kg/yr
Fe	1536	23	3.2	2313
Ni	1453	44	3.0	2432
Nb	2468	35	4.8	3848
Mo	2610	107	4.3	3974
Ta	2996	44	4.8	7494

^a For redeposited material at 1000°K.

^b For $q_n = 2.5 \text{ MW/m}^2$, 75% availability.

is not considered as a prime candidate trapping material because of concerns about high hydrogen trapping, however, tungsten cannot be ruled out at this time.)

3.3.2 First Wall/Limiter Shaping

Various design shapes can be potentially used for the first wall/limiter. A typical shape is shown in Fig. 3.3-1. This design is based on a tangency point at the inner midplane and a constant heat flux shape. To generate this curve, exponential particle and transport heat flux distributions were assumed in the scrapeoff zone. The heat transport e-folding distance is $\delta_E = 2$ cm at the outer midplane. (This value of δ_E is based on the INTOR value of 1.5 cm scaled for safety factor as $\delta \sim \sqrt{q}$.) The advantage of a tangency point at the inner midplane is that it minimizes the distance between the plasma and the TF coils, i.e., no extra scrapeoff distance is required at the inner midplane. Other choices of tangency points, e.g., bottom or outer midplane, or perhaps multiple points, are equally feasible from the standpoint of self-pumping. Similarly, a constant particle flux shape or other shape could be used.

An issue with using the first wall as a limiter is design tolerance on a large structure. The sensitivity of the limiter to fabrication errors was examined. Figure 3.3-2 shows the heat load of the limiter of Fig. 3.3-1 (at time of $t = 0$) for two conditions: (a) no error, and (b) maximum errors of ± 2 cm.

In Fig. 3.3-2, the limiter points are equally spaced on the curve of Fig. 3.3-1 with point 1 at the inner midplane ($y = 0$, $x = 4.3$ m) and point 45 at the outer midplane ($y = 0$, $x = 6.32$ m). The tolerance error is defined as the distance, δ , between the desired shape and the actual shape, as follows:

$$\delta(I) = \frac{I}{I_{\max}} \delta^{\max}, \quad [3.3-3]$$

where I is the limiter point and $I^{\max} = 45$.

The heat loads shown in Fig. 3.3-2 are based on a total power (alpha heating plus RF) of 227 MW and a radiated power fraction of 75%. The increase in heat loads for $\delta^{\max} = \pm 2$ cm is tolerable. Thus, a tolerance of ± 2 cm appears to be acceptable. This is a close tolerance but is considered feasi-

Fig. 3.3-1. Typical first wall/limiter shape.

Fig. 3.3-2. Heat flux on first wall/limiter as a function of tolerance error.

ble. A lower radiated fraction would increase the effect of design tolerances while a higher fraction, or larger e-folding distances would decrease the effect. Multiple tangency points and/or field ergodization would tend to decrease the effect of tolerance errors.

The heat load on the limiter of Fig. 3.3-1 was also computed as a function of time. If the surface growth rate is assumed to be everywhere uniform (1.5 mm/yr), then the heat load changes little. A uniform growth rate could potentially be achieved by carefully controlling of the impurity deposition. A second time variation case examined assumes that the instantaneous growth rate is proportional to the DT particle flux. This might be the case if trapping material were introduced to the plasma edge region and was subsequently transported to the wall in the same way as the hydrogen. In this case, the peak heat load increases by $\sim 20\%$ in 10 years. Again, this is a reasonably acceptable value.

3.3.3 Means of Introducing Material - Front Surface Limiters

For the first wall/limiter or localized front face limiters, trapping material can be potentially added to the scrapeoff zone or the edge plasma. Scrapeoff zone injection is probably the most desirable. We have examined several means of adding material. The simplest means appears to be impurity pellets.

The rate of ablation of poorly shielding pellets is roughly governed by the relation:

$$\frac{dr_p}{dt} \text{ (m/s)} = -1.33 \times 10^4 \left(\frac{A_p}{r_p^2} \right) \frac{N_e T_e^{3/2}}{\tau N_0} f \quad [3.3-4]$$

with A_p = surface area, r_p = pellet radius, N_e = electron density, T_e = plasma temperature, f = shielding factor, τ = energy required to ablate a single atom, and N_0 = solid density (units MKS, eV). The major uncertainties in this model are the exposed surface (A_p/r_p^2) = 2π to 4π and the required energy τ/f = 0.05 to 60 eV. Experimental data favors τ/f values of 0.1 - 2 eV, which has been interpreted as τ = .005 eV and f = .005. This is, however, subject to considerable experimental uncertainty.

The main difference in impurity injection over hydrogen injection is the higher binding energy ~ 5 eV (versus 0.005 eV). Also, the shielding could be

quite a lot better because of the $\sim Z^2$ dependence of impurity radiation in the appropriate temperature range. To a first approximation, the lifetime would be longer by the ratio:

$$\frac{\tau_Z}{\tau_H} \sim \frac{5}{.005} \sim 1000 . \quad [3.3-5]$$

Thus, the pellets can go quite slowly or can be small, e.g., blown in like dust.

We have made some calculations of impurity pellet ablation in TPSS which indicate that the results are very model dependent. Qualitatively, it appears that dropping pellets/dust from the top and/or sides of the torus is sufficient for ablation and subsequent transfer to the limiter. Control over where the pellets ablate could be achieved by choosing the pellet size.

The effect of the added impurities on the plasma is discussed in the next section. In terms of the transport of material to the limiter surface, the following general points should be noted. In steady state all introduced material will impinge on the limiter. The impinging material will be in the form of impurity ions which will both stick and self-sputter. Sputtered material will tend to have short mean free paths and will be redeposited at high fractions.⁽¹⁾ It is necessary that the self-sputtering coefficient of the redeposited material be less than unity. The energy of the redeposited material, resulting from scrapeoff zone ionization (the majority), is given approximately by:

$$U_R \approx Z_L \phi , \quad [3.3-6]$$

where Z_L is the charge state and ϕ is the sheath potential. The maximum tolerable edge temperature is then set by requiring less than unity self-sputtering. For the typical values of $\phi = 3 T_e$ and $Z_L = 3$, the edge temperature limit is:

$$T_e^{\max} \approx \frac{U_1}{9} , \quad [3.3-7]$$

where U_1 is the energy for unity self-sputtering.

For pure vanadium, calculations with the ITMC code (see Section 3.6.1) predict $U_1 = 1000$ eV. For vanadium saturated with 30% helium, $U_1 = 1500$ eV. The corresponding edge temperature limits for these cases are ~ 110 and ~ 170 eV. The latter value is considerably higher than previous estimates of ~ 50 eV but is subject to numerous uncertainties regarding boundary conditions, materials properties, etc. As discussed in the next section, much lower edge temperatures may be possible in any event.

3.3.4 Effect of Trapping Material on Plasma - Front Surface Limiters

The effects of adding trapping material to the plasma and other aspects of self-pumping have been studied with the REDEP impurity transport code⁽¹⁾ and the WHIST 1-D plasma transport code. The latter work is described in Refs. 2 and 3. In Refs. 2 and 3, the reactor models used were similar in size and power to the TPSS designs considered here but were not operated in the second stability regime. Also, current drive was not specifically modeled. This would affect aspects of the 1-D analysis dealing with thermal control and stability. However, the analysis of the work dealing with hydrogen and impurity transport and edge effects are probably not significantly different. These results can be summarized as follows: (1) The effects of 100% recycling of hydrogen with a self-pumped limiter (i.e. where only helium is removed), as judged by the evolution of the density and temperature profiles, together with pellet fueling to make up for the DT burnup, are fully acceptable and in fact tend in the right direction. Both higher central temperatures and lower central densities (desirable for current drive) are predicted, as well as lower edge temperatures (desirable for minimizing sputtering). Provided some pellet fueling is employed, no hydrogen needs to be removed by the impurity control system. (2) The transport of sputtered and injected impurities depends greatly on whether such transport is governed by neoclassical or non-neoclassical mechanisms. Neoclassical transport results in unacceptable central impurity concentrations for the front surface limiters. This is also the case for many if not most impurity control schemes. Only a low-Z coated limiter with high scrapeoff shielding or a high recycling divertor might be acceptable. The negative consequences of neoclassical transport are due to the well known tendency of collisional momentum exchange to sweep impurities into the plasma interior. Fortunately, there is little or no evidence for neoclassical impurity transport. (3) For non-neoclassical impurity transport,

the total inventory of a given impurity in the plasma is found to be equal to $\sim 0.25 S_{\text{eff}}$, where S_{eff} is the total influx of sputtered and injected atoms to the plasma edge/total outflux of D-T ions. We attribute the difference of the impurity concentration from S_{eff} to the fact that the impurity source is at the plasma edge, whereas much of the fuel ion source is comprised of more deeply injected pellets (the remainder being supplied by recycling, which is also a source in the context of this discussion). Thus, a newly introduced impurity atom has a greater chance of being promptly lost than the average fuel atom and, as confirmed by the code output, the impurity confinement times are commensurately shorter. (4) By injecting vanadium into the edge region and using some RF power to control the edge region power balance, very low edge temperatures (~ 10 eV) may be feasible.

As discussed, the edge temperature limit for self sputtering may be as high as 170 eV; the optimum edge temperature is not clear at this time.

Using the transport code results together with a series of REDEP calculations, we have prepared a summary, shown in Table 3.3-2, of the likely effects of impurity injection on the TPSS plasma. This summary is for reference edge conditions of $T_0 = 30$ eV and $N_0 = 1 \times 10^{20} \text{ m}^{-3}$ and for exponential density and temperature profiles in the scrapeoff zone. For $q_n = 2.5 \text{ MW/m}^2$, the current of DT ions to the limiter is $I_{\text{DT}} = 2.5 \times 10^{24} \text{ s}^{-1}$, based on a transport power, $P_{\text{TR}} = 57 \text{ MW}$. The current of injected material is $I_Z = 1.05 \times 10^{21} \text{ s}^{-1}$ which is only 4.2×10^{-4} of the DT current. This material is added to either the scrapeoff zone or the edge. Upon reaching the limiter the material causes self-sputtering, which is computed along with the DT and helium sputtering. The scrapeoff zone has a high shielding efficiency for the sputtered atoms. This results in only a small fraction of the sputtered material reaching the edge. The values of S_{eff} shown are from the REDEP calculations. They are equal to the sum of the injected current and the sputtered impurity current reaching the edge divided by I_{DT} . The impurity concentration in the plasma is given by $0.25 S_{\text{eff}}$, based on Ref. 2 results. The values of Z_{eff} are for the values of impurity concentrations with the average charge states listed. As shown, the increase in Z_{eff} is very small for scrapeoff injection. It is small for vanadium edge injection but not small for tantalum edge injection.

TABLE 3.3-2. EFFECTS OF TRAPPING MATERIAL ON THE PLASMA FOR A FRONT SURFACE SELF-PUMPED LIMITER

Parameter*	Scrapeoff Injection		Edge Injection	
	Vanadium	Tantalum	Vanadium	Tantalum
S_{eff}^a	2.6×10^{-5}	4.1×10^{-6}	4.7×10^{-4}	4.2×10^{-4}
Impurity concentration in plasma, N_Z/N_{DT}	6.5×10^{-6}	1.0×10^{-6}	1.2×10^{-4}	1.1×10^{-4}
Z_{eff}^b	1.00	1.00	1.06	1.40
Radiation power in 30 m ³ edge region	.6 MW	.2 MW	9.0 MW	20 MW
Radiation power in 120 m ³ core plasma region ^d	.07 MW	.4 MW	1.3 MW	45 MW
Acceptability for non-neoclassical impurity transport	Yes	Yes	Yes	No
Acceptability for neoclassical impuri- ty transport	No	No	No	No

* For non-neoclassical impurity transport except where otherwise indicated.

^a For $T_0 = 30$ eV, $N_{DT} = 1 \times 10^{20} \text{ m}^{-3}$ edge conditions.

^b For charge states of V + 23, Ta + 60.

^c For $N_{DT} = 10^{20} \text{ m}^{-3}$, worst case cooling rates.

^d For $N_{DT} = 1.5 \times 10^{20} \text{ m}^{-3}$, 10 keV cooling rates.

The next two parameters provide a rough estimate of the radiation loss in the edge and core regions due to the trapping material. Cooling rates of Ref. 4 were used for this computation. There could also be an equivalent amount of radiation in the scrapeoff zone but this would require a more detailed impurity and plasma transport analysis beyond the scope of the present analysis. In the case of vanadium, the radiation in the core plasma is low for either scrapeoff or edge injection. For tantalum the core radiation is very high for edge injection, but acceptable for scrapeoff injection. Thus, tantalum would have to be well confined to the scrapeoff zone. Further, WHIST code calculations of tantalum injection have also confirmed this point. The difference between the two materials are due to the difference in atomic number.

As mentioned, none of these materials are acceptable if neoclassical impurity transport applies. Otherwise, vanadium appears to be acceptable for both injection regions. In particular, it is not critical where the vanadium is injected. For tantalum it is important to confine the injection close to the limiter surface. We conclude that tantalum is of marginal acceptability for the front surface limiters, but cannot be ruled out at the present time.

3.3.5 Slot System

The slot self-pumped limiter is shown conceptually in Fig. 3.5-2. Typical system parameters are listed in Table 3.2-3. The limiter consists of a front face, two leading edges, and a slot region. The limiter is toroidally continuous and located at one poloidal location. Additional structural members and coolant manifolds can be continuous or located at discrete toroidal locations. Helium trapping is done on both sides of the slot region. Trapping material is added to the slot plasma where it is transported to the trapping surfaces. Material is added by injecting low velocity impurity pellets into the slot plasma. General considerations for pellet ablation are similar to the front face limiter discussed previously.

The surfaces comprising the slot region are shown as straight lines in Fig. 3.5-2 for conceptual purposes. However, both the front face and slot regions would likely be shaped to conform, in some way, to the poloidal flux.

The front face and leading edges are made of a high strength, low sputtering material, e.g. tantalum. For plasma edge temperatures below the unity

self-sputtering threshold, the net erosion rate of tantalum is predicted to be very low, if not essentially zero. This is due to the very small mean free paths (< 1 mm) for neutral Ta atoms. Therefore, only a thin front face structure may be required for multiyear operation. Because the heat flux on the slot surfaces can be made, by design, fairly low, we are free to select the trapping material for other than thermal properties. Typical choices are vanadium, nickel, or iron. The lower melting point of these materials, compared to tantalum, would tend to keep them from building up on the leading edges, due to the higher heat flux there.

The heat and particle flux to various regions of the limiter depends on the plasma thermal balance and on edge transport properties. The heat flux on the front face depends on the balance between radiation and transport in the plasma. This is variable, to some extent, by such means as ripple control, RF and profile control, and injection of high-Z material. The fluxes to the slot region depend heavily on edge transport processes. The heat load on the slot surfaces can be made arbitrarily low, at the expense of lowering particle flux, by increasing the distance of the slot from the front face.

A simple 0-D model can be used to define the general operating characteristics of the slot limiter. Parameters of this model are the following:

- N_{DT}^S = slot D-T density.
- T_s = slot plasma temperature ($T_i = T_e$).
- Q_s = power to slot.
- ϵ_s = slot helium trapping efficiency = probability that a helium ion entering slot does not return to the plasma.

The current of D-T ions entering the slot is given by:

$$I_{DT}^S = \frac{Q_s}{8kT_s} \quad [3.3-8]$$

The impurity density in the slot scales roughly as the ratio of impurity current to DT current:

$$N_Z = \frac{I_Z}{I_{DT}^S} N_{DT}^S \quad [3.3-9]$$

The value of I_z is based on the considerations discussed previously. The radiated power in the slot, due to the trapping material, is given by:

$$P_R^s = N_z N_e^s L V_s , \quad [3.3-10]$$

where $N_e^s \approx N_{DT}^s$, L is the cooling rate, and V_s is the slot volume. The helium current to the slot can be assumed to scale with DT current as follows:

$$I_\alpha^s = 0.2 f_\alpha I_{DT}^s , \quad [3.3-11]$$

where f_α is the helium concentration in the plasma, and the factor of 0.2 represents the effect of different edge recycling rates. The removal rate of helium is, by definition, ϵI_α^s , and for equilibrium this must be equal to the α production rate:

$$I_\alpha = I_\alpha^s \epsilon . \quad [3.3-12]$$

Combining the latter two equations gives for the helium concentration:

$$f_\alpha = \frac{I_\alpha}{\epsilon I_{DT}^s} . \quad [3.3-13]$$

The following are typical parameter values. For an inboard location $R = 5.25$ m, limiter height $H = 2$ m, and slot width $w = .1$ m, the slot volume is $V_s = 6.6 \text{ m}^3$. Typical plasma values based on estimates for pumped limiters are $N_{DT}^s = 1.5 \times 10^{19} \text{ m}^{-3}$, $T_s = 10$ eV and $Q_s = 6$ MW. The helium trapping efficiency depends on the sticking probability of impinging helium, and on the recycling of helium within the slot. If the impingement energy is high enough (~ 100 eV) we estimate a sticking probability of $\sim 25\%$. Such an impingement energy is obtained even for low plasma temperatures because of sheath acceleration of the doubly charged helium ion. Backscattered neutral helium may also have an appreciable energy for the first one or two bounces. Helium that does not stick on the first impingement within the slot would tend to recycle several times due to ionization. For a 25% sticking probability, the slot trapping efficiency should scale approximately as $\epsilon_s = 1 - .75^N$ where N is the number of times a helium atom recycles within the slot. For a conservative value of $N = 2$, $\epsilon \approx 0.5$. (For a fraction of 15% of the edge flux entering the

slot, the overall helium removal efficiency would be 7.5%.) Using the above parameter values we obtain for $q_N = 2.5 \text{ MW/m}^2$, vanadium trapping material, and the worst-case trapping rates of Ref.4:

$$I_{DT}^S = 4.7 \times 10^{23} \text{ s}^{-1}$$

$$\frac{I_Z}{I_{DT}^S} = 2.2 \times 10^{-3}$$

$$N_Z = 3.4 \times 10^{16} \text{ m}^{-3}$$

$$P_R^S = .8 \text{ MW}$$

$$f_\alpha = .007$$

Based on this analysis, impurity injection into the slot appears to be fairly non-perturbing, contributing a radiated power fraction of ~ 10% of the incoming power to the slot. Also, a helium sticking probability of 25% appears more than adequate to achieve low helium concentrations in the plasma.

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3.4 Neutronics

3.4.1 Neutronics Analysis

This section presents the neutronics analysis for the self-pumped limiter designs. The effort was focused in the following three areas:

- (1) Neutronics impact of several different limiter concepts such as conventional pumped limiter, first-wall limiter, slot limiter, etc.;
- (2) Impact on the overall blanket performance in terms of tritium production, particularly in the inboard region; and
- (3) Impact on radiation shielding especially from a standpoint of cost and mass utilization.

Due to the geometry of the limiter designs, all neutronics analyses presented here were performed using a multi-dimensional Monte Carlo technique based on MCNP.⁽¹⁾ The associated nuclear data libraries are based on the latest ENDF/B-V⁽²⁾ version.

3.4.2 Tantalum Coating for Conventional Pumped Limiter

Prior to a series of neutronic analyses conducted for the self-pumped limiter concepts, an analysis was carried out for a conventional poloidal pumped limiter design using a tantalum coating. The purpose was to identify the neutronic characteristics of tantalum as a limiter material and make a direct comparison with a beryllium coated limiter that was employed in the BCSS⁽³⁾ study. In addition, the analysis is expected to provide useful information in terms of a comparison with tantalum-base, self-pumped limiter designs to be studied later.

An early version of the TPSS reference parameter set specifies:

Major radius: 6.09 m

First-wall area: 300 m²

Limiter surface area: 60 m²

These parameters have been scaled to the BCSS model that has a 755 m^2 first-wall area and 7.0 m major radius, resulting in a limiter surface area of $\sim 80 \text{ m}^2$. This estimate is based on the major radius scaling (i.e., $60 \times (7.0/6.09)^2$). In fact the area thus derived coincides well with the actual area defined for the BCSS model.

Figure 3.4-1, which shows a vertical cross section of the computational model, has been reproduced after the BCSS study. The blanket model used for the analysis is the self-cooled, liquid-lithium design with V15Cr5Ti alloy as the structural material. The basic limiter configuration is based on the FED/INTOR Phase-2A study.⁽⁴⁾ The heat-sink material is assumed to be V15Cr5Ti. Figure 3.4-2 presents the tritium breeding ratio (TBR) as a function of tantalum thickness coated on the limiter surface facing the plasma. One finds that in the vicinity of a coating thickness of $\sim 10 \text{ mm}$, the impact of coating thickness can be formulated as ~ 0.0125 TBR decrease per 10 mm coating increase for this particular design, or more generally as $\sim 1\%$ TBR decrease per 10 mm coating increase. In comparison with the reference BCSS design, the TBR loss due to the tantalum coating (as opposed to Be in BCSS) amounts to ~ 0.035 or a fractional loss of $\sim 3\%$. Since the reference lithium blanket can yield a TBR of 1.31 without the limiter, the effect of the tantalum-coated limiter should be regarded as a breeding loss of ~ 0.065 in net TBR or a fractional loss of $\sim 5\%$. Although the breeding degradation shown here may not seem excessive for the liquid-lithium blanket design studied, some care should be exercised for other blanket designs in which tritium breeding is marginal.

One of the concerns associated with use of tantalum in the fusion environment is its possible transmutation to tungsten. This process is invoked by the Ta182 production via the Ta181 (n, γ) reaction and the subsequent β^- decay of Ta182 (decay half life of $\sim 115 \text{ d}$) to W182. Figure 3.4-3 illustrates the time-dependent variation of tantalum coating thickness as well as the tungsten production rate due to tantalum burnup as a function of reactor operating time. The computation is based on assumptions:

Neutron wall load: 2.5 MW/m^2

Limiter surface area: 60 m^2

Tantalum injection rate: 0.32 g/s

Initial tantalum coating: 1 mm

Fig. 3.4-1. A vertical cross-section of tokamak design.

Fig. 3.4-2. Effect of limiter coating materials on tritium breeding. (BCSS/STARFIRE model; Li/V15Cr5Ti system; MCNP-ENDF/B-V; 10,000 neutron histories.)

Fig. 3.4-3. Tungsten production due to tantalum burnup. (Neutron wall load = 2.5 MW/m^2 ; limiter surface = 60 m^2 ; Ta injection rate = 0.32 g/s ; initial Ta thickness = 1 mm .)

Fig. 3.4-4. A cross-sectional view of two-dimensional model.

It is found that for reactor operation times of interest, the maximum tungsten production is expected to be, at most, a few percent of tantalum. Note that at times on the order of years (which are substantially greater than the Ta182 decay time), the Ta182 concentration has been already saturated. In consequence, the W182 concentration becomes almost proportional to the amount of tantalum or its thickness. According to Fig. 3.4-3, the W182 production rate can approximately be formulated as 10 μ per 1 mm Ta-coating increase. In an energy range of ~ 4 eV-300 eV, Ta181 possess a bundle of large resonance capture cross sections that amount to 1,000 to 10,000 barns. Consequently, in such an energy range, the Ta181 (n, γ) Ta182 reaction mean-free-path becomes 2-20 μ . The implication is that a sizable fraction of the tungsten production is expected to take place in a very shallow surface region of the tantalum coating. If this is the case indeed, the atomic fraction of tungsten in tantalum will be much higher near the limiter surface, than what is shown in Fig. 3.4-3. In such a case, the surface property of the tantalum limiter may be altered in terms of the helium-retention characteristics. A further detailed study remains to be carried out to fully understand the neutronic impact of the tantalum-base limiter and its transmutation characteristics.

3.4.3 Impact of Self-Pumped Limiter Designs on Tritium Breeding

In this section, tritium breeding of the self-pumped limiter concepts are examined. The computational model used for the analysis is a two-dimensional infinite cylinder as illustrated in Fig. 3.4-4. The vacuum plenum present in a conventional pumped limiter has now been eliminated.

The first self-pumped concept examined was the localized front face limiter. Both tantalum and tungsten trapping material were considered. (As mentioned previously, tantalum is more likely to be acceptable than W but both were considered for comparison purposes.)

The limiter surface covers 20% of the entire first wall area. A set of the reference parameters, 289 m² first-wall area and 5.25 m major radius, was used to derive an equivalent first wall radius of 1.40 m for the present cylinder model. The first-wall/blanket compositions and dimensions are taken from the final BCSS study and defined as follows.

First wall (5 cm):	20% V15Cr5Ti + 80% Li (@ natural Li)
Blanket 1/2 (35 cm):	7.5% V15Cr5Ti + 92.5% Li
Blanket 3 (35 cm)	
Inboard:	10% V15Cr5Ti + 50% Li + 40% Fe1422
Outboard:	10% V15Cr5Ti + 10% Li + 80% Fe1422
Shield (30 cm):	80% Fe1422 + 20% H ₂ O

The coverage of the inboard region is 20% at the first wall of the reference model.

The limiter is modeled by three concentric layers:

- (1) Tantalum or tungsten coating - thickness varies
- (2) Coolant region (0.5 cm): 38.5% V15Cr5Ti + 61.5% Li
- (3) Heat sink region (1.5 cm): 100% V15Cr5Ti

All computations were carried out using MCNP with 5,000 neutron histories in each case. Table 3.4-1 compares the TBR's of several variants of the limiter design. A comparison is also shown with the no-limiter case to indicate breeding loss due to the limiter located at the bottom. The TBRs for the 3 cm and 5 cm Ta-limiters result in ~ 1.26 (-4.2% relative to the 1.31 full TBR) and ~ 1.24 (-5.5%), respectively. In the case of tungsten limiters, the effect of the limiter presence is quite trivial and the respective TBRs result in ~ 1.30 (-0.7%) and ~ 1.29 (-1.6%). As observed in the breakdown of these TBRs shown in Table 3.4-1, the impact on TBR of the self-pumped limiter designs is significantly mitigated by sizable amounts of neutron multiplication and neutron backscattering by tungsten and tantalum themselves. In fact, the neutron multiplication enhancement amounts to 0.075 - 0.103 per D-T fusion. As for comparison between tungsten and tantalum, tungsten is more effective for neutron-energy moderation and has less parasitic neutron loss than tantalum, as seen in the larger T_6 in tungsten.

The design impact of the self-pumped limiter varies substantially depending upon overall blanket designs, relative locations of the limiter, etc. As an example, let us consider the same reference liquid-lithium blanket that was analyzed earlier but assume the inboard blanket region to be consisted of the bulk shielding materials, i.e., 80% Fe1422 + 20% H₂O. Table 3.4-2 examines

TABLE 3.4-1. BREAKDOWN OF TRITIUM BREEDING RATIO FOR SEVERAL VARIANTS OF THE SELF-PUMPED LIMITER DESIGN

	No Limiter	3 cm - Ta	3 cm - W	5 cm - Ta	5 cm - W
Inboard	0.286	0.293	0.296	0.295	0.303
Outboard	1.028	0.964	1.005	0.944	0.988
Limiter	---	0.0019	0.0023	0.0016	0.0020
T_6	0.850	0.850	0.897	0.848	0.903
T_7	0.464	0.408	0.407	0.393	0.390
$T_6 + T_7$	1.314	1.258	1.304	1.241	1.293
Neutron Multiplication	1.181	1.256	1.258	1.283	1.284

TABLE 3.4-2. EFFECT OF SELF-PUMPED LIMITER DESIGNS ON TBR FOR NON-BREEDING INBOARD^a/REFERENCE OUTBOARD BLANKET DESIGN

a. No limiter	TBR = 1.067	
b. 5 cm Ta/bottom limiter	0.989	- 7.2%
c. 5 cm Ta/inboard limiter	1.094	+ 2.5%
d. 5 cm W/inboard limiter	1.132	+ 6.1%
e. 1 cm W/inboard limiter	1.086	+ 1.8%

^a Blanket composition: 80% Fe1422 + 20% H₂O

several different self-pumped limiter designs in terms of the breeding performance. Relative to the full breeding case ($TBR = 1.067$), the 5 cm tantalum-limiter located at the bottom as before yields about 7% less tritium whereas the same limiter but now located on the inboard side can yield about 2.5% more tritium.

It is noteworthy that the breeding performance has been improved rather than deteriorated because of the presence of the limiter. The breeding enhancement can be envisaged even more clearly when tungsten is used instead of tantalum. Table 3.4-2 indicates that the 5 cm tungsten limiter on the inboard side can breed ~ 6% more tritium than the case without the limiter. Evidently all these favorable neutronic performances stem from the large potential of neutron multiplication and backward elastic scattering of these materials. As such, the breeding performance is quite sensitive to the thickness of coating materials. As shown in Table 3.4-2, the 1 cm tungsten limiter design can afford only a 1.8% TBR increase compared to the ~ 6% increase for the 5 cm limiter.

The complete non-breeding inboard concept studied here may or may not be an adequate choice for the liquid-lithium blanket. However, the tungsten-base self-pumped limiter is favored from a neutronic point-of-view for use with any blanket designs in which a portion of the blanket is not fully utilized for breeding, e.g., a very thin inboard breeding blanket design.

The next designs analyzed were the slot limiter concept and first-wall limiter concept. The slot limiter concept utilizes the back side of the limiter (the side facing the first-wall) to retain helium-exhaust. Vanadium, nickel, and molybdenum are considered as the materials to be slotted in between the first wall and the limiter. The computational model used is identical with the one used previously (see Fig. 3.4-4). Namely, the limiter is located at the bottom, and the 1-cm tungsten region facing the plasma side is followed by the 1.5 cm $V_{15}Cr_{5}Ti/Li$ coolant channel. The slot materials mentioned above will be distributed over this coolant channel surface facing the first wall. In the case of vanadium, neutronics computations were made using $V_{15}Cr_{5}Ti$. We expect nearly identical results between this alloy and pure vanadium.

The first-wall limiter concept utilizes the whole first-wall area for the helium retention. The material considered for this concept is vanadium.

Therefore, from the neutronic standpoint, this concept can be regarded as a modification of the first-wall thickness.

Figure 3.4-5 compares the TBRs for all of the cases mentioned above. The adverse effect of slot materials on TBR is minimal with V15Cr5Ti and maximal with nickel. The TBR reduction due to nickel amounts to ~ 0.07 ($= 1.31 - 1.24$) for 6 cm nickel (@ 80% T.D.) or $\sim 1\%$ decrease per 1 cm of effective nickel thickness.

The TBR for the first-wall limiter concept is 1.28, about a 2.5% reduction compared to ~ 1.31 for the case with a bare first wall. One notices that the TBR obtained here is equivalent to that of ~ 2.75 cm nickel, ~ 5.25 cm molybdenum or a lot thicker V15Cr5Ti, all based on the slot limiter concept.

3.4.4 Impact on Radiation Shielding

Reduction in the amount of radiation shielding is probably the single most important impact of the self-pumped limiter concept. The reduction comes from the complete elimination of the huge vacuum pumping plenum and associated radiation shielding around it that would otherwise be required. For example, the STARFIRE design⁽⁵⁾ which uses a conventional pumped limiter incorporates 24 pumps and pumping ducts, two at each of 12 sectors. The DEMO design⁽⁶⁾ substantially reduced the number of vacuum pumping ports and their size compared to the STARFIRE design. However, the radiation shielding for this pumping system is still one of the major design issues associated with the DEMO design. (This reduction is implicitly related to a more optimistic criterion of required vacuum system conductance ratios than STARFIRE. For the same criterion, about three times as many ducts would be required in DEMO.)

Table 3.4-3 shows how much saving on weight and cost one can possibly make by utilizing the self-pumped limiter concept as opposed to the conventional pumped limiter concept. The analysis is based on a simple removal of all vacuum ducts and their shielding from the STARFIRE and DEMO designs. In the case of STARFIRE the saving reaches about 50% on both cost and weight. For DEMO the saving is not as large as STARFIRE because of the limiter reductions already built in. Nevertheless the possible saving amounts to $\sim 18\%$ of the weight and $\sim 16\%$ of the cost.

Fig. 3.4-5. Effect on tritium breeding of slot materials (80% T.D.) in self-pumped limiter designs. (Base limiter: 20% coverage; 1 cm tungsten followed by 1.5 cm V15Cr5Ti/Li.)

TABLE 3.4-3. IMPACT OF SELF-PUMPED LIMITER DESIGN ON SHIELDING MATERIAL COST^a AND WEIGHT

Material	STARFIRE				DEMO			
	Weight ^b (MT) ^d	Saving ^c (MT)	Cost ^b (M\$)	Saving ^c (M\$)	Weight ^b (MT)	Saving ^c (MT)	Cost ^b (M\$)	Saving ^c (M\$)
Fel422	9900	6342	116.2	74.4	1042	127	26.8	3.3
B ₄ C	887	492	36.0	27.0	115	22	10.1	1.9
W	840		48.7					
Ti6Al4V	147	49	11.7	3.7				
TiH ₂	1646	355	3.8	0.8				
Concrete					1629	332	2.8	0.6
Lead					929	221	7.0	1.7
Total	13420	7238	215.8	106.0	3716	702	46.7	7.4

^a Estimate based on 1982 dollars.

^b Original.

^c With self-pumping.

^d Metric tons.

STARFIRE and DEMO are greatly different in various aspects such as reactor size, instantaneous as well as integral neutron wall loads, etc. Also these designs are quite different from the TPSS design currently being studied. An attempt is, therefore, made to scale both STARFIRE and DEMO to TPSS based on its first-wall area of $\sim 300 \text{ m}^2$. The importance of this scaling is two fold. First, it will provide a first-cut estimate of the TPSS shielding requirements with the self-pumped limiter concept. Secondly, it will provide a comparison between the STARFIRE- and DEMO-type shielding approaches in terms of the cost and weight. This information will be useful to defining a TPSS shielding system (which will be designed in the near future), from the stand point of cost and mass utilization.

The STARFIRE shielding design features the use of two distinctive material groups in two different shield regions, i.e., use of titanium-base materials such as Ti6Al4V and TiH_2 in high-flux regions to avoid excessive shield activation and use of iron-base materials (e.g., Fe1422) in low-flux regions to accelerate the radiation attenuation. Somewhat massive use of Fe1422 in the low-flux region in STARFIRE is also due partly to the structural support for the TF-magnets (the anti-torque panels). On the other hand, the basic approach of the DEMO shielding is to utilize less-costly concrete not only for the bulk shield but for the shield around the relativistic electron beam penetration. Since the shielding performance of concrete is worse than that of iron-base materials (e.g., Fe1422), one requires a substantially larger volume of concrete than iron-base materials for a given shielding attenuation criterion.

Table 3.4-4 presents the results of the TPSS scaling. Note that the scaling shown is based merely on the TPSS first-wall area. Although the quite different reactor life times (or integral neutron wall loads) between STARFIRE ($I_w = 108 \text{ MW-y/m}^2$) and DEMO (21.2 MW-yr/m^2) have a large impact on the requirement of shield thickness, it has not been accounted for in Table 3.4-4. It is also noted that the tungsten shield employed in the STARFIRE inboard region has been replaced with a more conventional iron-base shield. The reason is to avoid use of this heavy and expensive material. This approach seems appropriate because the desire to minimize the thickness of the inboard shielding is not as strong as in the earlier designs due to the substantially larger aspect ratio in TPSS.

TABLE 3.4-4. COST AND WEIGHT SCALING^a OF THE STARFIRE AND DEMO SHIELD DESIGNS FOR TPSS SHIELDING ESTIMATE, WITH SELF-PUMPING

	STARFIRE Shield Concept		DEMO Shield Concept	
	Cost ^b (M\$)	Weight (MT) ^c	Cost ^b (M\$)	Weight (MT)
Fe1422	19.9	1695	20.4	793
B ₄ C	4.2	25	7.1	81
Ti6Al4V	3.5	13	--	--
TiH ₂	1.5	649	--	--
Concrete	--	--	1.9	1105
Lead	--	--	4.6	614
Total	29.1	2382	34.0	2593

^a Scaling based on the 300 m² first-wall area of TPSS.

^b Based on 1982 dollars.

^c Metric tons.

Table 3.4-4 reveals that there is no essential difference in cost and weight between the two design approaches when the self-pumped limiter concept is employed. It is also revealed that the TPSS shielding would cost $\sim 17\%$ less and weigh $\sim 30\%$ less than the DEMO shielding. In the case of comparison with STARFIRE, the TPSS shielding would cost $\sim 73\%$ less and weigh $\sim 82\%$ less.

It is difficult to make an accurate estimate of the mass utilization without more detailed analyses, particularly without analyses for the superconducting magnet designs. However, the result presented in Table 3.4-4 indicates the possibility of realizing a shielding design with a high mass utilization. Assuming a total shielding mass of ~ 2500 MT and a net electric power generation of ~ 1393 MWe (corresponding to 3000 MWth fusion and ~ 8 MW/m² neutron wall load) for TPSS, one finds a mass utilization of ~ 550 kWe/MT for the shield. This figure is about a factor of six higher than the STARFIRE shielding mass utilization factor (~ 89 kWe/MT). Even for a substantially small power plant of 540 MWe (which corresponds to 1275 MWth fusion and 3.4 MW/m² neutron wall load), the TPSS shield mass utilization comes to ~ 220 kWe/MT. At a minimum, this figure is a factor of two higher than the corresponding STARFIRE figure.

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3.5 Engineering Designs

3.5.1 Summary

Conceptual designs have been developed for lithium cooled versions of the first wall/limiter and slot limiter self-pumped systems. This section summarizes the results and conclusions of these engineering designs. The analyses performed include thermal hydraulics, MHD, and stress.

Schematics of the mechanical design of the first wall limiter and the slot limiter are shown in Figs. 3.5-1 and 3.5-2, respectively. Both designs have the desirable characteristics of high velocity and reduced lengths of the heated sections in the high surface heat flux region. The combination of these two characteristics results in reduced coolant residence time in the high heat flux region which is essential for liquid-metal cooled first wall/blanket and limiter. Both designs employ electrical insulators with a laminated structure to reduce the pressure drop to acceptable levels.

The major issue addressed for the first wall limiter is lifetime at different neutron wall loadings. Table 3.5-1 shows the calculated lifetime of the first wall limiter as a function of neutron wall loading with a plant availability of 75%.

TABLE 3.5-1. ESTIMATED LIFETIME VERSUS NEUTRON WALL LOADING FOR THE FIRST WALL LIMITER WITH A PLANT AVAILABILITY OF 75%

Neutron Wall Loading (MW/m ²)	Lifetime (yr)
2.5	13
3	10
4	6.5
5	4

The lifetime is 13 years with a wall loading of 2.5 MW/m², and it decreases to 4 years when the wall loading is 5 MW/m². The main reason that the lifetime decreases non-linearly with neutron wall loading is that the surface heat flux, the coating deposition rate, and the magnetic flux density all increase with neutron wall loading.

Fig. 3.5-1. Schematic of the first wall limiter coolant configuration.

Fig. 3.5-2. Schematic and dimensions of the slot limiter.

An important issue for the slot limiter is to determine the maximum allowable heat flux on the front face (plasma side) of the limiter since the heat flux in the slot is relatively insensitive to neutron wall loading. Table 3.5-2 shows the estimated maximum allowable heat flux on the front face of the slot limiter at different locations.

TABLE 3.5-2. ESTIMATED MAXIMUM ALLOWABLE SURFACE HEAT FLUX ON THE FRONT FACE OF THE SLOT LIMITER AT DIFFERENT LOCATIONS

Limiter Location	Maximum Allowable Surface Heat Flux (MW/m ²)
Bottom or outboard region	5
Inboard region	4

The reason that the allowable heat flux is lower in the inboard region is the longer blade length (i.e., longer heated length) and higher magnetic flux density for the slot limiter located there.

In reaching the conclusions shown in Tables 3.5-1 and 3.5-2, many assumptions were made which had significant impact on the outcome of the analyses. The validity of these assumptions are critical to the proposed designs. Table 3.5-3 lists the critical feasibility issues associated with the first wall limiter and the slot limiter.

In both designs, it was assumed that the maximum allowable coating temperature is equal to 70% of the melting temperature of the coating material. Deviations from this assumption could significantly affect the results of analyses.

For the first wall limiter, it was assumed that at the end of life, only 50% of the first wall area is capable of trapping impurity. This also implies that during the lifetime of the limiter, the surface area available for trapping helium varies with time.

Electrical insulators are used for both the first wall limiter and the slot limiter. The insulators are assumed to be of laminated construction. The coolant channels adjacent to the plasma chamber are insulated on three

TABLE 3.5-3. CRITICAL ISSUES FOR THE FIRST WALL LIMITER AND THE SLOT LIMITER

- Maximum allowable coating temperature for trapping impurity
- Minimum surface area required for trapping impurity (first wall limiter)
- Electrical insulator
- MHD and heat transfer
 - pressure drop
 - velocity distribution
- Effect of redeposited material on bending stress (first wall limiter)
- Thermal stress (slot limiter)

sides (the wall facing the plasma is not insulated). The effective wall thickness used in calculating pressure drop is assumed to be 0.2 mm.

Calculations of MHD pressure drop and the effect of velocity distribution on heat transfer always involve some uncertainties. This is generic to all liquid-metal-cooled components in fusion reactors.

In the analysis of the first wall limiter, credit was given to the effect of redeposited material which adds significantly to the stiffness of the first wall against bending. This resulted in increasing the allowable pressure by a factor of two.

In the analysis of the slot limiter, thermal stress was found to be the most limiting factor. To reduce the temperature gradient that causes the thermal stress, credit was given to the effect of the insulator on the bulk structural temperature. More refined analysis is needed in this area.

Finally, it should be noted that the possibility of using liquid metals other than lithium (such as NaK and sodium) should be explored in the future. These liquid metals could operate at lower temperatures ($< 500^{\circ}\text{C}$) which not only relaxes the temperature requirements on structural materials, but also could make it feasible to use bare wall insulators (not laminated structure) that will further reduce the pressure drop of the system.

3.5.2 First Wall Limiter

As discussed previously, the first-wall limiter uses the entire surface area of the first wall to trap helium in the plasma chamber, and thus elimi-

nates the need for a separate impurity control system and the associated cooling system. The first wall will be subjected to higher heat loads in this case compared to the heat loads on the first wall with a limiter present.

The key issue for the first-wall limiter is lifetime and neutron wall loading. As the neutron wall loading increases, the lifetime of the first wall limiter is likely to decrease as will the lifetime of the blanket and other components. The objective here is to estimate the upper limit of the lifetime of the first wall limiter for a given neutron wall loading.

Several scaling relationships are assumed in the analyses as follows:

$$\text{Coating growth rate, } \frac{dx}{dt} \sim P_N$$

$$\text{Surface heat flux, } q \sim P_N$$

$$\text{Magnetic flux density, } B \sim P_N^{1/4}$$

where P_N is the neutron wall loading. The coating growth rate is 1.5 mm/yr for a neutron wall loading of 2.5 MW/m².

The structural material is vanadium alloy and the coolant/breeder material is liquid lithium. The structural temperature limit is assumed to be 750°C and the structure/coolant interface temperature limit is also 750°C. The redeposited material is assumed to be pure vanadium with a density equal to 80% of its theoretical density, with a thermal conductivity equal to 70% of that of the metal at 100% theoretical density.

3.5.2.1 Mechanical Design

The first wall limiter is considered an integral part of the blanket; a self-cooled liquid-metal blanket design concept is adopted here. Figure 3.5-1 shows the schematic of the proposed first wall limiter/blanket. This design divides the blanket into the upper and lower halves, and thus reduces the heated length of the liquid metal duct in the blanket by a half. The higher velocity in the smaller first wall channel is necessary in order to cool the first wall and maintain the structural temperature at acceptable levels. The lower velocity in the back of the blanket has two advantages: (1) it reduces the pressure drop, and (2) it maintains sufficient average coolant temperature

rise through the blanket in order to achieve adequate thermal efficiency. The detailed dimensions of the first wall channels depend on parameters such as stress and coolant velocity which will be discussed in the following sections.

The most critical design region is the inboard blanket where the access is limited and the magnetic flux density is high. The heated length for either the upper or the lower half of the first wall/blanket in the inboard region is approximately 2 meters.

3.5.2.2 Analyses

The design evaluation included thermal-hydraulic, MHD, and stress analyses. Emphasis will be placed on the first wall channels since this is the most critical region. Analyses were performed in an iterative manner until the maximum lifetime is achieved for a given neutron wall loading.

An upper bound on lifetime can be estimated by determining the fluence limit on the vanadium structure. Table 3.5-4 shows the calculated fluence-limited lifetime of the first wall/blanket for various wall loadings. The results shown in Table 3.5-4 are based on a damage rate per wall loading of $10 \text{ dpa}/\frac{\text{MW}}{\text{m}^2}/\text{year}$ and a maximum 250 dpa allowable for vanadium alloy.

TABLE 3.5-4. FLUENCE LIMIT ON VANADIUM STRUCTURE

Wall Load (MW/m ²)	Lifetime, 75% Availability (Years)
2.5	13
3.0	11
4.0	8
5.0	6.5
6.0	5.5
7.0	4.6

* Based on a damage rate per wall loading of $\frac{10 \text{ dpa}}{\frac{\text{MW}}{\text{m}^2} - \text{year}}$ and a maximum of 250 dpa for vanadium.

Thermal-Hydraulic Analysis

The method adopted here for heat transfer analysis is similar to that of the BCSS.⁽¹⁾ The assumptions for liquid-metal heat transfer include: (1) uniform velocity distribution (slug flow); (2) turbulence suppressed; and (3) natural convection suppressed. Both surface heat flux and nuclear heating were included in the calculations.

Two major assumptions were made in determining the temperature limit of the first wall limiter. The first assumption is that the coating material (vanadium) is capable of trapping helium as long as the surface temperature is below 70% of its melting point. The second assumption is that only a fraction of the first wall area is required to trap helium, and certain portions of the first wall area are allowed to have surface coating temperatures greater than 70% of the melting point. Specifically, the following limiting condition was adopted:

$$T_{INT}^{AVE} \leq 0.7 T_{MP} - \Delta T_{FW} , \quad [3.5-1]$$

where T_{INT}^{AVE} is the average coolant/structure interface temperature, T_{MP} is the melting point of the coating (redeposited) material, and ΔT_{FW} is the temperature rise across the first wall limiter including the redeposited material. It should be noted that the average interface temperature, instead of the maximum interface temperature, is employed in Eq. [3.5-1]. This means that at the end of life, only 50% of the first wall area is still capable of trapping helium since the other 50% of the first-wall-limiter area has a surface temperature exceeding 70% of the melting point of the coating material.

MHD Analysis

The primary objective of the MHD analysis is to calculate the pressure drop of the first wall/blanket system. The largest pressure drop is apparently going to occur in the first wall channels since the velocity and the magnetic flux density are highest there. Using the scaling relationship described previously, the following equation is obtained for liquid lithium flow with a heated length of 2 m.

$$\Delta P_{FW} = 0.0558 \frac{P_N^{1.5} t_w}{ab \Delta T}, \quad [3.5-2]$$

where ΔP_{FW} is the pressure drop through the first wall channels, t_w is the half thickness of the wall between the adjacent coolant channel, a is the half width of the first wall channel in the radial direction, b is the width of the first wall channel in the toroidal direction, and ΔT is the average temperature rise through the blanket. The temperature rise, ΔT , should be in the range of 150 - 250°C in order to have attractive thermal efficiency, relatively low pumping power, and acceptable piping cost. The wall thickness (t_w), and the spans a and b are constrained by stress requirements and cannot be varied freely. A typical value of t_w is about 1 mm and that of a and b are a few centimeters. With these values, the pressure drop calculated by Eq. [3.5-2] is relatively low for low neutron wall loading (2.5 MW/m²). The pressure drop increases with the 1.5th power of neutron wall loading. Thus, the pressure drop through the first wall channel is likely to be high (still acceptable but may not be attractive) at higher neutron wall loading. In addition to the pressure drop through the first wall channels, there are other pressure drops through the blanket, such as the return flow at the back of the blanket, the bends, the inlet and outlet pipes.

To alleviate the pressure drop problem, electrical insulators are used throughout the first-wall-limiter/blanket with the exception of the wall facing the plasma. The insulator is assumed to be of laminated (sandwiched) structure. The following two assumptions are adopted in calculating the pressure drop through the entire first wall/blanket system.

1. Electrical insulator with laminated structure is applied on three sides of the first wall channel (the side facing the plasma is not insulated). The effective wall thickness in calculating the pressure drop is the thickness of the surface layer (assumed to have a thickness of 0.2 mm).
2. The total pressure drop is estimated to be twice the pressure drop through the first wall channel.

$$\Delta P_{total} \approx 2 \Delta P_{FW}$$

The second assumption is rather arbitrary and is adopted here primarily for feasibility considerations. It should be noted that although it may not be necessary to use insulators for low neutron wall loading, it is nevertheless, assumed that insulators are used for all applications here.

Stress Analysis

Most limiter and blanket designs use manifold schemes that require hot and cold channels to be situated next to one another. For example, in the BCSS design study the temperature differences between adjacent channels were 255°C for the helium cooled design and about 80°C for the liquid metal cooled design. This temperature drop occurs over a very short distance which can cause large stresses in the back plate of the manifold. Although stress analysis for this particular geometry is not available, a closely related problem, for which stress analysis is available, is the thermal mismatch stress in a solid circular cylindrical segment sandwiched axially between two solid circular cylinders.⁽²⁾ The analysis shows that a high normal stress (σ_{zz}) acting perpendicular to the interface (tensile in the segment at lower temperature and compressive in the one at higher temperature) is present at a section close to the interface. A similar high shear stress (τ_{rz}) is present at the interface. Both the normal and the shear stresses reach their peak values at the specimen surface, and are given by:

$$\sigma_{zz \text{ max}} = \frac{E\alpha \Delta T}{2(1-\nu)}, \quad [3.5-3]$$

and

$$\tau_{rz \text{ max}} = \frac{0.32 E\alpha \Delta T}{1-\nu}, \quad [3.5-4]$$

where E and ν are elastic constants, α is the coefficient of thermal expansion and ΔT is the temperature drop.

The maximum stress intensities (defined as twice the maximum shear stress) produced in channels made of PCA, HT-9, and V-15Cr-5Ti due to a temperature drop ΔT are shown in Fig. 3.5-3 which also includes the maximum allowable primary plus secondary stress limits ($3 S_m$) for the three materials. Ignoring primary stresses due to pressure loading, the maximum tempera-

Fig. 3.5-3. Variation of maximum thermal stress intensity in the back plate of two adjacent channels containing coolants at temperatures T and $T + \Delta T$, for three structural materials. The allowable maximum thermal stress intensity for the three materials are also shown.

As an example, consider the bending of a V-15Cr-5Ti flat first wall. Since the S_{mt} value (up to a life of 50,000 h) for this material, as determined from thermal creep and strength properties in the temperature range 400-650°C, is approximately equal to 235 MPa (determined by ultimate strength), the maximum pressure for a span (ℓ) and initial thickness (h_0) is limited by

$$P_{max} < 705 (h_0/\ell)^2 \text{ MPa} . \quad [3.5-5]$$

For conducting the check on the radiation creep limit, the composite consisting of the redeposited material on top of the structural material is replaced by a single layer of porous redeposited material. Further, it is assumed that the radiation induced creep rate of the redeposited material at a given stress and temperature is not substantially different from that of the structural material. Then, since the redeposited material is less stiff than the structural material, the computed maximum radiation induced creep strain on this homogeneous material will be an upper bound to the maximum strain in the actual composite. Denoting the maximum allowable pressure (based on radiation creep limit) on a beam of constant thickness h_0 by p_0 , the allowable maximum pressure on a beam, whose thickness during its lifetime increases due to redeposited material from h_0 to $h_0 + \Delta h$, is approximately given by:

$$P_{max} \approx \frac{2 (h_0 + \Delta h)}{2 h_0 + \Delta h} P_0 . \quad [3.5-6]$$

$$\text{When } \Delta h \gg h_0, P_{max} = 2 P_0 . \quad [3.5-7]$$

The allowable S_{mt} (based on radiation creep properties) of V-15Cr-5Ti for a fluence of 250 dpa and up to a temperature of 700°C is 90 MPa. Then the allowable pressure for the case where $\Delta h \gg h_0$ is

$$P_{max} = 2 P_0 < 360 (h_0/\ell)^2 . \quad [3.5-8]$$

Note that the allowable pressure limit is increased from what would be allowed under past practices by a factor of two, and safety is maintained because Eq. [3.5-8] is more restrictive than Eq. [3.5-5]. Plots of the allowable pressure on a beam made of V-15Cr-5Ti as limited by radiation induced creep and thermal

ture drops permissible for PCA and HT-9 are 180 and 265°C respectively. The allowable temperature drop for V-15Cr-5Ti is much larger (~ 500°C). Realistically, the allowable temperature gradients are smaller because of the presence of primary stresses. For example, if the primary stresses are assumed to equal their respective maximum permissible values, the allowable temperature drops for PCA, HT-9, and V-15Cr-5Ti are respectively 120, 175, and 325°C. Thus, for the first wall limiter with vanadium structure, this type of thermal stress is not likely to present a problem.

In blanket designs using the first wall as the limiter, a significant amount of redeposited material is formed so that by the end of life the thickness of the redeposited material can be much greater than the original thickness of the first wall at the beginning of life. This redeposited material, though not fully dense, adds significantly to the stiffness of the first wall against bending, particularly for deformations induced by radiation induced creep. By taking advantage of the stiffness of this redeposited material in a conservative fashion, the allowable pressure limit (based on radiation induced creep limit) for primary stress containment can be increased significantly, thus allowing the first wall to be designed to a much higher wall loading. This does not compromise safety, because the 5% radiation induced creep strain limit is not a safety criterion. Safety can be effectively maintained by requiring the primary stresses, based on original first wall thickness, to be less than S_{mt} , as determined from short-term yield and ultimate strengths and time dependent thermal creep properties. Therefore, the design criterion for primary stress in cases where significant amount of redeposition of material occurs has been made less stringent than what has been used in the past, in the following way:

- (1) The maximum primary membrane stress (calculated on the basis of initial first wall thickness) is limited to S_{mt} (equal to the lesser of S_m and S_t as determined from thermal creep properties), and the maximum primary bending stress to $1.5 S_m$ or $1.25 S_t$, whichever is less.
- (2) The maximum combined membrane and bending strain at the end of life due to radiation induced creep is limited to 5%, but credit for the stiffness of the redeposited material, if any, is taken to calculate the strains in a conservative fashion.

creep properties are given in Figs. 3.5-4 and 3.5-5, respectively.

3.5.2.3 Lifetime Versus Wall Loading

To determine the maximum lifetime of the first-wall-limiter/blanket system, iterative calculations were performed. For a given neutron wall loading, the lifetime based on the fluence limit described in Section 3.5.2.2 is assumed, and calculations were performed to determine if temperature and stress requirements are satisfied. If one of the requirements is not met, the lifetime is lowered until all the requirements are satisfied. In addition to the temperature and stress requirements, certain attractive features (not feasibility issues) were included. For example, the average coolant temperature rise through the blanket is maintained approximately between 200 - 250°C, and the total pressure drop through the system is kept below 2 MPa.

Figure 3.5-6 shows the calculated maximum lifetime versus neutron wall loading. The solid line represents the design limit (stress, temperature, etc.). The dashed line represents the fluence limit described previously. For a plant with a 75% availability, the lifetime of the first wall limiter can be greater than 10 years for a neutron wall loading of 2.5 MW/m². The lifetime is approximately 4 years if the neutron wall loading is increased to 5 MW/m².

Table 3.5-5 shows the key parameters of the first wall limiter for two neutron wall loadings. In Table 3.5-5, the maximum coating temperature at end of life exceeds 70% of the melting point of the vanadium (1240°C). As described in Section 3.2.2, it was assumed that, at end of life, 50% of the surface area is still below this temperature limit and capable of trapping helium.

It should be noted that these designs have not been optimized since uncertainties associated with the major assumptions are rather large. The validity of these major assumptions constitute the critical feasibility issues of the first-wall-limiter-concept.

3.5.3 Slot Limiter

The slot limiter has the unique characteristics of trapping helium in the slot region where the surface heat flux is rather low (0.1 MW/m²) and approxi-

Fig. 3.5-4. Allowable pressure (p_0) on a clamped V-15Cr-5Ti beam of initial thickness h_0 , as limited by a maximum 5% radiation induced creep strain limit at various values of end of life fluence. The temperature and fluence shown are thickness-averaged values. If the thickness of the plate is increased over its lifetime by Δh due to redeposited material, the allowable pressure may be increased by the factor $\frac{2(h_0 + \Delta h)}{2h_0 + \Delta h}$.

Fig. 3.5-5. Allowable pressure on a clamped beam as functions of maximum allowable stress. The allowable pressures may be increased by a factor 1.25 to take credit for bending stress. These pressures should not be increased to take advantage of redeposited material, if any.

Fig. 3.5-6. Lifetime versus neutron wall loading for the first wall limiter with a 75% plant availability.

TABLE 3.5-5. KEY PARAMETERS OF THE FIRST-WALL-LIMITER/BLANKET FOR NEUTRON WALL LOADING OF 2.5 and 5.0 MW/m²

Wall loading (MW/m ²)	2.5	5.0
Surface heat flux (MW/m ²)	0.76	1.52
Deposition rate (mm/yr)	1.5	3.0
Coating thickness at end of life (mm)	15	9
Lifetime at 75% availability (yr)	13	4
Inlet temperature (°C)	330	300
Outlet temperature (°C)	550	550
Coolant temperature rise (°C)	220	250
Gross thermal efficiency (%)	42	42
First wall coolant velocity (m/s)	0.25	0.45
Total pressure drop (MPa)	0.25	1.5
Maximum coolant/structure interface temperature - first wall/blanket (°C)	640/550	676/550
Maximum structural temperature (°C)	705	775
Maximum coating temperature at end of life (°C)	1250	1443
First wall channel		
Toroidal span (m)	0.04	0.02
Radial span (m)	0.07	0.07

mately independent of neutron wall loading. The redeposited layer in the slot region can be relatively thick without exceeding temperature limits of the structural and coating materials. The front face (the plasma side) of the slot limiter is subjected to higher heat fluxes ($1 - 5 \text{ MW/m}^2$). However, since there is very little erosion on the front surface, the structure can be kept thin to accommodate the high heat fluxes. These two advantages could result in long lifetimes (~ 10 years) even at high neutron wall loadings.

Table 3.5-6 is a list of the materials and relevant parameters for the slot limiter. As shown in Table 3.5-6, a design lifetime of 10 years is used which resulted in a 3.0 cm thick coating on both sides of the slot region at the end of life (EOL) of the limiter. The surface heat flux on the plasma side is proportional to the fusion power and is somewhat controllable. The location of the slot limiter could be at the bottom, the inboard region, or the outboard region. From the engineering point of view, it is desirable to locate the slot limiter in the bottom or the outboard region of the reactor since (1) the magnetic flux density is lower, and (2) access is limited in the inboard region. However, it may be advantageous to locate the slot limiter in the inboard region from the neutronics and plasma engineering points of view. If the limiter is located in the inboard region, the blade length is approximately 2 m. If the limiter is located in the bottom or the outboard region, the blade length is somewhat shorter (~ 1.5 m).

3.5.3.1 Mechanical Design

Figure 3.5-2 shows the schematic and dimensions of the proposed design for the slot limiter. At the inlet, coolant flows radially towards the plasma and it then splits into two paths in the high heat flux region in order to reduce the heated length. Coolant then returns through the back of the limiter and removes heat from the deposited material. The velocity in the coolant channel near the front face is the highest since the channel size is the smallest. The channel sizes in both the return flow and the inlet are larger than that of the front face in order to reduce the velocity (and the pressure drop). The return flow in the back of the limiter can be kept at relatively low velocity since the heat flux is very low there.

A key issue is to determine the maximum heat flux that the front face of the slot limit can tolerate. The entire limiter is assumed to be made of one

TABLE 3.5-6. MATERIALS AND RELEVANT PARAMETERS FOR THE SLOT LIMITER

Structural material	Tantalum
Coating material	Iron, nickel, vanadium
Coating thickness at EOL (10 years), cm	3
Surface heat flux, MW/m ²	
Plasma side	1-5
Slot region	0.1
Thickness in radial direction, cm	15
Blade length, m	1.5 - 2.0
Coolant	Liquid lithium
Location	Bottom, inboard, or outboard region

structural material (tantalum) with the exception of the deposited materials. A thickness of 2 mm is assumed for the tantalum at the front face as well as the leading edge of the limiter as shown in Fig. 3.5-2. A bottom limiter is assumed to be the reference design with a magnetic flux density of 3.8 Tesla and a blade length of 1.5 m.

3.5.3.2 Analyses

Thermal-Hydraulic Analysis

Thermal-hydraulic analysis includes calculations of the liquid-metal temperature, the structural temperature in the front face and the back of the limiter, the temperature of the deposited material, and the bulk structural temperature. The last parameter is needed in order to estimate the thermal stress in the limiter. A uniform velocity profile is assumed, as in the case of the first wall limiter, in calculating the temperature distributions of the liquid metal in the coolant channels. The following temperature limits were assumed.

Structural (tantalum) temperature $\leq 1400^{\circ}\text{C}$

Structure (tantalum)/coolant (lithium) interface temperature $\leq 1200^{\circ}\text{C}$
and

Coating temperature $\leq 70\%$ of melting point

Table 3.5-7 shows the melting temperatures and 70% of the melting temperatures of the three coating materials. It turned out that the most limiting factor is not the temperature listed in Table 3.5-7, but the thermal stress associated with the temperature gradient in the limiter (see stress analysis). The temperature gradient required for the thermal stress calculation is the temperature difference between the bulk structure and the maximum structural temperature of the limiter. The location of the maximum structural temperature for this particular design is near the leading edge of the limiter as a result of the high surface heat flux along the front face and the leading edge. If no insulator is used, the bulk structural temperature should be close to the average coolant temperature rise through the limiter. If an electrical insulator is used (which is assumed in the MHD analysis), the bulk structural temperature will be higher than the average coolant temperature

TABLE 3.5-7. MELTING AND 70% OF MELTING TEMPERATURES OF IRON, NICKEL, AND VANADIUM

Material	Melting Temperature, T_m (°C)	70% of T_m (°C)
Iron	1535	992
Nickel	1453	935
Vanadium	1890	1241

rise. The temperature increase of the bulk structural material due to the presence of an insulator will help to reduce the temperature gradient and hence reduce the thermal stress in the limiter.

To calculate the average bulk structural temperature, a simple model was developed. Figure 3.5-7 shows the geometry and the coordinate system employed in the model. The bulk structure of the limiter is cooled on either side by liquid lithium and it is assumed that there is no heat flux across the mid-plane. Thus, in Fig. 3.5-7, b is the half thickness of the structure, and δ is the thickness of the insulator layer. If the insulator is of the sandwiched construction, there will be another thin layer of structural material outside the insulator. However, the temperature rise across this layer will be much smaller than that of the insulator and thus can be neglected.

The steady-state heat conduction equation with volumetric heating (Q) applies to both the structure material and the insulator

$$\frac{\partial^2 T}{\partial x^2} = \frac{Q}{k}, \quad [3.5-9]$$

where k is the thermal conductivity of the material. The boundary conditions for the structure material are

$$\begin{aligned} x = 0, \quad k \left(\frac{\partial T}{\partial x} \right) &= 0 \\ x = b, \quad T &= T_b. \end{aligned} \quad [3.5-10]$$

The solution to Eqs. [3.5-9] and [3.5-10] is

Fig. 3.5-7. Geometry and coordinate system used in modeling and calculating the bulk structural material temperature in the presence of an insulator.

$$T = T_b + \frac{Q_s}{2 k_s} (b^2 - x^2), \quad [3.5-11]$$

where the subscript s refers to the structural material. The boundary conditions for the insulator are

$$\begin{aligned} k_s \left(\frac{\partial T}{\partial x} \right) &= k_i \left(\frac{\partial T}{\partial x} \right) \\ T &= T_b \end{aligned} \quad \text{at } x = b \quad [3.5-12]$$

and the solution to Eq. [3.5-9] and [3.5-12] is

$$T = T_b + \frac{b Q_i}{k_i} (b - x) \left[\frac{(1 + x/b)}{2} \right] - \left(1 - \frac{Q_s}{Q_i} \right), \quad [3.5-13]$$

where the subscript i refers to the insulator. Utilizing Eq. [3.5-13] and the convective boundary condition

$$-k_i \left(\frac{\partial T}{\partial x} \right) = h (T - \bar{T}_c), \quad \text{at } x = b + \delta \quad [3.5-14]$$

resulted in the following equation for T_b ,

$$T_b = \bar{T}_c + \frac{b Q_s}{h} (1 + B_i) \quad [3.5-15]$$

where $\bar{T}_c$ is some mean value of the coolant temperature, h is the heat transfer coefficient, and $B_i = h \delta / k_i$ is the Biot number. In obtaining Eq. [3.5-15], we have assumed that the nuclear heating rate (per unit volume) in the structural material is of the same order of magnitude of that in the insulator and that $\delta/b \ll 1$.

For a given coolant average temperature, Eqs. [3.5-11] and [3.5-15] can be employed to calculate the temperature distributions in the insulator and the structural material. The average bulk structural temperature ($\bar{T}_s$) can be obtained by integrating Eq. [3.5-11]

$$\begin{aligned}\bar{T}_s &= \frac{1}{b} \int_0^b T dx = T_b + \frac{Q_s b^2}{3 k_s} \\ &= \bar{T}_c + \frac{Q_s b^2}{3 k_s} + \frac{b Q_s}{h} (1 + B_i) .\end{aligned}\quad [3.5-16]$$

As an example, if the following values are adopted for lithium and tantalum,

$$\begin{aligned}h &= 20,000 \frac{W}{m^2 \cdot K} & (\text{Nusselt No.} \approx 8) \\ Q_s &= 95 \frac{MW}{m^2} & (\text{Neutron wall loading} = 5 \frac{MW}{m^2}) \\ b &= 0.004 \text{ m}\end{aligned}$$

and a thermal conductivity of $1 \frac{W}{m \cdot K}$ is assumed for the insulator (for example, $Y_2 O_3$), the following results are obtained:

$$\begin{aligned}T_b - \bar{T}_c &= 57^\circ C & \text{if } \delta &= 0.1 \text{ mm} \\ &= 95^\circ C & \text{if } \delta &= 0.2 \text{ mm} \\ &= 133^\circ C & \text{if } \delta &= 0.3 \text{ mm}\end{aligned}$$

Thus, an increase in temperature of $95^\circ C$ can be achieved if the thickness of the insulator is ~ 0.2 mm.

MHD Analysis

It was found necessary to use an electrical insulator in the limiter in order to reduce the pressure drop to acceptable levels (< 3 MPa). The assumptions and analysis were similar to that employed for the first wall limiter described in Section 3.5.2.2. Electrical insulators were assumed everywhere in the coolant channels with the exception of the duct wall facing the plasma. The sandwich structure of the insulator has a surface layer thickness of 0.2 mm. The single largest pressure drop is the result of relatively high velocity in the coolant channels adjacent to the front face and is calculated by the following equation

$$\Delta P_{FW} = \ell V B^2 \sigma_w t / a , \quad [3.5-17]$$

where ΔP_{FW} is the pressure drop through the coolant channels adjacent to the front surface, l is the half blade length (0.75 m) of the limiter, V is the coolant average velocity in these channels, B is the magnetic flux density (~ 3.8 Tesla for a bottom limiter), σ_w is the electrical conductivity of tantalum, t_w is the surface layer thickness of the insulator (0.2 mm), and a is the half channel width in the toroidal direction (7.5 mm). The total pressure drop is assumed to be twice the pressure drop through the channels adjacent to the front face of the limiter.

Stress Analysis

The geometry of the slot limiter is shown in Fig. 3.5-2. A proposed structural material is tantalum. A tantalum alloy T-111 (Ta-8%W-2%Hf) was used for this calculation since properties of this material were available.⁽³⁾ The allowable stress limits as determined from yield, ultimate, and thermal creep strengths are plotted in Fig. 3.5-8. For comparison, the allowable stresses for V-15Cr-5Ti are also shown in the figure. Note that in the temperature regime 200-750°C V-15Cr-5Ti is significantly stronger than T-111.

The front plate that forms one boundary of the coolant channel is subjected to bending because of coolant pressure. Assuming a span of 1.5 cm and a thickness of 2 mm, the maximum allowable pressures for a 6 year and a 10 year lifetime limiter with 75% availability are shown in Fig. 3.5-9. At an average front wall temperature of 900°C, the allowable pressure is about 3.8 MPa for a ten year lifetime. Thus, bending stress is not a problem for the present design of the slot limiter. No check on radiation induced creep was performed because of lack of data.

An approximate thermal stress analysis was performed by assuming the average metal temperature of the limiter to equal the average of the coolant inlet (300°C) and outlet temperatures. The maximum thermal stress at the leading edge is then proportional to the difference between the maximum metal temperature and the average metal temperature. Assuming a thermal stress allowable of 450 MPa, the maximum permissible metal temperature as a function of the coolant temperature rise through the limiter is shown in Fig. 3.5-10. It is obvious that for realistic values of coolant temperature rise, this

Fig. 3.5-8. Allowable stress intensities of tantalum alloy T-111 as a function of temperature. Also shown are the stress allowances for V-15Cr-5Ti. The solid lines marked S_m are based on yield and ultimate strengths, and the dashed lines marked S_{mt} also take into account thermal creep properties.

Fig. 3.5-9. Allowable pressure on a T-111 clamped beam of thickness 2 mm and span 1.5 cm as a function of average wall temperature and lifetime (75% availability).

Fig. 3.5-10. Maximum permissible metal temperature in a T-III slot limiter, as limited by thermal stress, as a function of average temperature rise (ΔT) of the coolant for a coolant inlet temperature of 300°C .

alloy cannot operate anywhere above a maximum metal temperature of 700°C without using an insulator. Although tantalum retains its strength up to a higher temperature than vanadium, its strength is too low for effective use as a structural material above 700°C. Thus, the most limiting factor for the present design of slot limiter is the thermal stress.

3.5.3.3 Results and Discussion

Table 3.5-8 summarizes the key design parameters of the slot limiter for a surface heat flux of 5 MW/m^2 . The maximum allowable temperature of the structural material (determined from thermal stress requirement) is approximately 730°C for this case. This is slightly below the maximum temperature of 745°C shown in Table 3.5-8. The 745°C maximum structural temperature can be reduced slightly by (1) increasing coolant velocity, (2) decreasing front wall thickness, and (3) increasing the thickness of the bulk structure. The 730°C allowable temperature can be increased by increasing the thickness of the insulating layer.

If the surface heat flux is less than 5 MW/m^2 , the maximum structural temperature and the pressure drop can both be reduced. If the limiter is located in the outboard region, the magnetic flux density is 2.9 Tesla and the pressure drop can be reduced. If the limiter is located in the inboard region, the magnetic flux density is ~ 4.4 Tesla and the blade length is 2 m. Both factors will make it more difficult to meet all the design requirements. Thus, if the limiter is located in the inboard region, the surface heat flux probably cannot exceed 4 MW/m^2 .

If vanadium alloy, instead of tantalum, is used as the structural material, thermal stress will no longer be the most limiting factor. The limiting factor is going to be the allowable structural temperature which is 750°C for vanadium alloy. This limiting temperature is approximately the same as that for tantalum due to thermal stress (730°C). However, the thermal conductivity of tantalum is approximately twice that of vanadium, therefore, use of vanadium as the structural material will not improve the design flexibility.

Finally, it should be noted that the possibility of using liquid metals other than lithium (such as NaK or sodium) as coolant for the limiter has not been explored here. There is some evidence indicating that the bare wall insulator (without the sandwiched construction) might be feasible provided

TABLE 3.5-8. KEY DESIGN PARAMETERS OF THE SLOT LIMITER WITH A SURFACE HEAT FLUX OF 5 MW/m²

Structural material/coolant	Tantalum/liquid lithium
Neutron wall loading (MW/m ²)	5.0
Total blade length (m)	1.5
Magnetic flux density (T) at bottom limiter	3.8
Coolant inlet temperature (°C)	230
Maximum coolant/structure interface temperature (°C)	590
Maximum structural temperature (°C) (front face)	745
Maximum coating temperature in the slot (°C)	
Iron	677
Nickel	643
Vanadium	702
Coolant velocity in the channel next to the front face (m/s)	2.0
Total pressure drop (MPa)	2.3
Insulator surface layer thickness (mm)	0.2
Insulator thickness (mm)	0.2

that the maximum coolant temperature is below 500°C. Bare wall insulators could further reduce the pressure drop since there will be little or no current through the duct walls. NaK and sodium could operate at lower temperatures than lithium, and thus could be used with bare wall insulators. The combination of lower operating temperature and bare wall insulators could relax both the temperature and the pressure drop restrictions on the current design.

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3.6 Material-Related Analysis

3.6.1 Sputtering Calculations

The sputtering performance of the self-pumped limiter material was calculated for different materials and energy ranges. The key requirement for the limiter material is the ability to trap helium more efficiently than hydrogen. Two materials are examined in this analysis, i.e. vanadium and tantalum. An important factor in these materials is that the self-sputtering yield should not exceed unity at the operating plasma edge temperature. The computer code used in this analysis is the ITMC code (Ion Transport in Materials and Compounds).^(1,2) This code can study in detail the transport of charged particles in solid materials and compounds and their related phenomena such as sputtering and the damage produced. Alloys with different surface and bulk compositions or with layered structures of different materials can be used.

Figure 3.6-1 shows the self-sputtering yield of vanadium at different ion energies and for different V-He composition as a result of He-trapping. The self-sputtering yield of pure vanadium exceeds unity for ion energies higher than 1 keV. However, for helium-trapped in vanadium surfaces the self-sputtering yield exceeds unity for higher ion energies. As an example for a 50% V-50% He limiter composition, the energy at which sputtering yield becomes larger than one is about 3 keV which is three times higher than that of pure vanadium. This has the advantage of permitting operation of the plasma at higher edge temperatures. The surface and bulk binding energies of vanadium atoms in the V-He composition system are assumed to be the same as pure vanadium. Also the helium atoms are assumed to occupy the vacancy positions during the deposition process. The self-sputtering yield of tantalum for different Ta-He composition systems is shown in Fig. 3.6-2. As shown, the threshold energy for self-sputtering yield to exceed one does not change as much for larger He fractions in tantalum. This is mainly because it is easier for He atoms to transfer or receive a substantial amount of energy (to start a cascade) to or from a vanadium atom rather than a tantalum atom.

Fig. 3.6-1. Vanadium self-sputtering yield as a function of incident energy.

Fig. 3.6-2. Tantalum self-sputtering yield for different incident energies.

3.6.2 Tritium Permeation and Inventory and Protium Permeation

Two key issues for self-pumping are the tritium inventory in the trapping material and the permeation of tritium and protium into the coolant. The retained tritium inventory should obviously be as low as possible. The tritium permeation rate is much less critical but should also be minimized. To first order, the permeation rates of tritium and protium are governed by a similar permeation removal parameter, ϵ , defined as the removal rate via permeation to the coolant divided by the particle impingement rate. The permeation should be low enough for acceptably low tritium throughout, but must be high enough to remove protium. Protium is formed by the D-D reaction at a rate of about 1/400 of the α -production rate (at the plasma temperatures considered in TPSS). This is equivalent to 0.11 gms of protium per day, for a 2.5 MW/m^2 wall loading.

In steady state, the protium concentration in the plasma can be related to the permeation as follows:

$$\frac{N_p}{N_{DT}} I_{DT} \epsilon = \dot{M}_p, \quad [3.6-1]$$

where I_{DT} is the current of DT ions to the boundary and $\dot{M}_p$ is the protium production rate. This expression assumes equal transport properties of hydrogen isotopes in the plasma.

To first order, I_{DT} scale linearly with neutron wall loading. For $q_N = 2.5 \text{ MW/m}^2$, and for the typical plasma edge conditions discussed in Section 3.3.4, $I_{DT} = 2.5 \times 10^{24} \text{ s}^{-1}$ and $\dot{M}_p \approx 7.9 \times 10^{17} \text{ s}^{-1}$. This gives for the protium concentration:

$$\frac{N_p}{N_{DT}} = \frac{3.2 \times 10^{-7}}{\epsilon}. \quad [3.6-2]$$

An acceptable protium concentration is about 1%. Only a small permeation efficiency is required to achieve this concentration.

To compute the permeation and inventory of hydrogen isotopes in candidate self-pumped materials, we have developed a steady state computer code. The permeation model assumes a hydrogen isotope atom implantation flux J_i at a depth δ in the wall which is much less than the wall thickness d .

It is assumed that gas molecules leave either wall surface (front or back) by recombination limited desorption according to

$$J = 2 K_r C^2, \quad [3.6-3]$$

where C is the dissolved hydrogen isotope concentration near the surface, and K_r is the recombination coefficient given by⁽³⁾:

$$K_r = \frac{4 \alpha}{K_{so}^2 \sqrt{\pi K M T}} \exp \left(-\frac{2 E_s - E_x}{K T} \right), \quad [3.6-4]$$

where

α = sticking coefficient (≈ 1 for clean surfaces)

K_{so} = pre-exponential Sievert's solubility constant

K = Boltzmann constant

M = mass of the molecule formed by recombination

T = temperature

E_s = energy of solution for hydrogen in metal

$E_x = E_s + E_d$ (E_d = diffusion energy)

$$\text{and } E_x = \begin{cases} 0 & \text{if } E_s + E_d < 0 \\ E_s + E_d & \text{otherwise.} \end{cases}$$

The protium concentration in the material is much less than the tritium or deuterium concentration. The effect of the protium isotope is taken into consideration by assuming that the protium flux leaving either surface is given by:

$$J = K_r C_H C_{DT}, \quad [3.6-5]$$

where

C_H is protium concentration.

C_{DT} is tritium-deuterium concentration.

The code was run for the first wall/limiter system using vanadium. This system is designed for 10 years of operation at 2.5 MW/m^2 neutron wall loading. Initially the wall thickness is about 2 mm with a front surface temper-

ature of 967 K and a back surface temperature of 913 K. Fresh vanadium is deposited on the wall at a rate of 1.5 mm/year. The thermal conductivity of the deposited vanadium is assumed to be 70% of full-density vanadium.

Table 3.6-1 shows the tritium inventory, tritium permeation rate, and plasma protium concentration for the vanadium wall as a function of the number of years of operation. As the wall thickness increases the front wall temperature also increases. This has the effect of increasing the hydrogen flux leaving the front surface and reentering the plasma. Thus, the permeation rate is largest in the early years of operation and decreases continuously thereafter.

As shown in Table 3.6-1, the tritium inventory is acceptable, being extremely small (several grams) for the full 10 years. The protium concentration in the plasma is acceptably small being less than 0.5% over most of the lifetime. The tritium permeation rate is also small. At the start of operation it is equal to about five times the tritium burnup rate. This is an order of magnitude smaller than what would be removed by a pumped limiter or divertor if no oxide layer or coated barrier is used. If necessary, the initial permeation rate could be made smaller by simply starting with an additional wall thickness. For example, an initial added thickness of 1.5 mm would cut the permeation rate approximately in half. This might impact the system lifetime but there already is a 30% margin, i.e. ~ 13 years predicted vs. ~ 10 years required in the lifetime. Another means of reducing the tritium permeation, if necessary, is to use an oxide barrier over a portion of the wall. This is possible because the protium concentration is lower than required. For example, using an oxide barrier over half of the wall structure would reduce the tritium permeation in half while still maintaining a low protium concentration.

3.6.3 First Wall Temperature Limit and Helium Retention Estimates

The maximum operating surface temperature allowed for the first wall is controlled primarily by thermal helium release from the wall. The desired helium retention in the wall material is roughly in the range of 20-30%. The helium flux to the wall is estimated to be $\approx 4 \times 10^{18} \text{ m}^{-2} \text{ s}^{-1}$. The vanadium deposition rate of about 1.5 mm/yr is approximately equivalent to one monolayer deposited in five seconds.

TABLE 3.6-1. TRITIUM AND PROTIUM PERMEATION AND INVENTORY CALCULATION FOR SELF-PUMPED FIRST WALL-LIMITER

Operation Time (years)	Wall Thickness (mm)	First Wall Temp. (°K)	Permeation Coefficient, ϵ	T-Permeation (g/d)	Plasma Protium Concentration	Tritium Inventory (g)
0	2	967	.0013	697	2.5×10^{-4}	1.6
1	3.5	1034	6.0×10^{-4}	324	5.3×10^{-4}	2.1
2	5	1085	3.6×10^{-4}	197	8.8×10^{-4}	2.6
3	6.5	1138	2.5×10^{-4}	133	.0013	2.8
4	8	1189	1.8×10^{-4}	96.5	.0018	3.1
5	9.5	1241	1.4×10^{-4}	73	.0024	3.2
6	11	1293	1.1×10^{-4}	57.3	.0030	3.3
7	12.5	1345	8.5×10^{-5}	46.2	.0037	3.38
8	14	1397	7.0×10^{-5}	38.1	.0045	3.44
9	15.5	1448	5.9×10^{-5}	31.9	.0054	3.47
10	17	1500	5.0×10^{-5}	27.2	.0063	3.5

Vanadium First Wall as Limiter
 Total Wall Area = 300 m^2
 Back Wall Temperature = 640°C
 D-T Particle Flux = $2.5 \times 10^{24} \text{ s}^{-1}$

Numerous experiments have shown that at low doses ($\phi = 10^{21} - 10^{22} \text{ m}^{-2}$) helium is effectively trapped in vacancies or small helium bubbles.⁽⁴⁻⁷⁾ At doses above a critical dose ϕ_c where surface blistering occurs, the implanted surface layer saturates and helium is re-emitted. Because of the continuous deposition and build up of wall material, the helium concentration inside the material is believed to be in the low dose limit ($\phi \lesssim 10^{22} \text{ m}^{-2}$) and will not exceed the critical dose for blistering. This can be shown by the fact that in a 100 seconds period the helium dose in the wall material will be of the order of $\phi \approx 10^{20} \text{ m}^{-2}$. In these 100 seconds the thickness of the wall would have increased by about 20 monolayers of vanadium which is roughly equal to the range of helium ions in the vanadium.

Because of the continuous increase of surface temperature due to the build-up and deposition of wall material, the thermal detrapping of helium can be an important factor that affects the successful operation of the self-pumped concept. In other words, the maximum allowable surface temperature is limited by the minimum amount of helium needed to be trapped inside the wall. In one experiment the thermal detrapping of helium from nickel as a function of temperature up to the melting temperature, and for various implantation doses at 300°K was studied.⁽⁸⁾ In this experiment it is shown that more than 30% of the injected helium is still trapped inside the nickel even for temperatures up to 0.7 of the nickel melting temperature and for doses up to $3 \times 10^{21} \text{ m}^{-2}$. Since there is no experimental data on helium thermal release from vanadium, it is assumed that the vanadium wall can also operate at a maximum surface temperature in the vicinity of 0.7 of the vanadium melting temperature and still retain more than 30% of the trapped helium.

In general, several important factors need to be studied experimentally to firmly establish the parameters for helium retention and thermal release, and the allowed maximum operating surface temperature for the trapping material. One important factor is the effect of implanting helium at very high temperatures. This may increase the helium release from the surface and could change the phase structure of the material.⁽⁹⁾ Alternatively because helium is simultaneously injected with the deposited wall material, a roughly constant helium profile could be maintained inside the wall. This could decrease helium release more than predicted by the nickel experiment in which helium is monoenergetically injected leading to a Maxwellian helium distribution in the

wall. However, higher helium concentrations at the surface may increase its release especially at higher temperatures. Other factors such as the properties and the structure of the redeposited materials are very important in demonstrating whether or not the new added material will be able to retain the helium.⁽¹⁰⁾ The effect of simultaneous bombardment of hydrogen on helium retention and release and the combined effect of many of the radiation damage processes that are taking place need to be studied in detail.

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3.7 Effect of a Self-Pumped Limiter System on the Fuel Recovery System

The self-pumped limiter concept affects the design of the fuel recovery system because this limiter concept is capable of maintaining needed plasma purity without the use of vacuum pumps. In particular, there no longer is a steady stream of fuel being circulated from the plasma chamber, out through vacuum ducts, through multiple pumps to a plasma exhaust system where it is purified, isotopically separated, and then returned to the plasma chamber via pellet fuelers or neutral beams. Because of this obvious difference, the need for the plasma exhaust processing subsystem was examined.

The conclusion reached from this study was that most of the components of the plasma exhaust subsystem were redundant and could be eliminated from the tritium system design for a fusion reactor design using a self-pumped limiter. The components left were the blanket processing subsystem, the fueling unit (pellet fuelers, etc.), the storage facility for tritium fuel, and the atmospheric tritium recovery unit(s). In previous designs, the storage facility had been grouped with the plasma exhaust subsystem.

In this report, two modifications of this new tritium system design have been conceived, one for liquid blankets and one for solid blankets. The descriptions and flow diagrams are found in Sections 4.3.7 and 4.4.4.1, respectively. Both systems are simpler than previous designs.

To determine if there was a capital cost advantage ensuing from this modification, an analysis was done to determine the capital cost of each of the designs, a tritium system to be used with a conventional limiter and a simplified tritium system for use with the self pumped limiter. Two reactor power levels (1200 MW_f and 4000 MW_f) were considered. The results are shown in Tables 3.7-1 and 3.7-2. (The capital costs of the fueler and the atmospheric recovery unit are not included since they would be common to both systems.) It was found that there is a capital cost savings of \$20.9 M for the 1200 MW_f plant and ~ \$33.3 for the 4000 MW_f plant.

TABLE 3.7-1. CAPITAL COSTS FOR THE PLASMA PROCESSING SUBSYSTEM FOR TWO DIFFERENT LIMITER DESIGNS

Reactor Power (MW _f)	Subsystem	Conventional/Vacuum Pumps Limiter (\$M)	Self Pumped Limiter (\$M)
1200	Storage ^a	7.5	7.5
	Processing ^b	<u>20.9</u>	<u>-</u>
	Total	28.4	7.5
4000	Storage ^a	7.5	7.5
	Processing ^b	<u>33.3</u>	<u>-</u>
	Total	40.8 ^c	7.5

^a Storage category includes receiving/shipping area, storage units, and analytical equipment. Actual amount of tritium stored is assumed to be < 1 kg.

^b Processing category includes fuel cleanup unit, isotope separation unit, small solid waste unit, tritiated gas waste unit, small tritiated water unit, associated secondary support systems, and monitors and controls for this equipment.

^c D. L. Smith, et al., "Blanket Comparison and Selection Study Final Report," Argonne National Laboratory, ANL/FPP-84-1, Chapter 5, page 5-52.

TABLE 3.7-2. CAPITAL COSTS FOR THE BLANKET PROCESSING SUBSYSTEM FOR TWO DIFFERENT LIMITER DESIGNS (LIQUID LITHIUM BLANKET/COOLANT ASSUMED)

Reactor Power (MW _f)	Conventional/Vacuum Pumps Limiter (\$M)		Self Pumped Limiter (\$M)	
	with IHX ^b	w/o IHX	with IHX	w/o IHX
1200 ^a	70	45	70	45
4000 ^a	91	66	91	66

^a In a reactor with a conventional limiter system, ≤ 0.3 kg/d of tritium is processed in the blanket system at 1200 MW; at 4000 MW ≤ 0.8 kg/d of tritium is processed. With a self-pumped limiter system, the amount of tritium/deuterium processed varies from ~ 0.3 kg/d to 2 kg/d during life of plant at 1200 MW. The amount of tritium/deuterium processed at 4000 MW would be ≤ 5 kg/d. Both blanket processing systems would process greater amounts of deuterium if excess deuterium were added to the blanket system for tritium control.

^b IHX - intermediate heat exchanger.

CHAPTER 4

BLANKET AND TRITIUM SYSTEMS

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4. BLANKET/FIRST WALL/SHIELD SYSTEM

4.1 Summary

The Blanket/First Wall/Shield design activity focused on the development of improved concepts that can be incorporated into the high β reference reactor concept developed in the related activities. Generic blanket concepts considered in this study were based on the leading candidates identified in the Blanket Comparison and Selection Study (BCSS). These included the leading self-cooled liquid metal concept (Li/Li/V) and the leading solid breeder concept (He-cooled Li_2O). In addition, an innovative concept based on a self-cooled Flibe design was developed and evaluated. Emphasis was placed on design simplicity, improved safety, reduced activation, and high performance. Several special issues, e.g., tritium containment and recovery, blanket energy multiplication, and electrical insulators were considered in detail for each concept to support the blanket development issue. Special attention was given to the general aspects of inherent safety and afterheat, the characterization of organic coolants for fusion applications, and a further assessment of beryllium for enhancing the blanket energy multiplication. Other major considerations in the blanket/first wall/shield development related to reduced reactor size and power, improved mass utilization, high reliability, and long component lifetime.

4.1.1 Self-Cooled Liquid Metal Blanket

The self-cooled liquid metal blanket concept provides several inherent features: (1) design simplicity associated with utilization of the same fluid as both breeder and coolant, (2) most of the fusion energy is deposited directly in the coolant, and (3) the coolant also serves as the tritium recovery fluid. In the present study liquid lithium, as the breeder-coolant with a vanadium alloy structure, was developed as the reference self-cooled liquid metal blanket concept. A ferritic steel structure and a LiPb breeder were considered as backup options. The MHD effects associated with self-cooled liquid metal blanket/first wall systems are substantially reduced by the lower magnetic fields required for higher β plasmas, lower neutron wall loading resulting from reduced power output, and smaller reactor size. Therefore, improved performance characteristics of self-cooled liquid metal

blanket concepts are achievable compared to the BCSS guidelines simply because of the relaxed design constraints.

Electrical insulators have been incorporated into the blanket to further reduce operating pressures. Use of flow tailoring of the liquid metal to further improve first wall cooling capability has also been evaluated. For the lower neutron wall loadings corresponding to reactor outputs of 600-800 MWe, a partially permanent blanket has been developed that could last the entire reactor lifetime. Only a relatively thin blanket section near the first wall needs to be routinely replaced, thus minimizing waste management. A high temperature shield, utilizing lithium coolant, maximizes energy recovery and eliminates the safety concern associated with a water-cooled shield. The vanadium-alloy structure potentially provides long lifetime and higher temperature operation. Placement of manifolds in the core of the higher aspect ratio torus should provide a simpler design, better maintenance access, improved safety, and reduced shield costs. Key aspects of the designs evaluated in the current study include the following:

a. Design simplicity.

The designs employ poloidal coolant ducts of rectangular cross sections. A form of MHD flow tailoring is utilized to reduce the complexity of the first wall, blanket, and reflector, and thus contribute to ease of fabrication, maintenance, and replacement.

b. Use of a first wall as an impurity control device.

A self-pumped first wall limiter represents a great improvement in overall reactor design because it eliminates a major reactor component. The associated benefits outweigh the shortcomings of reduced first wall lifetime and reduced high neutron wall loading capability. In addition to self-pumped first wall limiter designs, a number of design options with separate limiters have also been developed.

c. Modular first wall/blanket/reflector/shield construction.

Such a construction takes advantage of the radial neutron fluence gradient and requires periodic replacement of the first wall and adjacent blanket portion only. The balance of the blanket, the

reflector, and the shield are expected to last for the reactor lifetime.

d. Integrated first wall/blanket/reflector/shield.

This feature, which requires a hot shield, employs the liquid metal coolant for removal of the heat from the shield. A separate cooling system for the shield is thus eliminated. Additional advantages include removal of heat from the shield at high temperature for effective energy conversion, increase of overall blanket and shield thermal inertia for decay heat accommodation, and simplification of fabrication and maintenance. The only shortcoming appears to be the increased requirement for protection of the (TF) coils from the high temperature shield and greater constraints on materials selection.

Because the design details depend on the neutron wall loading and because, for a given wall loading, design compromises must be made, a number of design options were explored. These options utilize either a reversed poloidal or a straight through poloidal flow concept. The structural material in all cases is vanadium alloy and the coolant is lithium. Designs with separate limiters can achieve a neutron wall loading capability of about 5 MW/m^2 with bare structural walls near the first wall and insulated laminated wall construction in regions of low fluence only. When laminated wall construction is used in the first wall coolant channels, the neutron wall loading capability exceeds 10 MW/m^2 . Designs with first wall self-pumped limiters require laminated construction at the first wall coolant channels. Their neutron wall loading capability is about 5 MW/m^2 , but the first wall lifetime is limited to 3 years for such wall loadings.

The design concepts and associated issues are discussed in more detail in 4.3.1. The design configurations are described in 4.3.2. The results of blanket calculations and an outline of the methodology used in the analysis are given in 4.3.3. The structural, materials, and neutronic considerations supporting the designs are given in 4.3.4 through 4.3.6. Tritium containment issues are covered in 4.3.7. Discussion of possible design improvements for future work are presented in 4.3.8. Finally, a detailed comparison of the design options considered in the study is given in 4.3.9.

4.1.2 Helium Cooled Solid Breeder Blanket

A review of the BCSS effort indicated that the $\text{Li}_2\text{O}/\text{He}$ design was the highest rated solid-breeder concept. However, the review also indicated that major improvements in the areas of tritium breeding, blanket thickness, energy multiplication, power-conversion efficiency, breeder temperature window, integrity of purge and coolant flow paths, and tritium leakage could be made. By substituting a vanadium alloy for the BCSS HT-9 structural material, coolant outlet temperatures can be increased from 510°C to 650°C with subsequent increases in the power-conversion efficiency from 39% to 44%. By mixing beryllium with lithium oxide, the higher energy multiplication factor results in a higher thermal power generation for a given neutron wall loading and reactor size. In addition, the use of Be results in design flexibility, control of the tritium breeding ratio as coolant or structure fractions are increased, thinner inboard and outboard blankets, oxygen control to minimize tritium inventory in Li_2O and corrosion of the breeder cladding, and increased (effective) thermal conductivity of the breeder.

Technical problems associated with the use of Li_2O and vanadium alloys necessitate some major design changes in the $\text{Li}_2\text{O}/\text{He}$ blanket. Sealed breeder tubes are proposed instead of the BCSS "open" plates to eliminate concerns regarding Li_2O (LiOT) mass transfer, purge and coolant path integrity, and cladding integrity. The high permeability of vanadium alloys to hydrogen isotopes allows the tritium to permeate to the primary coolant at relatively low tritium partial pressures. Additional design modifications were then required to recover both the tritium and the heat from a high pressure (5 MPa) and high temperature (650°C) helium coolant and to protect the vanadium alloy from excessive corrosion and embrittlement. A liquid sodium tritium recovery system is proposed to remove tritium and oxygen impurities from the coolant loop with little thermal or mechanical energy loss. Also, double-walled steam generator tubes are recommended to reduce the in-leakage of steam through cracked tubes. It is estimated that for a maximum coolant temperature of 650°C , the moisture level in the helium should be kept much less than 10^{-3} vppm ($\sim 10^{-2}$ Pa). Whether the double-walled steam generator tubes and the liquid-sodium tritium-recovery system are sufficient to maintain such strict limits remains to be demonstrated.

The use of a vanadium alloy as a first wall material and the doping of

the helium coolant with 7 vppm of a hydrogen isotope to enhance tritium recovery from the solid breeder presents an additional challenge to the design. Because of the leakage of the hydrogen isotope to the plasma, it is recommended that the isotope be deuterium and that the rate of leakage of deuterium through the first wall be less than the consumption rate. This condition requires a coating on the coolant side of the first wall to act as a permeation barrier. Tungsten is recommended because of its low permeability. However, problems with depositing a thin tungsten coating on vanadium alloys, with radiation embrittlement of tungsten, and with thermal strain mismatch suggest that a more suitable material be found for this application.

The TPSS $\text{Li}_2\text{O}/\text{He}$ design offers the potential for a significant reduction in the cost of electricity as compared to the RCSS design because of the higher energy multiplication factor and the higher power-conversion efficiency. This conclusion needs to be supported quantitatively for a point design by estimating the added costs of the design innovations used to accommodate the use of a vanadium alloy as the first-wall/ blanket structural material. The key feasibility issues associated with the proposed design are:

1. The effectiveness of the liquid-sodium tritium recovery system and the double-walled steam generator tubes in maintaining the low oxygen and moisture limits imposed by the use of a vanadium alloy for the first wall and breeder cladding;
2. The development of a barrier coating material and fabrication technique for the coolant side of the first wall to limit the D_2 (and T_2) leakage to the plasma to be less than the consumption rate in the plasma; and
3. The effectiveness of the Be addition to Li_2O and D_2 addition to He in providing the reducing environment required to meet the tritium inventory limits for the breeder and the corrosion/embrittlement limits for the breeder-cladding.

4.1.3 Self-Cooled Flibe Blanket

An innovative blanket concept which utilizes the molten salt Flibe as both breeder and coolant has been evaluated. The effort focused initially on identification of the critical issues associated with a Flibe blanket and development of possible solutions for resolving these critical issues. The second part of the work involved incorporating the unique features of the Flibe into an attractive blanket concept.

The critical issues associated with Flibe are marginal tritium breeding, tritium containment/recovery, compatibility with the structure, and the operation problem associated with its high melting temperature. Analyses indicate that both compatibility and tritium containment issues may be resolved or alleviated by the addition of beryllium and/or lithium to the Flibe. The tritium breeding problem appears manageable only if a vanadium alloy is used as the structural material. Trace heating of the primary loop and the dump system will be required for startup and shutdown conditions.

The key features of the Flibe are (1) low electrical conductivity, (2) inertness to water and air, (3) low activation, (4) high volumetric heat capacity, and (5) high temperature stability. These features can be incorporated into a blanket design that is simple, safe, low pressure, compact, and high performance. The blanket system described incorporates these features on a self consistent basis. To accommodate the feature of high temperature operation, a V-based alloy is chosen as the structural material. A super-critical, one stage reheat steam cycle is chosen for the power conversion. The basic philosophy of choosing the components of the blanket/power conversion system is based on reasonable extrapolation of present day technology.

Some critical issues remain to be resolved. A number of possible schemes are proposed for tritium containment; however, they must be experimentally verified. The problems associated with using V-alloy (e.g., compatibility, nonmetallic element transport, He or H embrittlement effects) and the engineering problems associated with a high melting temperature coolant must be assessed.

4.1.4 Recommendations for Further Work

Based on the results obtained, the recommended blanket/first wall/shield activity for FY 1986 will focus on further development of the self-cooled lithium blanket with the vanadium alloy structure. Use of electrically insulated walls, both laminated and coated, to minimize MHD effects will be evaluated in more detail. Effort will also be placed on evaluation of MHD flow tailoring. More detailed evaluation will be performed on issues associated with the use of a vanadium structure, e.g., cost, use of a bimetallic heat transport system, and the potential for radiation embrittlement. An integrated power conversion and maintenance concept will be developed. The hot shield concept, high energy multiplication and shield optimization will also be studied. Low activation ferritic steels will be considered as a backup structure and Flibe as the backup breeder/coolant. Research progress on solid breeders will be monitored.

4.2 Scope/Approach

The blanket/first wall/shield design activity focused on the development of improved concepts that can be incorporated into the high β reference reactor concept developed in the ANL TPSS effort. Generic blanket concepts considered in this study were based on the leading candidates identified in the Blanket Comparison and Selection Study (BCSS). These included the leading self-cooled liquid metal concept, i.e., liquid lithium as breeder/coolant with a vanadium alloy structure, and the leading solid breeder concept, i.e., He-cooled Li_2O . In addition, an innovative concept based on a self-cooled Flibe design was developed and evaluated. Emphasis was placed on design simplicity, improved safety, reduced activation and high performance. Several special issues, e.g., tritium containment and recovery, inherent safety, blanket energy multiplication, electrical insulators, and the possible use of organic coolants were considered in detail to support the blanket development issue. Other major considerations in the blanket/first wall/shield development related to reduced reactor size and power, improved mass power density, high reliability, and long component lifetime.

The lithium/vanadium system is developed as the reference self-cooled liquid metal concept with the ferritic steel structure and LiPb breeder as backup option. The MHD effects associated with self-cooled liquid metal blanket/first wall systems are substantially reduced by the lower magnetic fields required for higher β plasmas, lower neutron wall loadings resulting from reduced power output, and smaller reactor size. Therefore, substantially improved performance characteristics of self-cooled liquid metal blanket concepts should be achievable compared to those corresponding to the BCSS guidelines because of the relaxed design constraints.

The effort included evaluation of electrical insulators to further reduce operating pressures and use of flow tailoring of the liquid metal to improve first wall cooling capability. For the lower neutron wall loadings corresponding to reactor outputs of 600-800 MWe, a partially permanent blanket that could last the entire reactor lifetime is evaluated. For this case, only a relatively thin blanket section near the first wall needs to be routinely replaced, thus minimizing waste management. A high temperature shield, which utilizes lithium coolant, maximizes energy recovery, and eliminates the safety concern associated with a water-cooled shield is evaluated. The vanadium-

alloy structure potentially provides long lifetime and higher temperature operation. Placement of manifolds in the core of the higher aspect ratio torus should provide a simpler design, better maintenance access, improved safety and reduced shield costs.

Effort on the helium-cooled Li_2O blanket concept primarily involve attempts to improve the economic performance of this blanket, which was identified as the most serious limitation for this concept in the BCSS. Potential improvements for this concept related to (1) the use of vanadium alloy as the structure to permit higher helium outlet temperatures, (2) containment of Li_2O in a sealed tube to allow higher operating temperatures, (3) elimination of one tritium recovery system by recovering tritium directly from the helium coolant, and (4) use of beryllium for energy multiplication to improve the economics.

The two key feasibility issues of the He/ Li_2O blanket relate to the use of the vanadium alloy structure and excessive hydrogen (D) permeation through the first wall from the helium into the plasma chamber. To prevent oxidation of vanadium, the oxygen potential in the system must be maintained at very low levels. Therefore, tritium will be present in the reduced form, i.e., T_2 or HT. Tritium containment and recovery become much more difficult without the ability to oxidize the tritium and without use of oxide barriers. Addition of hydrogen or deuterium appears necessary to maintain acceptable tritium inventories in the ceramic breeder. Preventing excessive hydrogen/deuterium permeation through the first wall becomes a major constraint which requires use of an effective hydrogen barrier.

An innovative blanket concept which utilizes the molten salt Flibe as both breeder and coolant is evaluated. Key features of this concept relate to (1) design simplicity, since Flibe serves as both breeder and coolant similar to the self-cooled liquid metal concepts, (2) low operating pressures for the Flibe, (3) inertness to water and air, and (4) low activation. Vanadium-base alloys are considered as the only candidate structural material because of the relatively high melting temperature (460°C) of Flibe, which requires relatively high operating temperatures. Other critical issues associated with this concept relate to startup problems associated with the high melting temperature, tritium containment/recovery, marginal tritium breeding, and compatibility with the structure.

4.3 Self-Cooled Liquid Metal: Lithium-Vanadium

4.3.1 Concepts and Issues

The Blanket Comparison and Selection Study⁽¹⁾ (BCSS) demonstrated that magnetohydrodynamic (MHD) considerations are the primary driver in the development of self-cooled liquid metal blanket configurations with acceptable thermal-hydraulic and thermo-mechanical performance. Because the BCSS was conducted under fixed reactor parameters, the fact that liquid-metal blanket design and performance depend strongly on reactor parameters, most notably magnitude of magnetic flux density, neutron wall loading, and reactor size, was not demonstrated clearly. The TPSS reactor with its higher β , and thus lower toroidal magnetic flux density, and smaller size, allows the development of alternate blanket configurations. Such configurations not only have attractive ranges of operating parameters, but they also alleviate most concerns about possible elimination of the design window, brought about by uncertainties in the MHD analysis.

In developing the self-cooled liquid-metal designs the following objectives, sometimes conflicting, were considered. These objectives refer not only to the blanket, but also to the overall attractiveness of the tokamak reactor.

- Conformance to all material stress and temperature limits. The 5% radiation induced creep strain was not adhered to because (a) it is arbitrary, and (b) such creep is non-damaging; otherwise, the same limits were used here as in the BCSS. Instead, limits to dimensional changes, resulting from radiation induced creep strain, were imposed when appropriate.
- Favorable thermal hydraulic performance. Objective criteria of such performance, for a given neutron wall loading, are the high coolant exit temperature (up to 550°C) for high thermodynamic efficiency, and low pressure drop for reduced pumping requirements and losses.
- Size of design window. A large design window is attractive not only because it indicates the ability of the reactor to operate under off-

Analyses of afterheat are conducted for the Li/Li/V and the Flibe/Flibe/V concepts to evaluate the inherent safety features of these concepts. The potential of organic coolants for fusion applications is evaluated in detail. This study also considered the resource question associated with use of beryllium for enhancing energy multiplication.

4.3 Self-Cooled Liquid Metal: Lithium-Vanadium

4.3.1 Concepts and Issues

The Blanket Comparison and Selection Study⁽¹⁾ (BCSS) demonstrated that magnetohydrodynamic (MHD) considerations are the primary driver in the development of self-cooled liquid metal blanket configurations with acceptable thermal-hydraulic and thermo-mechanical performance. Because the BCSS was conducted under fixed reactor parameters, the fact that liquid-metal blanket design and performance depend strongly on reactor parameters, most notably magnitude of magnetic flux density, neutron wall loading, and reactor size, was not demonstrated clearly. The TPSS reactor with its higher β , and thus lower toroidal magnetic flux density, and smaller size, allows the development of alternate blanket configurations. Such configurations not only have attractive ranges of operating parameters, but they also alleviate most concerns about possible elimination of the design window, brought about by uncertainties in the MHD analysis.

In developing the self-cooled liquid-metal designs the following objectives, sometimes conflicting, were considered. These objectives refer not only to the blanket, but also to the overall attractiveness of the tokamak reactor.

- Conformance to all material stress and temperature limits. The 5% radiation induced creep strain was not adhered to because (a) it is arbitrary, and (b) such creep is non-damaging; otherwise, the same limits were used here as in the BCSS. Instead, limits to dimensional changes, resulting from radiation induced creep strain, were imposed when appropriate.
- Favorable thermal hydraulic performance. Objective criteria of such performance, for a given neutron wall loading, are the high coolant exit temperature (up to 550°C) for high thermodynamic efficiency, and low pressure drop for reduced pumping requirements and losses.
- Size of design window. A large design window is attractive not only because it indicates the ability of the reactor to operate under off-

design conditions, but also because it indicates that the feasibility of a particular design is not endangered by uncertainties in the design analysis. One convenient size measure of the design window is the ratio of the nominal neutron wall loading to the maximum neutron wall loading possible for that particular design.

- Design simplicity. Although objective criteria (e.g., the number of individual modules, ducts, welds, etc.) can be used as a measure of design simplicity, for the most part, this remains a subjective issue. A further complication is added by the fact that different aspects of a design (analysis, fabrication, and maintenance) impose different requirements for simplicity. The BCSS liquid-metal-cooled design is a good example of such a situation. Although that design is "simple" from the fabrication point of view, it is complex from the MHD point of view. Such MHD complexity may ultimately result in fabrication complexity if special measures must be taken to deal with MHD-related issues.
- Safety. This very important issue has several aspects; some are related to other objectives, and some are unique. For example, design simplicity is, in part, a safety consideration in as much as a "simpler" design will be easier to implement with greater confidence. In contrast, the use of low activation materials, passive removal of afterheat, minimization of source terms in accident situations, etc. are design aspects unique to safety.
- Long lifetime. The lifetime is determined essentially by the neutron wall loading and the allowable DPA for the structural material. However, because there is a neutron fluence gradient within the blanket, the parts of the blanket which are removed from the first wall are much more long lived than is the first wall. A design which allows partial blanket removal and replacement, while maintaining the bulk of the blanket intact for the lifetime of the reactor, is clearly desirable.
- Simplified plasma impurity control devices. This objective is a step

towards reactor simplification and may indeed introduce some complexity in the blanket design. However, the benefits of the former far outweigh any shortcomings of the latter.

- Increased energy multiplication in the blanket. The benefits are obvious. What is less obvious is the fact that, although increased energy multiplication places additional demands on thermal hydraulics, these demands are easier to accommodate than those resulting from increases in the neutron wall loading corresponding to the same total energy deposition in the blanket.
- Unified design of blanket and shield. Such a design will eliminate a separate cooling system for the shield (overall simplification) and will remove the heat deposited in the blanket at a high temperature suitable for efficient energy conversion (increased performance). In order to achieve maximum benefit, a high temperature shield must be utilized.

All of the afore mentioned objectives were successfully addressed in the blanket design development process. Because some of the objectives overlap and/or conflict with each other, compromises had to be made to arrive at the proposed designs. The compromises represent judgements on the relative importance of the different objectives in arriving at overall attractive designs.

Finally, it should be stressed that, although the stated objectives and the overall blanket concepts are not peculiar to TPSS, the detailed designs and operating parameters are.

The design concepts, described in more detail in the following section, were developed around simple poloidal flow channels for first wall cooling and blanket heat removal. The two variations of this scheme utilize coolant channels extending over one half and over the entire poloidal length of the inboard blanket. The blanket material in contact with the liquid lithium coolant is vanadium alloy for low activation (safety), high operating temperatures (improved thermodynamic performance), and high DPA limit (long life). The portion of the blanket adjacent to the first wall is removable for replacement when the dpa limit is reached, whereas the remainder of the

blanket, the blanket/reflector, and the shield are expected to last over the reactor lifetime. Separate limiters and self-pumped first wall limiters are both included in the concepts studied, with the former being more suitable for longer lifetime and higher neutron wall loading.

In order to control the MHD pressure drop, laminated wall construction was employed. In some of the designs such laminated construction was used only in the inlet and outlet ducts and in the portions of the blanket removed from the first wall, where the neutron fluence is low. In others, laminated construction was also used in the first wall channels. The use of insulating wall coatings is not deemed necessary for the lower neutron wall loading concepts. From the results obtained so far in the TPSS, the benefits of using such insulating coatings for higher neutron wall loading can be readily ascertained.

During the first phase of the study the objective was to develop attractive designs for a 1000 MW_{th} reactor. This was accomplished without any serious compromises involving the afore mentioned objectives. In fact, the design constraints for a reactor of such a thermal output did not prove to be demanding and no optimization of the designs was necessary. In the second phase of the study, emphasis was placed on higher neutron wall loading and thermal output. It soon became evident that trade-offs involving the basic design objectives were necessary. The trade-offs and their implications were explored so that informed design choices can be made in the future.

4.3.2 Design Configurations

Two design configurations have been analyzed in detail. The first, to be referred to as DESIGN A throughout this report, is a reversed poloidal flow concept. Such a concept, shown in Fig. 4.3.2-1, was briefly considered in the BCSS, where it was called dual-channel poloidal flow. Each "channel" is really a collection of a number of individual channels. This concept is an alternate to the reference BCSS design. The second configuration, DESIGN B, is a multiple poloidal channel concept, Fig. 4.3.2-2, similar to the MARS blanket concept.

The coolant channels for both designs are rectangular in cross section. The design of the first wall channels is the most critical. The first wall is a continuous plate, Fig. 4.3.2-3a, (thickness: 1.0-4.0 mm) attached to radial ribs to form the first wall coolant channels. The thickness of 1.0 to 4.0 mm does not include the additional material which will be eroded away during the lifetime of a blanket in the presence of a conventional limiter or that which will be deposited in the self-pumping first wall limiter concept. This additional thickness is grooved to reduce thermal stresses and does not enter in the structural integrity considerations.

The first wall plate must be nearly parallel to the toroidal direction. It must also conform to the plasma in the poloidal direction when a first-wall-self-pumped-limiter concept is used. A slight curvature of the first wall between the ribs enhances the pressure bearing capability of the wall (see Section 4.3.4) and may be used, if necessary.

Because the pressure drop is determined by the coolant velocity at the first wall and the corresponding rib thickness there, the minimum rib thickness is taken to be 2 mm. As an MHD flow control device, the rib thickness may be increased with the distance from the first wall. This will result in reduced coolant velocity away from the first wall and, therefore, more uniform coolant temperature. As a side benefit, the stresses in the thickened parts of the ribs will decrease, thus reducing the portion of the blanket subjected to high stresses. This method of MHD flow control can be used throughout the blanket making it possible, in principle, to have a coolant channel extending radially throughout the entire blanket thickness for DESIGN B, and throughout the front part of the blanket for DESIGN A. In practice, toroidal walls will be needed to provide support to the ribs so that pressure forces in the

Figure 4.3.2-1. DESIGN A. Reversed poloidal flow concept. Each flow path shown in the figure consists of several coolant ducts, depending on neutronic and thermal considerations. If separate removal of the blanket and first wall is required, the reflector will be provided with an inflow region similar to that of the blanket.

Figure 4.3.2-2. DESIGN B. Once through poloidal flow concept. The flow paths shown in the figure consist of several coolant ducts, depending on neutronic and thermal considerations.

Figure 4.3.2-3a First wall and blanket coolant channels. The continuous webs vary in thickness in the radial direction. The variation is designed to result in non-uniform radial velocity distribution and uniform temperature distribution. The perforated plates support pressure forces in the toroidal direction. Their thickness is determined, primarily, by neutronic considerations.

toroidal direction can be supported. Such walls, however, are not needed for flow containment and can be perforated. Their thickness is immaterial to flow and pressure distribution and can be chosen so as to satisfy neutronic requirements. The coolant pressure at each poloidal station of DESIGN B will be thus uniform, as will be the temperature, with appropriate rib thickness distribution. The same can be accomplished for the front and the back portion of the blanket for DESIGN A.

The toroidal separation between the ribs, referred to as the span in this report, can be chosen between the values of 2.0 to 4.0 cm. Higher values of the span result in reduced pressure in the blanket because the pressure drop is inversely proportional to the span. On the other hand, the pressure bearing capability of a flat first wall is inversely proportional to the square of the span. Therefore, loss of pressure bearing capability is more rapid than the pressure reduction. To compensate for this effect the first wall thickness can be increased. This action, however, will result in larger temperature drop through the first wall and, consequently, in larger coolant velocity to maintain the peak material temperature within allowable limits. A higher coolant velocity will, of course, result in higher pressure drop. As a result of such interdependence the first wall thickness, the span, and the coolant velocity must be determined simultaneously by proper consideration of MHD, thermal, and structural effects.

Because the magnitude of the span determines the number of coolant channels and, therefore, reflects on the complexity of the blanket, larger values of the span are desirable. Although, the span was maintained between the limits of 1.0 to 4.0 cm for most of this study, so that higher neutron wall loadings could be possible, acceptable designs of blankets with separate limiters and neutron wall loadings of 2.4 MW/m^2 were developed with values of the span up to 10 cm.

Another way of reducing the total number of coolant channels and, thus, blanket complexity is to increase the span of the channels further away from the first wall, as shown in Fig. 4.3.2-3b. Since the pressure at any poloidal station can be made to be uniform, the toroidal walls separating the channels are not pressure loaded and these walls can be made as thick or thin as neutronic considerations dictate. They can also be perforated.

Figure 4.3.2-3b First wall and blanket coolant channels. Same as in Fig. 4.3.2-3a except that, in order to reduce the number of coolant channels and blanket complexity, the cross sectional dimensions of the coolant channels removed from the first wall are increased.

It is seen then that, with the exception of the first wall thickness and the span of the first wall channels, there is considerable latitude in the design of both blanket configurations to allow for a final design which will accommodate neutronic, fabrication, and other considerations.

There are, however, additional considerations which will affect the overall blanket thickness. For DESIGN A, the total radial thickness available for flow of the portion of the blanket adjacent to the plasma is, for a given neutron wall loading, inversely proportional to the average coolant temperature rise through the entire blanket. This thickness is not large. The attractiveness of DESIGN A is based on the MHD pressure drop being small over the returning flow leg of the blanket. This can be achieved by either providing a combined thickness available for the return flow which is several times larger than that of the front leg of the blanket, or by providing insulated liners for the returning leg of the blanket. Although the latter solution can be very attractive, it is not of great help for those designs for which insulated liners for the front leg of the blanket have already been assumed. For these designs there will be a competition between the need for available flow area, and the need of high structural to coolant ratio dictated by neutronic considerations. Such competition may ultimately result in increased blanket-reflector-shield thickness. For DESIGN B, available flow area is not a concern. A total radial blanket thickness available for flow of 30 cm is more than adequate.

If the first wall is to be used as a self-pumped limiter, the large temperature drop through the thick deposited layer dictates a higher coolant velocity. Higher coolant velocities are also required in designs with high neutron wall loadings. Such high velocities require that insulated liners be used in the first wall channels. For heat transfer reasons a liner is used only on the three sides of the rectangular coolant channel (the first wall side remains bare). Whereas such use of insulators in the high neutron fluence environment of the first wall is more challenging than their use in the back portion of the blanket and in the inlet and outlet ducts. Our work to date, see Section 4.3.5, identifies a number of insulator materials which are good prospects for such an application.

The shield design considerations are covered in detail in Section 4.3.6. As far as the overall blanket and shield design is concerned it should be

pointed out that a concept such as that shown in Fig. 4.3.2-1 has a number of desirable features. First, the same coolant system with the same inlet and outlet temperatures as that for the blanket proper is used. The particular flow path proposed has the advantage of maintaining the back of the shield at a comparatively low temperature ($\sim 300^{\circ}\text{C}$) for minimization of thermal insulation requirements. The heat removal requirements are low as will be the velocities required. As a result the MHD pressure drop is expected to be low. An MHD throttle (a single piece of thick wall duct) can be used before the inlet to the shield to maintain the entire shield at low pressure.

DESIGN A was a natural design solution to the presence of a toroidal "pusher" coil, which was used in earlier TPSS plasma calculations to achieve the second stability regime required for high β 's. Although the "pusher" coil is no longer a part of the TPSS reactor design, DESIGN A was retained because of some of its attractive features which will be discussed in Section 4.3.9. An early concern about this particular design was its apparent lack of coolant draining capability. Such a concern is readily addressed by simply providing for a fluid passage between the two halves of the blanket. Since the pressures on both sides of the passage are equal, there is no flow through the opening under normal operating conditions. To further ensure the absence of flow during operation, an MHD valve can be installed at the opening. Such an MHD valve is nothing more than a thick conducting rim around the mouth of the opening. The high pressure drop required to drive the fluid through the opening will ensure virtual lack of flow for reasonably well balanced pressures at the two halves of the blanket.

4.3.3 Blanket Analysis

As explained in Section 4.3.2 the blanket performance is primarily dictated, for both DESIGN A and B, by the first wall thickness and the toroidal dimension of the first wall coolant channels (span). For a given neutron wall loading, there are many acceptable combinations of wall thickness and span, each combination corresponding to a different coolant pressure.

To arrive at an acceptable design, simultaneous attention must be paid to MHD, structural, and heat transfer considerations. Figure 4.3.3-1 shows the result of heat transfer analyses of first wall cooling in the presence of a separate limiter. This figure can be used to obtain the necessary coolant velocity for a given neutron wall loading for a variety of first wall thicknesses. The analysis is for a coolant inlet temperature of 280°C and a peak structural material temperature of 750°C (vanadium alloy). Although designs with higher inlet temperature and lower structural temperatures are certainly feasible, the largest possible neutron wall loadings can be achieved when these temperatures are allowed to reach their aforesaid limits. It is important to examine, in the future, the performance of designs with lower peak structural temperatures because of the uncertainties associated with the material temperature limit of vanadium alloys. In addition, the results of Fig. 4.3.3-1 refer to heated first wall poloidal length of 2 m (DESIGN A). For DESIGN B, where the heated length is doubled, the required coolant velocity will be also doubled (this is only approximately true).

Figure 4.3.4-2 of Section 4.3.4 summarizes the results of structural analyses in the form of allowable coolant pressures for different spans, first wall thicknesses, and first wall initial deflections.

The MHD pressure drop, which depends on both the span and the magnetic flux density (which in turn varies with neutron wall loading), provides the remaining ingredient necessary for the design analysis. Because of the simplicity of both blanket designs, the MHD analysis becomes simple. The pressure drop is computed on the basis of the poloidal heated length (2 m for DESIGN A and 4 m for DESIGN B) increased by 50% for DESIGN A and 20% for DESIGN B to account for the pressure drop over the inlet and outlet ducts, the return leg and the bend of DESIGN A, etc. The resulting formulae for first wall channels without insulators are:

Figure 4.3.3-1 Maximum neutron wall loading as a function of the coolant velocity and first wall thickness. Peak material temperature: 750°C. Coolant inlet temperature: 280°C. First wall heated length: 2 m.

$$\Delta p = \frac{10.2 \times V \cdot P_N^{1/2}}{\text{span}}, \text{ MPa for DESIGN A}$$

and

$$\Delta p = \frac{16.3 V \cdot P_N^{1/2}}{\text{span}}, \text{ MPa for DESIGN B}$$

where V is the first wall channel average coolant velocity in m/s and P_N is the neutron wall loading in MW/m^2 . The square root dependence of the pressure drop on neutron wall loading arises from the fourth root dependence of the magnetic flux density as neutron wall loading.

When insulated liners of thickness of 0.2 mm are used on the three sides of the first wall channels, the right hand side of the pressure drop formulae is multiplied by 0.2.

To arrive at the peak neutron wall loading that a particular choice of first wall thickness and span will accommodate, Fig. 4.3.3-2 is first used to obtain the allowable pressure for a given first wall thickness, span, and initial curvature. Then the average coolant velocity is varied until the neutron wall loadings, as obtained from the MHD pressure drop formulae and from the curves of Fig. 4.3.3-1, coincide. This value of the neutron wall loading is the maximum allowable for the particular choice of parameters.

Table 4.3.3-1 summarizes the results of numerous such calculations for both designs with separate limiters and without insulators on the first wall channels. For each of the twelve combinations of span (2 cm, 3 cm, and 4 cm) and first wall thickness (1 mm, 2 mm, 3 mm, and 4 mm) calculations were done for an initially flat first wall, a wall with an initial deviation from flatness of 1 mm, and one with an initial deviation of 2 mm. The 1 mm first wall thickness proved to be too small for the 4 cm span. The 4 mm first wall has enough flexural rigidity, and deflections of 1 mm and 2 mm do not provide any substantial benefit.

The following conclusions can be drawn from the results of Table 4.3.3-1:

- (a) Decreased span and increased initial deviation from flatness (both of which can be seen as adding to design complexity) result in increased neutron wall loading capability. This is because both provide increased pressure bearing capability of the first wall, allowing higher coolant velocities.

TABLE 4.3.3-1

PERFORMANCE OF BLANKETS WITHOUT INSULATORS. SEPARATE LIMITERS.

	SPAN: 4 cm			SPAN: 3 cm			SPAN: 2 cm		
	1 mm		2 mm	1 mm		2 mm	1 mm		2 mm
	Flat	Deviation	Deviation	Flat	Deviation	Deviation	Flat	Deviation	Deviation
	First Wall Thickness - 1 mm								
PRESSURE DROP, MPa	--	--	--	0.35	0.6	1.0	0.8	1.3	2.7
DESIGN A									
Velocity FW Channel, m/s				0.06	0.1	0.15	0.09	0.13	0.23
Neutron Wall Loading, MW/m ²				2.4	3.2	4.3	3.2	3.8	5.3
FW Channel Radial Dimension, cm				19.5	15.5	14	17.5	14.5	11.5
DESIGN B									
Velocity FW Channel, m/s				0.05	0.8	0.12	0.07	0.10	0.18
Neutron Wall Loading, MW/m ²				1.5	2.0	2.7	2.0	2.4	3.4
Average Velocity, m/s				0.035	0.050	0.065	0.050	0.060	0.085
	First Wall Thickness - 2 mm								
PRESSURE DROP, MPa	0.8	0.9	1.3	1.5	1.6	2.3	3.3	3.6	5.0
DESIGN A									
Velocity FW Channel, m/s	0.15	0.17	0.23	0.2	0.22	0.28	0.28	0.29	0.38
Neutron Wall Loading, MW/m ²	4.0	4.2	4.9	4.6	4.8	5.5	5.5	5.6	6.4
Front Channel Radial Dimension, cm	13	12	10.5	11.5	10.5	9.5	9.5	9.5	8.5
DESIGN B									
Velocity FW Channel, m/s	0.12	0.14	0.18	0.16	0.18	0.22	0.22	0.23	0.30
Neutron Wall Loading, MW/m ²	7.6	2.7	3.1	2.9	3.1	3.5	3.5	3.6	4.1
Average Velocity, m/s	0.06	0.065	0.075	0.070	0.075	0.085	0.085	0.090	0.10
	First Wall Thickness - 3 mm								
PRESSURE DROP, MPa	1.8	1.9	2.3	3.3	3.6	4.0	7.4	7.4	8.7
DESIGN A									
Velocity FW Channel, m/s	0.3	0.32	0.38	0.4	0.43	0.45	0.55	0.55	0.62
Neutron Wall Loading, MW/m ²	5.2	5.3	5.6	5.8	6.1	6.2	6.8	6.8	7.2
Front Channel Radial Dimension, cm	8.5	8.0	7.5	7	7	7	6	6	6
DESIGN B									
Velocity FW Channel, m/s	0.24	0.26	0.30	0.32	0.34	0.36	0.44	0.44	0.50
Neutron Wall Loading, MW/m ²	3.3	3.3	3.5	3.6	3.8	3.9	4.3	4.3	4.5
Average Velocity, m/s	0.08	0.08	0.085	0.090	0.095	0.095	0.10	0.10	0.11
	First Wall Thickness - 4 mm								
PRESSURE DROP, MPa	3.3	--	--	5.9	--	--	13.2	--	--
DESIGN A									
Velocity FW Channel, m/s	0.52			0.66			1.0		
Neutron Wall Loading, MW/m ²	6.2			6.7			7.8		
Front Channel Radial Dimension, cm	6			5			4		
DESIGN B									
Velocity FW Channel, m/s	0.42			0.53			0.7		
Neutron Wall Loading, MW/m ²	3.9			4.2			5.2		

- (b) Increased first wall thickness also results in increased neutron wall loading for the same reason. In fact, this can be seen to be a preferable means of increasing neutron wall capability as the pressure drop penalty is not as high. Of course, the first wall thickness cannot be allowed to increase indefinitely, because, beyond an optimum thickness, the temperature drop through the thick first wall will result in high required coolant velocities, thus negating any benefit resulting from increased flexural rigidity.
- (c) DESIGN B has, for the same pressure drop, a lower peak neutron wall loading capability than DESIGN A. In fact, it can be shown that, for the same pressure drop, the peak neutron wall loading capability of DESIGN B is 65% of that of DESIGN A.
- (d) The maximum allowable operating pressure is determined by the span and the first wall thickness and is, therefore, independent of the design concept.
- (e) The front channel radial dimension, given only for DESIGN A where it is critical, may have an impact on overall blanket thickness, as explained in Section 4.3.2. The available flow area through the entire blanket thickness should be at least 2-3 times larger than the dimension given in the table.
- (f) The average coolant velocity through the blanket, given only for DESIGN B where it is meaningful, was computed on an overall thickness available for flow of 40 cm. The results indicate that, since the average velocity is considerably lower than the first wall channel velocity, the available flow area in the blanket will not be an issue for this design concept.
- (g) Although it is not evident from simple inspection of the table, operation at neutron wall loadings below the maximum possible value will result in significant reduction in operating pressure and margin of safety. For example, if a span of 4 cm and a first wall thickness of

TABLE 4.3.3-2

PERFORMANCE OF BLANKETS WITH INSULATORS. SEPARATE LIMITERS.

	Span: 4 cm	Span: 3 cm	Span: 2 cm
<u>FIRST WALL THICKNESS, 1 mm</u>			
DESIGN A			
Velocity in FW Channel, m/s	0.47	0.47	0.47
Neutron Wall Loading, MW/m ²	8.0	8.0	8.0
Pressure Drop, MPa	0.7	0.9	1.35
Allowable Pressure Drop, MPa	0.7 ^a	1.0 ^a	1.35 ^b
FW Channel Radial Dimension, cm	8.5	8.5	8.5
<u>FIRST WALL THICKNESS, 2mm</u>			
DESIGN A			
Velocity in FW Channel, m/s	0.6	0.6	0.6
Neutron Wall Loading, MW/m ²	8.0	8.0	8.0
Pressure Drop, MPa	0.85	1.15	1.75
Allowable Pressure Drop, MPa	0.9 ^b	1.5	3.3
FW Channel Radial Dimension, cm	6.5	6.5	6.5
DESIGN B			
Velocity in FW Channel, m/s	0.44	0.58	0.8
Neutron Wall Loading, MW/m ²	4.9	5.6	6.4
Pressure Drop, MPa	0.8	1.5	3.3
Allowable Pressure Drop, MPa	0.8	1.5	3.3
Average Velocity, m/s	0.12	0.14	0.16
<u>FIRST WALL THICKNESS, 3 mm</u>			
DESIGN A			
Velocity in FW Channel, m/s	0.8	0.8	0.8
Neutron Wall Loading, MW/m ²	8.0	8.0	8.0
Pressure Drop, MPa	1.13	1.54	2.3
Allowable Pressure Drop, MPa	1.8	3.3	7.4
FW Channel Radial Dimension, cm	5.0	5.0	5.0
DESIGN B			
Velocity in FW Channel, m/s	0.9	1.15	1.6
Neutron Wall Loading, MW/m ²	6.1	6.9	8.0
Pressure Drop, MPa	1.8	3.3	7.4
Allowable Pressure Drop, MPa	1.8	3.3	7.4
Average Velocity, m/s	0.15	0.17	0.20
<u>FIRST WALL THICKNESS, 4 mm</u>			
DESIGN A			
Velocity in FW Channel, m/s	1.12	1.12	1.12
Neutron Wall Loading, MW/m ²	8.0	8.0	8.0
Pressure Drop, MPa	1.6	2.15	3.2
Allowable Pressure Drop, MPa	3.3	5.8	13.2
FW Channel Radial Dimension, cm	3.5	3.5	3.5
DESIGN B			
Velocity in FW Channel, m/s	1.5	1.9	2.24
Neutron Wall Loading, MW/m ²	7.1	7.7	8
Pressure Drop, MPa	3.3	5.8	10.3
Allowable Pressure Drop, MPa	3.3	5.8	13.2
Average Velocity, m/s	0.17	0.19	0.20

^a2.0 mm deviation required in this case to reach 8 MW/m².^b1.0 mm deviation required in this case to reach 8 MW/m².

4 mm is chosen for operation at 5 MW/m^2 instead of the maximum possible of 6.2 MW/m^2 for DESIGN A, the required coolant velocity would drop to 0.33 m/s and the pressure would drop to 2.1 MPa (36% reduction). Alternatively, if the pressure drop and the coolant velocity is maintained, the peak material temperature of 750°C would be reduced considerably.

Table 4.3.3-2 gives similar results for both DESIGNS A and B with separate limiters and with insulated liners on three sides of the first wall channels. In this case, with a few exceptions noted in the table, only flat walls were considered. Also, because no effort was made to extend the neutron wall loading beyond a value of 8 MW/m^2 , a value which is easily attained with DESIGN A, the results are presented in a different format. When the neutron wall loading capability for a particular set of parameters exceeds 8 MW/m^2 , a neutron wall loading of 8 MW/m^2 is chosen and an operating pressure for this loading is given. Obviously, the operating pressure is then lower than the allowable pressure, which is also given. For those cases (almost all cases for DESIGN B), where the peak neutron wall loading that can be achieved is less than 8 MW/m^2 , the operating and allowable pressures are identical.

Similar conclusions, to those drawn from the results of Table 4.3.3-1, can be drawn from the results of Table 4.3.3-2.

In addition to the results on maximum neutron wall loading capability, given in Tables 4.3.3-1 and 4.3.3-2, designs for a neutron wall loading of 2.4 MW/m^2 , corresponding to $\sim 1000 \text{ MW}_{\text{th}}$ power output, were developed for both separate limiters and first wall self-pumped limiters. The features of these designs are summarized in Table 4.3.3-3.

As can be seen from the results of this table, both DESIGNS A and B with separate limiters have perfectly acceptable operating parameters. DESIGN B has a larger pressure drop than does DESIGN A but, even so, the pressure drop is not high. It should be noted that none of the design constraints has been reached. The pressure is considerably lower than the allowable pressure, as are the peak material and interface temperatures than their respective limits. Although the results for insulated first wall channel liners are also given, it is clear that such insulated liners are not needed for the separate limiter designs. The same cannot be said for the first wall as a limiter design. The

TABLE 4.3.3-3. BLANKET DESIGNS FOR 2.4 MW/m^2 NEUTRON WALL LOADING

	SEPARATE LIMITER				FW LIMITER	
	DESIGN A		DESIGN B		DESIGN A	DESIGN B
	NO INSUL.	INSUL. LINER	NO INSUL.	INSUL. LINER	INSUL. LINER	INSUL. LINER
First Wall Thickness, mm	4	4	4	4	1.5	1.5
First Wall Channel Span, cm	4	4	4	4	4	2
First Wall Channel Velocity, m/s	0.12	0.12	0.24	0.24	0.25	0.50
Pressure Drop, MPa	0.5	0.1	1.5	0.3	0.2	0.8
Allowable Pressure Drop, MPa	3.3	3.3	3.3	3.3	0.45	1.8
Inlet Temperature, °C	300	300	300	300	330	330
Outlet Temperature, °C	550	550	550	550	550	550
Maximum Interface Temp., °C	630	630	630	630	640	640
Maximum Material Temp., °C	700	700	700	700	710	710
Lifetime, years	10	10	10	10	10	10

temperature drop through the 15 mm thick deposited layer requires that the first wall be as thin as possible (here 1.5 mm). The thin first wall results in low allowable coolant pressures which can only be met with insulated liners on the first wall channels.

The results for the first wall as limiter designs are conservative in that no credit was taken for the added stiffness of the first wall resulting from the thick deposited layer. A more ambitious design would specify a lower coolant velocity, and thus pressure, at the beginning of life. The velocity would then be allowed to increase gradually with time to provide the added cooling requirement brought about by the deposited layer. The increased pressure would not result in substantially higher stresses because of the added stiffness of the first wall. Additional information on the self-pumped limiter concepts and for the first wall as limiter concept at increased neutron wall loadings is given in the section on impurity control.

The uncertainties involved with the structural, thermal, and MHD analyses are expected to be minimal as a result of the simplicity of the design configurations. In fact, in order to simplify the analyses, credit was not taken for a number of real effects which would have increased the operating range of the blanket. Some of these effects are as follows:

- (1) The allowable stress was obtained for the highest material temperatures considered. Yet it is known that the location of the highest stress does not coincide with the location of the highest material temperature. In fact, the maximum stress occurs at the blanket inlet where the first wall temperature is the lowest; whereas the peak temperature occurs at the blanket mid-point for DESIGN A or blanket exit for DESIGN B where, in both cases, the pressure is very low.
- (2) The allowable stress in the first wall is determined from considerations of strength and thermal creep. The prevailing stresses in the first wall are, at the beginning of life, bending stresses. As time progresses, the first wall assumes a curvature resulting from non-damaging radiation induced creep. Such curvature increases the load bearing capability of the first wall, as can be seen in Fig. 4.3.4-2 or, viewed differently, relieves the high bending stresses. In

effect, the allowable stresses to which the bending stresses must be compared are those corresponding to shorter lifetime than the actual one.

- (3) The critical design calculations are those pertaining to the inboard blanket because the magnetic flux density is higher there, but the neutron wall loading is lower at the inboard blanket by as much as 20% below the average value. As a result, the average neutron wall loadings can be 20% higher than the values quoted in Tables 4.3.3-1 and 4.3.3-2.

It is seen then that the analyses are both straightforward and, in a number of aspects, conservative. The only optimism injected in the analysis, apart from that associated with temperature and stress limits of vanadium alloys, is related to the feasibility of insulated liners in the first wall channels, for those design options which use such insulators. The discussion of Section 4.3.5 indicates that such optimism is justified.

As regards insulated liners and coatings, the following considerations are also of importance. A 0.2 mm insulated liner provides a five fold reduction in pressure drop. Such a reduction has been shown to be more than adequate. An insulated coating can result in a two hundred fold reduction in pressure drop. Such a large reduction is not, of course, necessary. Insulated coatings should be considered mainly because their application will be, in all likelihood, easier than the fabrication of laminated walls.

If a pressure drop reduction of only several fold is needed, the insulating surface layer thickness and resistivity can be much smaller than the values quoted in Section 4.3.5. In fact, submicron layers of most oxides or nitrides will be adequate. The possibility of creating such layers with appropriate additives in the lithium and/or structure is then a very attractive one. Such layers may, in addition to providing relief in the MHD pressure drop area, result in reduced corrosion rates.

The choice of vanadium alloy as a structural material can also be considered optimistic in that the material temperature and fluence limits as well as the allowable stresses are based on limited data. The limits used here are the same as those used in the BCSS, except for the structural temperature limit of 750°C which exceeds the BCSS limit of 720°C.

4.3.4 Stress Analysis

The first wall of a liquid metal cooled blanket will be subjected to coolant pressure as well as thermal loadings. The coolant pressure will cause bending in a flat first wall, and the maximum displacement at the center of a span will increase with fluence due to radiation induced creep. Both the maximum stress and displacement in the first wall can be substantially reduced by putting a slight curvature on the first wall. Because of its impact on MHD pressure drop and also on the limiter design, the maximum displacement of the first wall cannot be allowed to increase indefinitely. However, the precise allowable displacement not being known at the present, the results are presented in a parametric form. Additional displacement of the first wall occurs because of a temperature distribution in the blanket. The magnitude of this displacement depends on a number of factors such as support configuration for the blanket, the connectivity between various structural elements of the blanket, etc. Detailed finite element analyses of the blanket are presented to explore possible effects of connectivity between the main structural elements of a reversed poloidal flow blanket.

4.3.4.1 Slightly Curved First Wall

The allowable pressure on a flat first wall can be substantially increased by putting a slight curvature on the first wall so the pressure loading, instead of being contained by bending stresses, is contained by predominantly membrane stresses. A series of finite element analyses was carried out for a curved beam subjected to internal pressure as shown in Fig. 4.3.4-1. The first wall was assumed to be clamped at both edges with no relative displacement being allowed between the supports. The material was V-15Cr-5Ti, and the average temperature was taken as 700°C. The design life was assumed to be 40,000 hrs ($S_{mt} = 165$ MPa) with an end-of-life fluence of 250 dpa. The maximum allowable pressure P_{max} was defined as the pressure needed to produce a stress of S_{mt} (unirradiated) in the first wall, where the stress allowable S_{mt} was determined from the yield, ultimate, thermal creep, and stress rupture properties of the material. Since radiation induced creep is non-damaging, the 5% radiation creep limit was not used. Instead, a series of curves indicating the total maximum end-of-life deviation from a flat first wall due to radiation induced creep were determined. The actual allowable pressure on

Figure 4.3.4-1 Finite element model of a slightly curved first wall of initial deviation from flatness H subjected to pressure loading p .

the first wall is the lesser of $P_{\max}$ and the pressure required to produce a given maximum deviation from flatness at end-of-life. Figure 4.3.4-2 gives the allowable pressure as a function of the initial deviation from flatness, H , of the first wall, and for spans of 2, 3, and 4 cms and first wall thicknesses of 1, 2, and 3 mms (exclusive of any grooved thickness). Note that the allowable pressure becomes determined by displacement constraints when either the thickness of the first wall is decreased or the initial deviation from flatness (H) and the span of the first wall is increased.

The $P_{\max}$ in these figures varies linearly with the allowable stress S_{mt} . For example, if the design life were decreased by a factor of two to 20,000 h, then the allowable stress S_{mt} would be increased from 165 MPa to 190 MPa, and the curves marked $P_{\max}$ should be raised by a factor 1.15. Similarly, the curves corresponding to various values of maximum deviations from flatness at end-of-life are approximately proportional to the inverse of the fluence. If the end-of-life fluence were reduced by a factor of two to 125 dpa, then these curves should be raised by a factor of two. Note that the allowable pressure, based on considerations of radiation induced creep deformation, varies linearly with the inverse of the design fluence. On the other hand, the allowable pressure, based on strength and thermal creep rupture properties, is far less sensitive to the design life. Thus, the designer has a lot of flexibility to choose the maximum allowable pressure, as determined by displacement constraints, by adjusting the design life (fluence). However, his ability to increase the maximum allowable pressure (based on strength constraints) by decreasing the design life, is much more limited.

4.3.4.2 Thermal Stress and Displacement

A series of finite element thermal stress analyses of a dual channel poloidal flow blanket has been completed. The results show that the geometry and the thicknesses for the various channels have to be carefully selected so that unacceptably large deflections do not occur anywhere in the blanket. Both the deflection and the maximum stresses in the first wall can be reduced by providing rigid structural connection between the inlet and the outlet channels.

Figure 4.3.4-3 shows the simplified geometry used in the analysis with the coolant at the inlet entering the blanket at a temperature of 280°C, and

FIRST WALL THICKNESS - 1 mm

FIRST WALL THICKNESS - 2 mm

FIRST WALL THICKNESS - 3 mm

Figure 4.3.4-2 Design pressure for a slightly curved V-15Cr-5Ti first wall as function of initial deviation from flatness H and span for first wall thicknesses of 1 mm, 2 mm, and 3 mm. $P_{\max}$ is determined on the basis $S_{mt} = 165$ MPa corresponding to temperature of 700°C and a time of 40,000 h. $P_{\max}$ for a different value of S_{mt} should be obtained by direct linear scaling. The maximum pressures for different values of deviation from flatness at end of life (EOL) correspond to a fluence of 250 dpa. These values scale linearly with the reciprocal of the fluence.

Figure 4.3.4-3 Idealized geometry of a dual channel poloidal flow blanket consisting, in general, of four walls.

Figure 4.3.4-4 Distribution of wall temperature assumed for thermal stress analysis. Initial stress free temperature is assumed to be 280°C.

after a 180° turn at the top, exiting the blanket at the bottom at a temperature of 550°C. The large temperature difference between the inlet and the outlet channels can lead to large deflections in the blanket if the blanket is not provided with sufficient bending stiffness. Although the hot coolant is recirculated into the back of the blanket thereby reducing the temperature gradient between the front and the back of the blanket, the analyses show that, in order to reduce both the maximum stress and the displacement of the first wall, the front channel should be rigidly connected to the back channel. This is true because, if the two channels were structurally separated, in order to reduce the maximum deflection in the first wall, the hot first wall would have to be connected structurally to the second wall. Since the second wall has the lowest average temperature of all the four walls, it exerts a maximum constraint on the first wall. Although the constraint can be reduced by reducing the thickness of the second wall, the reduced thickness also makes the deflection of the front channel unacceptably large. A structural separation of the two channels could be profitably used if the flow in the blanket could be reversed thus bringing in the cold coolant at the back of the blanket instead of at the front. However, considerations of heat transfer efficiency preclude this possibility.

The temperature distribution assumed for the analysis is shown in Fig. 4.3.4-4. No surface heat flux is assumed for the horizontal section of the blanket. A constant temperature gradient of 90°C is assumed to persist in the 3 mm thick first wall throughout the vertical section. Pressure stresses are not included in the analysis. The temperature of the second wall is assumed to increase uniformly from 280°C at the inlet, to 310°C at the base of the vertical section, to 480°C at the top where the coolant makes a 180° turn. The back wall temperature increases from 480°C at the top to 550°C at the outlet. The boundary conditions assumed for the stress analyses are such that the inlet and the exit channels are prevented from any vertical displacement at the far end, and the back wall is supported against any horizontal displacement at the top and at the bottom. Plane strain (i.e., $\epsilon_z = \text{constant}$) condition is assumed to prevail in both the channels. However, the constant strains (ϵ_z) for the two channels can be different if they are assumed to be structurally separate.

An initial analysis which assumed that the first wall was hung from the top without being attached structurally to the second wall, showed that although the stresses are quite low, the displacements of the first wall at the bottom of the vertical section would be unacceptably high. All subsequent analyses are performed assuming some form of structural connection between the four walls. The structural connection has been idealized as two extreme cases by coupling the displacements in two fashions. First, the normal (transverse) displacements of two or more walls can be coupled without any in-plane (shear) displacement coupling, i.e., the walls remain parallel to each other but may undergo relative shear displacement. Second, both the normal and the in-plane displacements are coupled which represents a much more rigid connection between the walls. In reality, the thin radial webs which separate the individual coolant paths in the first wall channel, provide some shear rigidity which is somewhere between the two extreme cases. The analyses have also been carried out for two types of geometry. First, the inlet channel and the outlet channel are assumed to share a common wall which has a large temperature gradient through it. Second, the common wall between the two channels is replaced by two walls that are structurally and thermally separated by a gap which is assumed to be 2 cms in this analysis.

Table 4.3.4-1 shows a summary of all the displacements and stresses for the various geometries and constraints analyzed. For reference, the allowable thermal stress for V-15Cr-5Ti is about 700 MPa. The first three entries correspond to cases where the second and the third wall are combined to form a single solid wall, and the back wall is structurally separated from the front channel. Note that although the stresses are reasonably low, the maximum displacements are unacceptably large even for the case where the second wall thickness is increased to 2 cm. The displacements can be reduced significantly by structurally separating the second and the third wall, connecting the first wall to the second wall and connecting the third wall to the back wall rigidly, as shown by the next three entries. A typical displaced shape is shown in Fig. 4.3.4-5. The displacements can be further reduced by either increasing the second wall thickness or by increasing the shear rigidity of the first wall channel. However, in the latter case, the maximum stresses in the first wall channel are somewhat larger than in the other cases.

Table 4.3.4-1 SUMMARY OF MAXIMUM DISPLACEMENTS, TOROIDAL STRAINS, AND STRESSES IN THE VARIOUS WALLS OF THE REVERSED POLOIDAL FLOW V-15Cr-5Ti BLANKET AS A FUNCTION OF THE SECOND WALL THICKNESS. THE THICKNESSES OF THE FIRST, THIRD (FOR CASES WHERE IT IS STRUCTURALLY SEPARATE FROM THE SECOND WALL) AND THE FOURTH WALLS ARE ASSUMED TO BE 3 mm, 5 mm, AND 10 mm RESPECTIVELY

Second Wall Thickness (mm)	Constraint Between Walls						Maximum Displacement of First Wall (mm)	Toroidal Strain (%) ϵ_z		Maximum Stress Intensity (MPa)			
	First/Second Normal	First/Second Shear	Second/Third Normal	Second/Third Shear	Third/Fourth Normal	Third/Fourth Shear		Front Channel	Back Channel	First ^a Wall	Second Wall	Third Wall	Fourth Wall
10 ^b	yes	no	—	—	no	no	950	0.43	0.53	507	186	—	46
10 ^b	yes	yes	—	—	no	no	470	0.43	0.53	578	225	—	56
20 ^b	yes	yes	—	—	no	no	330	0.44	0.53	611	203	—	175
5	yes	no	no	no	yes	no	50	0.38	0.53	525	144	55	48
10	yes	no	no	no	yes	no	1	0.37	0.53	543	148	47	50
5	yes	yes	no	no	yes	yes	11	0.38	0.53	627	249	53	46
5	yes	no	yes	no	yes	no	0.4	0.48	0.48	408	255	96	96
10 ^b	yes	no	—	—	yes	no	44	0.49	0.49	407	298	—	119

^aMaximum stress at the top of the blanket where temperature is highest.

^bCombined second and third wall thickness.

Figure 4.3.4-5 Typical deformed (solid line) and underformed (dashed lines) shapes of a dual channel poloidal flow blanket due to temperature gradient. Scales for displacements are vastly exaggerated.

An interesting point to note from Table 4.3.4-1 is that the toroidal strain of the inlet channel is actually smaller than that of the outlet channel. This is contrary to intuition because the first wall has the highest temperature in the blanket. However, this is brought about by the constraint imposed on the first wall by the second wall, which is cooler than either the third or the fourth wall. Thus, the already high compressive stress in the first wall is actually made higher by its connection to the cool second wall. The remedy is, of course, to connect all four walls structurally, taking advantage of the warmer back walls to increase the average temperature of the resulting single structure. This reduces the temperature differential between the average temperature and the maximum first wall temperature which is the driving force for the thermal stress. Comparisons of the maximum stress in the first wall for the first entry with that of the last entry and for the fourth entry with that of the next to the last entry in Table 4.3.4-1 illustrate this effect for cases where the dividing wall between the inlet and the outlet channels is a single solid wall or two solid walls separated by a gap. However, for simplicity in both cases, coupling of shear displacement between the walls are neglected. Note that both the maximum stress and the maximum deflection of the first wall are reduced significantly by structurally connecting the two channels. Although the maximum stresses in the other walls are increased by this process, they are still much less than that in the first wall. It should be remembered, however, that structurally connecting the two channels may create high thermal stresses in the radial webs (normal to the toroidal direction) that are common to both the channels. These stresses have been shown to be acceptable for an inter-channel temperature difference of 270°C in V-15Cr-5Ti, see Section 3.2.3c.

4.3.4.3 Conclusions

1. Design pressure on a straight first wall can be significantly increased by making the first wall slightly curved.
2. An independent first wall, hung from the top and without a structural connection with the second wall, will have unacceptably large displacements.

3. If the inlet and the outlet channels share a common wall but are structurally separate, the first wall displacements are unacceptably large. The displacements can be reduced by increasing the stiffness of the common wall; but this drives the maximum stress in the first wall up. Structural separation of the two channels would help reduce the stresses in the first wall if the coolant flow in the blanket were reversed.
4. The best solution for reducing both the stresses and the deflection of the first wall is to structurally connect all the walls together and to provide the blanket with sufficient bending and shear rigidity.
5. The maximum thermal stresses in all cases considered are within the allowable limit for V-15Cr-5Ti.

4.3.5 Materials Considerations

Various materials considerations pertinent to the self-cooled liquid-lithium/vanadium-alloy (V-15Cr-5Ti) blanket have been addressed previously in the Blanket Comparison and Selection Study (BCSS)⁽¹⁾. Topics included are: thermophysical properties of liquid lithium and vanadium alloys; nuclear properties, welding/fabrication, mechanical properties and radiation effects of vanadium alloys; and liquid-lithium/vanadium-alloy corrosion and compatibility. Since little new data has become available on these topics in the interim, the BCSS assessment should remain current and need not be repeated here. The bulk of the materials considerations given in this section is, therefore, devoted to the evaluation of electrical insulators for liquid metal blankets.

Magnetohydrodynamic (MHD) pressure losses are an important consideration in the design of liquid metal blankets for magnetically confined fusion reactors. Both MHD experiment and theory indicate that the pressure losses would be significantly reduced if high electrical resistance structural walls were used in the design. Two possible methods of achieving this objective have been considered in this study. The first method uses a thin insulator coating on the surface of the conducting wall; the second method uses a laminated structure with a thin metallic layer over an insulator layer on the wall. Technical requirements for the insulator materials are:

1. Adequate electrical resistivity.
2. Stable in contact with liquid lithium and/or vanadium alloy (V-15Cr-5Ti).
3. Radiation stability.
4. Mechanical integrity.
5. Fabricability.

Additional considerations for insulator materials also include low activation and cost.

Only low-voltage (<1 volt) insulators are required in the liquid-metal blanket applications. Blanket operating temperatures (up to 750°C for vanadium alloy) favor the use of ceramic insulators, since organic insulators generally do not meet the temperature requirement. A review of the existing

data base on ceramic insulators is provided below, along with an assessment of candidate materials with respect to each of the above requirements. An overall evaluation then follows which identifies the most promising candidates for either coating or laminated structure configuration, as well as the data needs.

Electrical Resistivity Requirements

Candidate ceramic insulators can be identified by the electrical resistivity requirements for minimizing the MHD pressure losses. Conservative calculations have been performed to derive such requirements for the coating and laminated structure.⁽²⁾ In both configurations, the electrical resistivity requirement is not a singular number, but depends on several parameters such as the dimension of the conduit to be insulated, the insulator thickness, the Hartmann number, the thickness of metal clad, and the electrical resistivities of metal clad and liquid metal. For the conservatively chosen parameter values in Table 4.3.5-1, the electrical resistivity (ρ) requirements are:

$$\rho > 10^4 \Omega - \text{cm}$$

for the insulator coating, and

$$\rho > 10^2 \Omega - \text{cm}$$

for the laminated structure. The electrical resistivity requirement for insulator coating, as given above, is more stringent. This requirement has been derived for a coating which provides the maximum possible reduction in MHD pressure drop. A coating satisfying this resistivity requirement will result in a pressure drop which is more than two orders of magnitude smaller than that for the uninsulated wall case. Such a small pressure drop may not be needed. For a few fold decrease in pressure drop, which is more than adequate for most cases, insulating coatings with resistivity requirements identical to those for the laminated structure will suffice.

Figure 4.3.5-1 shows the electrical conductivities ($1/\rho$) of some of the best known ceramic insulators as a function of temperature. Inspection of the

TABLE 4.3.5-1
INSULATOR ELECTRICAL RESISTIVITY REQUIREMENTS AS DETERMINED FROM
CONSERVATIVE DESIGN CALCULATIONS^a

Laminated Structure	$\rho_{\text{insul}} > \rho_{\text{clad}} \frac{a^2}{t_{\text{clad}} t_{\text{insul}}}$
	a: half width of the conduit (10 cm)
	ρ_{clad} : metal clad resistivity ($10^{-4} \Omega - \text{cm}$)
	t_{clad} : metal clad thickness (200 μm)
	t_{insul} : insulator layer thickness (100 μm)
	$\rho_{\text{insul}} > 10^2 \Omega - \text{cm}$
Insulator Coating	$\rho_{\text{coat}} > \rho_{\text{LM}} \frac{Ma}{t_{\text{coat}}}$
	a: half width of the conduit (10 cm)
	ρ_{LM} : liquid metal resistivity ($3 \times 10^{-5} \Omega - \text{cm}$)
	M: Hartmann number (10^5)
	t_{coat} : insulator coating thickness (20 μm)
	$\rho_{\text{coat}} > 10^4 \Omega - \text{cm}$

^aParameter values are representative of the TPSS self-cooled liquid-lithium/
vanadium alloy blanket.

Figure 4.3.5-1. Electrical conductivities of candidate oxide and nitride insulators.

figure shows that even at the structural design temperature limit (750°C for vanadium alloy), all of these ceramic insulators (under unirradiated condition) possess electrical resistivities many orders of magnitude (10^2 - 10^{10}) greater than those required for minimizing the MHD pressure losses. The selection of candidate insulators will, therefore, be determined more by their ability to satisfy the other technical requirements. Figure 4.3.5-1 also shows that the eleven listed materials are either oxides or nitrides. In general, carbides, borides, and silicides have lower electrical resistivities than those of the listed materials, even though they may still qualify as candidates against the requirement for the laminated structure. For the purpose of this evaluation, however, only candidate oxides and nitrides are considered.

Table 4.3.5-2 lists the selected properties of the fifteen oxide and nitride candidate insulators evaluated in this study. Four materials, Sc_2O_3 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, SiON_2 , and $\text{Si}_4\text{Al}_2\text{O}_2\text{N}_6$ - 5% Y_2O_3 , have been added to those shown in Fig. 4.3.5-1. The last three have been considered in the U.S. Insulator Irradiation Program; scandium oxide (Sc_2O_3) should be similar to Y_2O_3 .

Stability in Contact with Liquid Lithium and/or Vanadium Alloy

Stability of candidate insulator materials in contact with liquid lithium and vanadium alloy has been assessed based on thermodynamic data. Compatibility with liquid lithium is particularly important for the coating configuration, as the insulator will be in direct contact with liquid metal. For the laminated structure, the insulator/liquid-lithium compatibility is not a concern, as long as the metal clad stays intact.

Figures 4.3.5-2 and 4.3.5-3 display the standard free energies of formations (ΔF°) as a function of temperature for candidate oxide and nitride insulators, respectively.⁽⁶⁾ Also plotted on these figures are the free energies of formations for the known oxides and nitrides of lithium (Li_2O , Li_2O_2 , LiAlO_2 , Li_3N) and vanadium alloy (VO , V_2O_3 , V_2O_4 , V_2O_5 , Cr_2O_3 , TiO , TiO_2 , Ti_2O_3 , VN , CrN , Cr_2N , TiN). From this information, the free energies of reactions (ΔG°) have been calculated for a large number of chemical reactions involving candidate insulators, liquid lithium, and vanadium alloy. Among the candidates, only Y_2O_3 , CaO , BeO , MgO , LiAlO_2 , and possibly $\text{Y}_3\text{Al}_5\text{O}_{12}$ are thermodynamically stable in liquid lithium saturated with oxygen. Even these oxides may become unstable at low oxygen concentrations in lithium.

TABLE 4.3.5-2
SELECTED PROPERTIES⁽³⁻⁵⁾ OF CANDIDATE CERAMIC INSULATORS

	BeO	MgO	CaO	SiO ₂	HfO ₂	Al ₂ O ₃	Y ₂ O ₃	Sc ₂ O ₃	MgAl ₂ O ₄	Y ₃ Al ₅ O ₁₂	AlN	BN	Si ₂ ON ₂	Si ₃ N ₄	Si ₄ Al ₂ O ₂ N ₆ 5% Y ₂ O ₃
Melting Point (°C)	2570	2800	2600	1730	2810	2050	2410	2405	2135	—	2230	3000	—	1900	—
Density (g/cc)	3.01	3.58	3.32	2.32	9.68	3.98	4.84	3.86	3.54	—	3.05	2.27	—	3.44	—
Crystal System ^a	H	C	C	M	M	H	C	C	C	C	H	H	—	O	—
Electrical Resistivity ^b (Ω - cm)	10 ¹⁰	10 ¹¹	10 ¹¹	10 ⁷	2.5×10 ⁶	2×10 ¹⁰	10 ⁹	—	2×10 ⁷	—	10 ⁷	5×10 ¹¹	—	1.25×10 ⁸	—
Thermal Conductivity ^b (W/m°C)	5×10 ²	11.5	7.0	5.0	1.6	9.1	4.0	—	8.1	2.7	22.0	35.0	—	6.0	—
Thermal Expansion Coefficient (×10 ⁻⁶ /°C)	8.0	13.0	12.2	23.0	5.6	7.8	7.8	8.0	8.0	—	5.0	12.5	—	2.4	—
Flexural Strength ^b (MPa)	70	96	—	105 (298°C)	138 (298°C)	235	—	—	35	—	—	—	—	—	—

^aH: hexagonal, C: cubic, M: monoclinic, O: orthorhombic.

^bValues are taken at 600°C unless indicated otherwise.

Figure 4.3.5-2. Standard free energies of formations of candidate oxide insulators and oxides of lithium and vanadium alloy (V-15Cr-5Ti).⁽⁶⁾

Figure 4.3.5-3. Standard free energies of formations of candidate nitride insulators and nitrides of lithium and vanadium alloy (V-15Cr-5Ti).⁽⁶⁾

Of the three nitride candidates (AlN, BN, Si₃N₄) evaluated, all were found stable in liquid lithium saturated with nitrogen, and their thermodynamic stability in descending order is AlN, BN, and Si₃N₄. Again, the stability of these nitrides will be reduced at low nitrogen concentrations in lithium.

With regard to the thermodynamic stability of candidate insulators in contact with vanadium alloy, all oxide candidates were found to be stable except SiO₂ which tends to react with titanium in the vanadium alloy to form titanium oxides (TiO, TiO₂, Ti₂O₃). Among the nitride candidates, Si₃N₄ is the only material that may have a compatibility problem with vanadium via the following reaction:

$$\Delta G^0 = - 26.7 \text{ kcal/mole at } 900 \text{ K} .$$

Boron nitride (BN) and aluminum nitride (AlN), while thermodynamically stable with respect to vanadium, may react with titanium in the vanadium alloy to form TiN. Since insulator/structure compatibility is an essential requirement for both coating and laminated structure configurations, Si₃N₄ probably should be ruled out from the list of potential candidates. The compatibility of V-15Cr-5Ti with BN and AlN should be experimentally investigated.

Radiation Stability

Most properties of ceramics are affected by irradiation, although not always deleteriously. As a general rule, structural changes result from neutron bombardment, while electrical properties are altered by absorption of ionizing energy. Radiation effects in ceramic insulators have been studied in the past at Los Alamos National Laboratory covering topics on swelling, mechanical strength, thermal conductivity, surface alteration, dielectric breakdown strength, electrical resistivity, electrolysis and loss tangent. Experimental data on these subjects were reported in a series of annual reports on special purpose materials⁽⁷⁻¹²⁾ and review articles.^(13,14) In the following, the data most relevant to the development of low-voltage ceramic insulators for liquid-metal blankets are summarized. Topics included are

swelling, mechanical strength, thermal conductivity, and electrical resistivity.

Table 4.3.5-3 compiles the fast-neutron ($E > 0.1$ MeV) irradiation-induced swelling in various ceramic insulators at the given irradiation temperatures and fluence levels. Candidate insulators for which swelling information has not been found are also included to indicate data needs. Based on the known swelling results, beryllium oxide (BeO) exhibited the highest volumetric swelling, while alumina (Al_2O_3) came in a close second. Due to their hexagonal crystal structure, both BeO and Al_2O_3 also displayed swelling anisotropy which manifests itself in grain boundary cracking in polycrystalline materials. When BeO or Al_2O_3 is used in the coating configuration, microcracking is likely to result in the loss of coating mechanical integrity; their electrical resistivities may also be degraded with liquid metal penetration into cracks. For the laminated structure configuration and as long as the metal clad stays intact, microcracking of BeO and Al_2O_3 is not a serious concern. However, attention needs to be paid in the design of metal clad to ensure that swelling can be accommodated.

The candidate insulators with a cubic crystal structure are always preferred for radiation stability, since problems associated with anisotropic swelling are then avoided. Table 4.3.5-2 shows that MgO , CaO , Y_2O_3 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, and MgAl_2O_4 all fall into the cubic structure category. The data in Table 4.3.5-3 (with the exception of CaO for which we have no data) also show low to moderate swelling in these materials. For the other non-cubic low swelling materials, grain boundary separation has not been observed in Si_3N_4 after irradiation,⁽¹⁴⁾ but this material has been set aside previously owing to its incompatibility with vanadium alloy. As for $\text{Si}_4\text{Al}_2\text{O}_2\text{N}_6 - 5\% \text{Y}_2\text{O}_3$, the available data⁽¹⁴⁾ suggests that its radiation stability may be limited by the presence of silicate phases; although adjustment of composition and impurity control may result in enhanced resistance to damage.

Irradiation data on thermal conductivity degradation and on changes in strength, fracture, toughness, and microhardness of candidate insulators can be found in Refs. 7 to 14. With the exception of single-crystal MgAl_2O_4 , post-irradiation room-temperature thermal diffusivities (hence thermal conductivities) of BeO , MgO , Al_2O_3 , Y_2O_3 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, Si_3N_4 , Si_2ON_2 , and $\text{Si}_4\text{Al}_2\text{O}_2\text{N}_6 - 5\% \text{Y}_2\text{O}_3$ all show significant reduction (50-90%) from their unirradiated

TABLE 4.3.5-3
SUMMARY OF IRRADIATION-INDUCED VOLUMETRIC SWELLING (% $\Delta V/V$)
IN CERAMIC INSULATORS⁽⁷⁻¹⁴⁾

Materials	Irradiation Temp. ($^{\circ}\text{C}$)	Fast Fluence ($\times 10^{26}$ n/m ² , E > 0.1 MeV)	% $\Delta V/V$
pc MgO ^a	160	2.1	2.6-3.0
MgO	800, 1100	0.5	~1.0
BeO	650, 825	1-2	3-10
sc Al ₂ O ₃ ^a	650	0.8-2.3	2.7-4.1
	740	0.3-2.3	1.7-4.4
pc Al ₂ O ₃ ^a	650	1.2-2.3	3.0-3.5
	740	0.3-2.3	1.9-6.5
Y ₂ O ₃	650, 825	1-2	0.5-1.5
Y ₂ O ₃ -1% ZrO ₂	650, 825	1-2	0.5-1.5
MgAl ₂ O ₄	650, 825	1-2	<0.5
Y ₃ Al ₅ O ₁₂	650, 825	1-2	<0.5
Si ₃ N ₄	650, 825	1-2	0.5-1.5
Si ₂ ON ₂	650, 825	1-2	<0.5
Si ₄ Al ₂ O ₂ N ₆	650, 825	1-2	<0.5
5% Y ₂ O ₃	-	-	-
CaO	-	-	-
HfO ₂	-	-	-
SiO ₂	-	-	-
Sc ₂ O ₃	-	-	-
AlN	-	-	-
BN	-	-	-

^apc: polycrystal; sc: single crystal.

values. However, the reduction in thermal conductivity per se is not a serious problem as long as the insulators are not applied on the high heat flux walls. Changes in mechanical strength and fracture toughness are of concern only if the insulators are used in coating configuration. In this connection, the data show strength increase for Al_2O_3 and MgAl_2O_4 , no change for $\text{Y}_3\text{Al}_5\text{O}_{12}$, and reduction for Si_2ON_2 .

Among the four topics addressed in this subsection on radiation stability, the change in electrical resistivity is difficult to assess because of the lack of data for most candidate materials. Unlike thermal conductivity which generally decreases owing to irradiation generated defects on phonon scattering, electrical resistivity may either increase or decrease depending on how structural changes affect trapping, recombination, or scattering centers of the charge carriers. Furthermore, absorption of ionization energy may be a greater concern as this would generate a concentration of charge carriers beyond that induced by thermal effects (radiation-induced conductivity). Electron bombardment study of single crystal Al_2O_3 ⁽¹⁵⁾ showed that over a fairly wide temperature range (room temperature to 1000 K), the increase in electrical conductivity is roughly proportional to the ionization dose rate for both chromium-doped and neutron-irradiated Al_2O_3 . If one assumes the same proportionality holds for the other candidate insulators and takes the estimated ionization dose rate ($10^3 - 10^4$ Gy/s) for the liquid-lithium/vanadium-alloy blanket, the increase in electrical conductivity (or decrease in electrical resistivity) would be in the range of 3 to 4 orders of magnitude. This level of reduction in electrical resistivity would still be acceptable for most candidate insulators used in the laminated structure configuration.

Mechanical Integrity

Mechanical integrity is a major concern only for coating insulators. Microcracking of insulators due to swelling anisotropy has already been discussed. This subsection examines the other source of insulator cracking which may result from thermal expansion mismatch between candidate insulators and vanadium alloy. Stresses in the coating are generated by the difference between the thermal expansion of the coating and that of the underlying base metal. If stress free at T_0 , the stresses depend on the new temperature T , on

the elastic properties of the material, and on the coefficients of expansion. For a thin coating on an infinite slab and assuming the elastic properties for coating and slab are the same, the approximate formulae for stresses are:⁽¹⁶⁾

$$\sigma_c = E(\alpha_b - \alpha_c) \Delta T (1 - 3j + 6j^2) \quad [4.3.5-1]$$

$$\sigma_b = E(\alpha_c - \alpha_b) \Delta T (j) (1 - 3j + 6j^2) \quad [4.3.5-2]$$

where σ is the stress, E is the Young's modulus, α is the thermal expansion coefficient, $\Delta T = T - T_0$, j is the ratio of coating to slab thickness, and subscripts c and b denote coating and base metal, respectively.

For the insulator coating considered in this study, j is much less than 1 and Eq. 4.3.5-1 reduces to

$$\sigma_c \approx E(\alpha_b - \alpha_c) \Delta T, \text{ or} \quad [4.3.5-3a]$$

$$\sigma_c \approx E\{(\Delta L/L_0)_b - (\Delta L/L_0)_c\} \quad [4.3.5-3b]$$

where $\Delta L/L_0$ is the linear thermal expansion. In Eq. 4.3.5-3 the usual sign convention for stress (positive means tension) is followed so that the coating is in tension when $(\Delta L/L_0)_b$ is greater than $(\Delta L/L_0)_c$.

Table 4.3.5-4 lists the thermal expansion data (% $\Delta L/L_0$) for the candidate insulators as well as V-15Cr-5Ti. The calculated stresses in these insulator coatings are given in Table 4.3.5-5, along with some of the known tensile flexural strengths. If one uses a tensile stress criterion for cracking, MgO, CaO, SiO₂, and BN are all unlikely to develop cracks because these coatings will be in compression. Beryllium oxide (BeO), HfO₂, and MgAl₂O₄, on the other hand, may develop cracks since their tensile stresses exceed the corresponding flexural strengths. Of the remaining materials, Al₂O₃ may stay intact, but this material has other problems of swelling and liquid lithium incompatibility. The two nitrides, AlN and Si₃N₄, have the highest calculated tensile stresses. Their use in the coating configuration is therefore doubtful.

TABLE 4.3.5-4
THERMAL EXPANSION (% $\Delta L/L_0$) OF CANDIDATE INSULATORS AND V-15Cr-5Ti
FROM 25°C TO INDICATED TEMPERATURE

Material	500°C ^a	600°C ^b	1000°C ^a
BeO	0.36	0.46	0.88
MgO	0.61	0.75	1.33
CaO	0.56	0.70	1.28
SiO ₂ (α -quartz)	0.92	1.32	1.22 (575°C)
HfO ₂	0.26	0.32	0.57
Al ₂ O ₃	0.36	0.45	0.83
Y ₂ O ₃	0.35	0.45	0.80
Sc ₂ O ₃	0.37	0.46	0.83
MgAl ₂ O ₄	0.37	0.46	0.82
AlN	0.23	0.29	0.54
BN	0.58	0.72	1.30
Si ₃ N ₄ (α)	0.10	0.14	0.28
(β)	0.07	0.10	0.22
V-15Cr-5Ti ^c	0.47	0.60	1.0

^aValues taken from ref. 4.

^bLinear interpolation from 500 and 1000°C values.

^cIntegrated from thermal expansion coefficient of V-15Cr-5Ti, $\alpha = 9.52 \times 10^{-6} + 1.6 \times 10^{-9} T$ (°C).

TABLE 4.3.5-5
CALCULATED THERMAL EXPANSION MISMATCH STRESSES IN CANDIDATE
INSULATOR COATINGS (at 600°C)

Material	σ_c (MPa) ^a	Tensile Flexural Strength (MPa) ^b
BeO	159	110
MgO	-170	90
CaO	-114	90 (guess)
SiO ₂	-820	107 (25°C)
HfO ₂	319	138 (25°C)
Al ₂ O ₃	171	235
Y ₂ O ₃	171	235 (guess)
Sc ₂ O ₃	159	235 (guess)
MgAl ₂ O ₄	159	34.5
AlN	353	-
BN	-137	-
Si ₃ N ₄ (α)	524	-
(β)	569	-

^aBased on Eq. [3b] with Young's modulus of V-15Cr-5Ti = 1.14×10^5 MPa.

^bValues taken from ref. 4.

Fabricability

Almost any materials which melt congruently into a fluid liquid and have reasonably low vapor pressure can be flame- or plasma-sprayed. A partial listing of reported coatings given in Ref. 4 shows that almost any material can be plasma sprayed, even some of the easily oxidizable or pyrophoric materials. The quality of the coatings obtained is frequently not described or carefully investigated, so the value of many of these reported coatings should be viewed with some reservation. Almost all sprayed coatings are somewhat porous because the application process is unable to eliminate all the fissures and joints between the particles. This porosity is an important characteristic of the coating because it provides lower modulus of elasticity and an extra flexibility to absorb differential thermal expansion stresses and thermal shock stresses. Based on the wide industrial experience in fabricating high temperature coatings, the fabrication of coating insulators on vanadium alloy does not appear to be a feasibility issue.

The fabrication of laminated structure for the liquid-lithium/vanadium alloy blanket is more complex than that for the coating and therefore, more costly. One can probably consider successive sprayings or spraying followed by swaging operation. Some development is required in this area, but fabrication does not appear to be a feasibility issue for the laminated structure either.

Low Activation and Cost

Table 4.3.5-6 gives relative rankings of the candidate insulator materials with respect to low activation and cost. Radioactivity consideration of Al (γ emitter) places all materials containing aluminum in the poor category, whereas that of N (β emitter) places the nitrogen containing materials in the moderate category. For some materials, such as Y_2O_3 , Sc_2O_3 , and HfO_2 , no ranking was given because they have not been investigated from low activation viewpoint. The low activation materials candidates include BeO, MgO, CaO, and SiO_2 .

Cost rankings given in Table 4.3.5-6 are again qualitative and relative. Resource abundance, ease of preparation, and stage of development place HfO_2 , Y_2O_3 , Sc_2O_3 , and $Y_3Al_5O_{12}$ in the high cost category; BeO, $MgAl_2O_4$, and $Si_4Al_2O_{2N_6} - 5\% Y_2O_3$ in the moderate cost category; the remaining candidates all fall into the low cost category.

TABLE 4.3.5-6
RELATIVE RANKINGS OF CANDIDATE INSULATORS ACCORDING TO
LOW ACTIVATION AND COST

Materials	Low Activation	Cost
BeO	good	moderate
MgO	good	low
CaO	good	low
SiO ₂	good	low
HfO ₂	-	high
Al ₂ O ₃	poor	low
Y ₂ O ₃	-	high
Sc ₂ O ₃	-	high
MgAl ₂ O ₄	poor	moderate
Y ₃ Al ₅ O ₁₂	poor	high
AlN	poor	low
BN	moderate	low
Si ₂ ON ₂	moderate	low
Si ₃ N ₄	moderate	low
Si ₄ Al ₂ O ₂ N ₆ - 5% Y ₂ O ₃	poor	moderate

While low activation and low cost are desirable attributes for the candidate insulators, it is debatable whether they should constitute overriding concerns in the insulator selection. The amount of insulator materials used in the liquid-lithium/vanadium-alloy blanket, regardless of coating or laminated structure configuration, will be a small fraction of the other major blanket materials.

Overall Evaluation

Assessment of the fifteen candidate insulators against the five technical requirements, low activation, and cost can be summarized as follows:

1. Si_3N_4 is ruled out because of its incompatibility with vanadium alloy (formation of VN).
2. SiO_2 , HfO_2 , Al_2O_3 , and MgAl_2O_4 should not be used in coating configuration because of their incompatibility with liquid lithium. SiO_2 also appears thermodynamically unstable in contact with vanadium alloy (formation of titanium oxides).
3. BeO and Al_2O_3 are not recommended as coating insulators because of their relatively large irradiation-induced swelling and microcracking. Their use in laminated structure configuration may also be limited by the need to accommodate swelling.
4. Thermal expansion mismatch with vanadium alloy raises doubts in using AlN and Si_3N_4 in coating configuration.
5. CaO , MgO , BN , and Y_2O_3 appear to be the most promising candidates for insulator coating. Stability in high purity lithium is a major question. BN may also react with titanium in the vanadium alloy to form TiN . Their mechanical integrities appear good from the viewpoint of thermal expansion mismatch with vanadium alloy. CaO , MgO , and Y_2O_3 all have a cubic crystal structure, exhibit low swelling (CaO is probably similar to MgO) and high electrical resistivity so that even a resistivity reduction of 3 to 4 orders of magnitude during irradiation would not represent problems for these materials

Boron nitride (BN) has the highest electrical resistivity (unirradiated) among the candidate insulators; however, it has a hexagonal crystal structure and its swelling behavior is currently unknown.

6. Since the insulator stability in liquid lithium and mechanical integrity are of no concern for the laminated structure configuration, and since the electrical resistivity requirement is considerably less stringent than that for the insulator coating, the selection of materials is much more flexible in the case of laminated structure. In fact, all of those candidates that qualify as insulator coating would also qualify as laminated structure. To this list one should also add MgAl_2O_4 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, and AlN , the former two due to their known radiation stability, the last due to its low cost.

In conclusion, the incorporation of insulator material into the TPSS innovative design of liquid-lithium/vanadium-alloy blanket appear highly attractive and technologically feasible. The systematic evaluation of the fifteen candidate insulators turned out some interesting new candidates, e.g., CaO , BN , and possibly AlN , that have not been considered in the U.S. Fusion Development Program on special purpose materials. These new materials are recommended for inclusion in future investigations.

4.3.6 Integrated Shield Concept and Neutronics

The integrated first wall/blanket/shield concept is motivated by several design considerations. Capital cost, design simplicity, improved safety, and high performance are the key factors in developing this concept. Main features and nucleonic analyses of this concept will be discussed briefly in this section.

4.3.6.1 Main Features

As mentioned before, this concept integrates the first wall, blanket, and shield in one system with one coolant. The integration process resolves many design problems and improves the reactor performance. The use of one coolant eliminates the safety issue related to the compatibility of the shield coolant, especially water, with the liquid metal blankets. Also, the shield integration with the blanket increases the thermal inertia of the system to accommodate the decay heat after shutdown which enhances the reactor safety. In addition, the outer shield surface provides a direct path to remove the decay heat by conduction. The elimination of the shield coolant system simplifies the manifold system and reduces the maintenance time. In addition, the energy deposited in the shield is recovered by the main coolant which increases the energy multiplication per fusion neutron and reduces the environmental impact of the reactor.

In this concept, the shield is designed to protect the toroidal field (TF) coils. In fact, the total thickness of the first wall/blanket/shield is kept the same around the plasma which minimizes the total mass of the reactor. This configuration reduces the size of the TF coils which is desirable from the capital cost, mass utilization, and maintenance points of view. An igloo shield is used outside the TF coils to reduce the biological dose during operation and after shutdown to the permissible levels. The igloo shield utilizes low cost and low activation materials. This type of shield does not require an active coolant or design integration with the reactor main structure. These features reduce the time required for maintenance, capital cost, and the reactor building size.

4.3.6.2 Nucleonic Analyses

Nucleonic analyses were performed to define the performance of a typical

first wall, blanket, and shield integrated concept. A three-dimensional geometrical model describing the tokamak reactor of this study was developed for the analysis. The reactor has a major radius (R_0) of 5.25 m, plasma radius (a_0) of 0.875 m, and plasma elongation (κ) of 2. The scrapeoff layer thickness is 0.08 m at the midplane. The plasma boundary is fitted to a D-shape defined as follows:

$$R = R_0 + a_0 \cos(t + 0.25 \sin t), \quad [4.3.6-1]$$

$$Z = a_0 \kappa \sin t,$$

where (R, Z) describes a point on the plasma boundary and t is a parameter which varies from 0 to 2π . A uniform spatial source distribution was used to show the effect of the geometrical parameters without interference from the spatial distribution of the DT source. However, the neutron energy spectrum $n(E)$ is considered as follows:

$$n(E) = C \exp \left(\frac{\sqrt{E} - \sqrt{B}}{A} \right)^2, \quad [4.3.6-2]$$

where $C = 1.68$, $B = 14.07$ MeV, and $A = 0.56$ MeV.

The integrated concept in this model has liquid lithium for tritium breeding and heat removal, a vanadium structure, and a steel reflector. Table 4.3.6-1 gives the material compositions and zone thicknesses used for the analysis based on previous studies.⁽¹⁾ The shield zone has the same composition as the reflector which does not represent the optimum choice from the shielding point of view. In fact a study is needed to define the shield composition and arrangement for this concept.

The neutron and neutron-induced photon transport calculations were performed with MCNP code,⁽²¹⁾ which is a general Monte Carlo code for neutron and photon transport. Continuous energy representation for nuclear cross sections (pointwise data) was used for the transport calculations. All the nuclear cross sections were generated from ENDF/B-V. Two variance reduction schemes were employed to improve the accuracy and efficiency of the calculations. These are zone splitting and weight cutoff with Russian roulette. Nuclear fluxes and nuclear heating rates without decay energy were calculated in all

TABLE 4.3.6-1. FIRST WALL, BLANKET, AND SHIELD PARAMETERS

Zone Description	Zone Thickness (cm)	Zone Composition (Vol.%)
First wall	1	50% V15Cr5Ti, 50% Li ^a
Breeding	23	7.5% V15Cr5Ti, 92.5% Li ^a
Reflector	36	10% V15Cr5Ti, 10% Li ^a , 80% type Fe1422 steel
Shield	46	10% V15Cr5Ti, 10% Li ^a 80% type Fe1422 steel
Gap	2	100% vacuum
Dewar	5	100% Nitronic-33 steel
Gap	2	10% insulator, 90% vacuum
Coil case	5	100% Nitronic-33 steel
Superconductor	5	35% Nitronic-33 steel, 15% insulator, 31.5% copper, 3.6% NbTi, 15% liquid He

^a Natural lithium.

zones as well as the first wall spatial distributions in the poloidal direction. The inclusion of the decay energy is expected to increase the nuclear heating values. Figure 4.3.7-1 shows the geometrical model used in the calculations where a reflective boundary condition was assumed at the mid-plane. The toroidal field (TF) coils were assumed to cover the whole outer shield surface in this model. This assumption causes an increase in the total nuclear heating of the TF coils. This increase is approximately proportional to the increase in the surface area between the TF coils and the shield.

The tritium breeding production rates per zone are given in Table 4.3.6-2. The inboard section of the reactor produces ~ 28% of the total tritium production. The energy deposition per fusion neutron is given in Table 4.3.6-3 for each zone. The inboard section of the reactor produces ~ 27% of the total energy deposited which represents an important parameter for thermal hydraulic design. The energy multiplication factor is 1.28 which is quite satisfactory for liquid metal design without neutron multiplier. In this calculation, the total thickness of the first wall, blanket, and shield is 1.06 m which is small relative to the other liquid metal designs.⁽¹⁾ As a result, the nuclear responses in the TF coils are relatively high as shown in Table 4.3.6-4. Also the shield configuration is far-off from the optimum conditions. The poloidal distributions of the neutron fluence and the nuclear heating in the first wall are plotted in Figs. 4.3.6-2 and 4.3.6-3, respectively. The valleys in Figs. 4.3.6-2 and 4.3.6-3 are caused by the corners in the first wall model located 62, 88.5, and 142 degrees, as shown in Fig. 4.3.6-1. The difference between the maximum and the minimum values in these distributions is around 10% ignoring the valleys due to the corners in the first wall model. The source spatial distribution is expected to increase this difference to ~ 20%.

Figure 4.3.6-1 Geometrical model for the calculations.

TABLE 4.3.6-2. TRITIUM BREEDING RATIO

Zone Description	Tritium Breeding Ratio						
	Inboard Section		Outboard Section		Total		
	T7	T7	T6	T7	T6	T7	T6 + T7
First wall	0.0040	0.0039	0.0093	0.0105	0.0133	0.0144	0.0277
Breeding	0.1781	0.0916	0.4249	0.2710	0.6030	0.3626	0.9656
Reflector	0.0347	0.0015	0.0903	0.0047	0.1250	0.0062	0.1311
Shield	0.0099	0.0000	0.0299	0.0000	0.0398	0.0000	0.0398
Total	0.2267	0.0970	0.5544	0.2862	0.7811	0.3832	1.1642

TABLE 4.3.6-3. ENERGY DEPOSITION PER FUSION NEUTRON

Zone Description	Energy Per Fusion Neutron, MeV		
	Inboard Section	Outboard Section	Total
First wall	0.186	0.476	0.662
Breeding	2.583	6.910	9.493
Reflector	1.842	5.144	6.986
Shield	0.233	0.657	0.890
Total	4.844	13.187	18.031

TABLE 4.3.6-4. MAIN PERFORMANCE PARAMETERS NORMALIZED TO 1 MW/m^2 NEUTRON WALL LOAD OR $1 \text{ MW}\cdot\text{y/m}^2$ NEUTRON EXPOSURE

Tritium breeding ratio	1.16
Energy deposition per fusion neutron, MeV	18.11
Energy multiplication factor	1.28
Total inboard thickness (first wall, blanket, shield), m	1.06
Maximum nuclear heating in the winding pack, mW/cm^3	18.2
Maximum dose in thermal insulators, rads	2.6×10^{10}
Maximum fluence in the superconductor, n/cm^2	5.3×10^{18}

Figure 4.3.6-2 Spatial distributions of neutron fluence in the first wall normalized to $1 \text{ MW}\cdot\text{y}/\text{m}^2$.

Figure 4.3.6-3 Spatial distributions of nuclear heating in the first wall normalized to $1 \text{ MW}/\text{m}^2$ neutron wall loading.

4.3.7 Tritium Containment Recovery/Fueling

One of the unique features of the self-pumped limiter, proposed as the base line impurity control system, is that its use eliminates one of the tritium processing systems which have been found in previous commercial reactor designs, i.e., the plasma exhaust system. In this new concept, all of the tritium which is not burnt in the plasma enters the vanadium walls through implantation. The tritium in the vanadium first wall/limiter then diffuses to the self-cooled lithium blanket. Thus, the lithium now serves three functions, breeder, coolant for the breeder, and tritium fuel processing system. The reactor is now not dependent upon the vacuum pumps for tritium processing reliability but rather on the blanket tritium processing system.

The amount of material (tritium and deuterium) which diffuses into the lithium from the vanadium is ~1 kilogram of tritium and ~ 0.67 kilograms of deuterium each day. This compares with less than 0.3 kg of tritium being bred each day in the lithium for a 1200 MW_f reactor with a 1.5 breeding ratio. For a 800 MW_f reactor with a 1.05 breeding ratio, the amount of bred tritium is less than 0.13 kg. Thus, the conditions which we must place on the tritium and deuterium recovery system are not driven primarily by the need to replace the tritium burnt in the plasma but, rather, by the need to replace the total input to the plasma needed to keep the reactor operational. This new criterion simplifies the total fuel recovery system which includes both the plasma and the blanket. The total capital cost is also reduced. However, this new design puts new requirements on the blanket processing system. It must be highly reliable. It should also have a low total residence time and a high efficiency.

A tritium recovery system which satisfies the latter requirements is the molten salt/electrolysis system.⁽¹⁷⁻¹⁸⁾ The total anticipated residence time of both the tritium and the deuterium moving through coolant lines, a surge tank, a molten salt contactor and a molten salt electrolysis unit to release tritium and deuterium as gaseous T₂/D₂ would be less than 20 minutes.⁽¹⁾ The tritium inventory in the molten salt electrolysis part of the system is less than 10 g. The entire blanket processing system can be operated continuously at temperatures higher than 500°C which improves the overall efficiency of the fusion plant since all heat is removed once at the heat exchanger.

A diagram of the proposed fuel processing system at the commercial reactor plant is shown in Fig. 4.3.7-1. To minimize the amount of tritium passing through the heat exchanger a low tritium partial pressure ($\sim 2 \times 10^{-7}$ Pa) is maintained in the lithium. If one examines Fig. 4.3.7-2 one notes that the tritium concentration maintained in the lithium at this partial pressure is ~ 2 appm or 1 kg of tritium/ 10^6 kg of lithium. The anticipated mass of lithium to be used is 1.25×10^5 kg (250 m^3). Thus, under these conditions, the lithium contains ~ 125 g of tritium.

To attain a 2 appm reduction during processing, the fraction of lithium flow processed is 0.1% (see Fig. 4.3.7-3). At a flow of $130 \text{ m}^3/\text{s}$, this is $\sim 0.13 \text{ m}^3/\text{s}$ which is a reasonable flow rate. If a lower partial pressure is desired ($\sim 10^{-9}$ Pa), then the desired tritium concentration in the lithium would be less than 1 appm. A process flow rate of 1% would be required ($13 \text{ m}^3/\text{s}$). A larger processing unit would also be needed. The optimum size of the blanket tritium processing system will be determined in the next phase of this study.

One area which needs to be mentioned is the amount of total hydrogen species dissolved in the vanadium walls in the blanket. At a tritium partial pressure of $\sim 2 \times 10^{-7}$ Pa, and a total blanket wall volume of $\sim 200 \text{ m}^3$, the tritium concentration is 10^{-4} kg/m^3 for an inventory of ~ 20 g. At these levels, hydrogen embrittlement is not expected to be a problem.

The tritium recovery system proposed is a simplified fuel processing system. The system is continuously on-line, has a low tritium inventory, and has a low environmental release potential. This system attempts to enhance all the advantages of both the self-pumped limiter design and liquid lithium itself. Some of the areas yet to be addressed are the electrical efficiency of the total system, the amount of salt dissolved in lithium, and corrosion problems anticipated at high operating temperatures.

Figure 4.3.7-1 Fueling system for a self-cooled lithium blanket with a self-pumped first wall limiter.

Figure 4.3.7-2 Effect of tritium concentration in lithium on the tritium partial pressure over lithium.⁽¹⁹⁾

Figure 4.3.7-3 Effect of tritium concentration reduction during processing on the fraction of lithium which must be processed.

4.3.8 Special Issues

There are other possibilities for enhancing the design of a self-cooled liquid metal blanket and, by extension, the attractiveness of a tokamak reactor. These possibilities were not pursued during the first phase of the TPSS, either because the need for further development to establish feasibility is high, or because the methodology for obtaining quantitative results, suitable for design calculations, is not available. Most prominent among these possibilities for further design improvements are: a) MHD flow tailoring and b) MHD pressure reduction through interfacial resistive layers.

Magnetohydrodynamic flow tailoring⁽²⁰⁾ makes it possible, in principle, to achieve highly non-uniform velocity profiles by appropriate variation of the duct cross-sectional shape in the flow direction. Such profiles can be used for adequate cooling the first wall while maintaining low average coolant velocities and thus relatively low MHD pressure drop. Although the shaping of ducts can be considered as an added complexity, MHD flow tailoring may obviate the need for insulators and achieve high neutron wall loading capability with bare metal walls. The principle on which MHD flow tailoring is well established. Nonetheless, calculational tools, that will allow prediction of velocity profiles and pressure drops beyond the qualitative level, are not yet available. Ongoing research in this area may make such tools available in the coming year.

The designs considered in this study, integrate the first wall, blanket, and shield in one system to improve the reactor performance, as mentioned in Section 4.3.6. This concept requires that the shield accommodate the high temperature of the main coolant. The increase in the shield temperature represents a concern for the design of the toroidal field (TF) coils. Thermal insulator materials or other design solutions will be required to reduce the heat losses to the TF coils. Also, special attention is needed to define the flow path in the shield section to avoid any increase in the pressure losses. However, the low heating rates in this section simplify the design. A manifold arrangement different from the conventional one is used with this concept to further improve overall system performance. As shown in Fig. 4.3.8-1, the center section of the reactor is used for housing the return coolant lines. This arrangement simplifies maintenance procedures for blanket replacement, provides better blanket access, and permits a smaller reactor

building. Also, the total length of the coolant lines may be reduced, thus enhancing mass utilization and reactor safety.

Figure 4.3.8-1 Arrangement of reactor inlet and outlet ducts. There are sixteen inlet ducts through the gaps of the sixteen toroidal field coils. The sixteen similar outlet ducts are combined into two main outlet ducts routed through the center of the torus.

4.3.9 Evaluation

Although the basic configurations developed in the TPSS are only two, DESIGN A and DESIGN B, the use of insulating liners or coatings, and the first wall as limiter or separate limiter options combine to produce the following blanket options.

- 1) Poloidal flow with flow reversal, DESIGN A. No insulators in first wall channels. Separate limiter.
- 2) Same as 1) with the addition of insulated liners in first wall channels.
- 3) Same as 1) with the addition of insulating surface coatings in first wall channels.
- 4) Same as in 2) with the first wall acting as a limiter.
- 5) Same as in 3) with the first wall acting as a limiter.
- 6) Poloidal flow, DESIGN B. No insulators in first wall channels. Separate limiter.
- 7) Same as in 6) with the addition of insulated liners in first wall channels.
- 8) Same as in 6) with the addition of insulating coatings in first wall channels.
- 9) Same as in 7) with the first wall acting as limiter.
- 10) Same as in 8) with the first wall acting as limiter.

The ten different blanket design options can be compared with each other on the basis of blanket design simplicity, high neutron wall loading capability, overall reactor simplicity, impact on blanket thickness, need for further development, minimization of pressure drop, and lifetime of replaceable first wall and adjacent blanket portion. Such a comparison is shown in Table 4.3.9-1. The intent of Table 4.3.9-1 is to summarize in a qualitative way the advantages and disadvantages of the various design options. The figures of merit should not be used in a quantitative way, and they should not be added to give an overall score of a particular design, particularly so, because no weighing of the various design attributes was attempted here.

The designs with the first wall as a limiter have obviously the highest scores for reactor simplicity since that concept eliminates a major reactor component (divertor or limiter).

TABLE 4.3.9-1. COMPARISON OF TPSS BLANKET DESIGN OPTIONS

DESIGN ATTRIBUTES	DESIGN A				DESIGN B			
	SEPARATE LIMITER	F.W. AS LIMITER	SEPARATE LIMITER	F.W. AS LIMITER	SEPARATE LIMITER	F.W. AS LIMITER	SEPARATE LIMITER	F.W. AS LIMITER
No. Insul.								
Insulated Liners								
Insulating Coatings								
Insulated Liners								
Insulating Coatings								
No Insul.								
Insulated Liners								
Insulating Coatings								
Insulated Liners								
Insulating Coatings								
Reactor Simplicity	3	3	3	1	1	3	3	1
Blanket Simplicity	2	4	3	4	3	1	3	2
High Neutron Wall Capability	2	1	1	3	3	3	2	4
MHD Pressure Drop	3	2	1	2	1	4	2	1
Need for Future Development	1	2	3	3	4	1	2	3
Blanket/Shield Thickness	2	2	2	2	2	1	1	1
Lifetime of First Wall	1	2	2	3	3	1	2	3

A low score indicates higher ranking.

The DESIGN B with separate limiter and no insulators is the simplest blanket design, with the blanket consisting essentially of a tube bank. That simplicity comes at a price. This option has high MHD pressure drop and low neutron wall capability. The options with insulating coatings or insulated liners are rated lower in blanket simplicity because of the added fabrication complexity. One should keep in mind, however, that use of insulators will decrease the MHD pressure drop and will thus allow larger spans, bringing about a substantial reduction in total number of ducts and weldings.

Design options with the first wall as a limiter have a reduced neutron wall capability. The main limitation arises from the large temperature drop within the deposited layer. Relief can come from higher allowable temperature or lower thickness of the deposited layer. The former possibility does not appear to be very promising because the peak material temperature of the deposited layer is set by impurity adsorption considerations. The latter solution leads to small first wall lifetimes.

The options with the insulated liners or insulating coatings have the highest neutron wall capability because they allow high first wall coolant velocities without large MHD pressure drop. In the options without insulators, increased neutron wall loading is accompanied by larger MHD pressure drop, particularly for DESIGN B. Although all the stresses are within allowable limits, the high pressures required for the larger neutron wall loadings for uninsulated DESIGN B may make this option unattractive for high neutron wall loading.

Uninsulated options with separate limiters are rated high in need for future development because, as a result of their simplicity, the MHD effects are well understood. Options with insulated liners need development in the areas of fabricability of the laminated construction and irradiation effects on ceramic insulators. The need for future development of insulating coatings is higher because compatibility of insulator and liquid metal coolant is an additional issue. Also the integrity criteria of such coatings are more stringent than those for insulators in laminated structures. Additional development will also be needed for the options with the first wall as a limiter to establish the feasibility and operational characteristics of self-pumped limiters.

The main drawback of DESIGN A as compared to DESIGN B is the overall blanket/shield thickness. In DESIGN A, provisions must be made to allow for sufficient flow area for the return flow, preferably two to three times larger than the flow area available for the portion of the blanket adjacent to the first wall. Although this is easily accomplished for the lower neutron wall loading with an overall blanket/shield thickness of about one meter, at higher neutron wall loadings the blanket/shield thickness would have to be increased.

As explained earlier, all design options take advantage of the neutron fluence gradient through the blanket and shield by allowing partial replacement of the blanket. DESIGN B is more flexible than DESIGN A in this regard, because, in the latter, for practical reasons, the return leg of the blanket would most probably have to be replaced along with the first wall channels. Options using insulators are given lower ratings than bare wall options because irradiation damage to the insulators may necessitate more frequent replacement. Finally, options using the first wall as a limiter are given the lowest rating, because the first wall lifetime is limited by the allowable thickness of the deposited material. For high neutron wall loadings this can be a severe limitation.

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4.4 Helium-Cooled Solid Breeder

4.4.1 Concept and Issues

Of the solid breeder designs considered in the Blanket Comparison and Selection Study (BCSS),⁽¹⁾ the Li_2O breeder with the He coolant received the highest overall ranking for both tokamak and tandem mirror systems in terms of engineering, economics, safety, and R&D. Although this overall relative rating was high, a review of the sub-categories revealed several areas in which major improvements could be made. These include tritium breeding, blanket thickness, blanket energy multiplication, power-conversion (gross and net) efficiency, breeder temperature window, geometrical integrity of the purge and coolant flow paths, tritium leakage, and lifetime of the major structural components (e.g., first wall).

In the TPSS effort, the main thrust of the design innovations has been to lower the cost of electricity by making significant improvements to the thermal power generation and the power-conversion efficiency. By substituting a vanadium alloy (e.g., V-15Cr-5Ti) for the BCSS HT-9 structural material, coolant outlet temperatures can be increased to optimize the gross power-conversion efficiency of the system. By mixing Be with the solid breeder, the higher energy multiplication results in a higher power generation for a given neutron wall loading and reactor size. These are certainly not new ideas. The novelty of the design emerges from solving numerous technical difficulties that arise from the use of vanadium and beryllium, as well as lithium oxide. These are summarized in the following paragraphs.

Problems associated with the use of vanadium alloys have been well documented in the BCSS interim⁽²⁾ and final⁽¹⁾ reports. The ones addressed in this work are corrosion, oxygen and hydrogen embrittlement, and high permeability to hydrogen isotopes. With a vanadium alloy as a cladding material for breeder elements it is important to control the exposure of the structure to oxygen and/or moisture. As Be has a higher affinity for oxygen than either V or Li, it creates a reducing environment within the breeder elements which minimizes oxidation at the breeder-structure interface. On the coolant side of the cladding and the first wall, the compatibility of He and vanadium is an issue. Moisture and oxygen impurities in the coolant, along with residual moisture (e.g., T_2O , HTO , H_2O) from the tritium processing, must

be considered. Two approaches have been adopted to mitigate this problem. A tritium recovery system has been designed which does not require the oxidation of tritium in order to separate it from the helium coolant. Liquid sodium is used as a getter for tritium to separate it from the helium coolant. Secondly, for the purposes of tritium recovery from the breeder, the helium is doped with a hydrogen isotope which tends to create a reducing atmosphere on the coolant side of the structural material.

The high permeability of vanadium alloys to hydrogen isotopes has both positive and negative consequences for the design of solid breeder blankets. The primary design benefit is that by using a vanadium alloy for breeder cladding, the need for a separate helium loop to purge the tritium from inside the breeder elements is eliminated. Tritium is allowed to permeate the cladding from the breeder to the primary helium coolant which then acts as the medium for both tritium recovery and heat transport. This feature reduces the complexity of having a separate helium loop within the blanket for tritium recovery and lessens the concern about maintaining the integrity of purge flow paths at the breeder-clad interface. However, the negative consequences of the high permeability require some major design changes. With a vanadium alloy as a first wall material, the designer needs to insure that the leakage of hydrogen isotopes to the plasma is not excessive. Two design innovations were used to solve the problems associated with leakage to the plasma. First, the coolant side of the first wall and the module side walls are coated with a thin layer ($\sim 1 \mu\text{m}$) of tungsten which acts as a diffusion barrier for hydrogen isotopes. Calculations are then performed to ensure that this design change does not induce excessive hydrogen embrittlement. Second, the helium coolant is doped with deuterium rather than protium for the purposes of tritium recovery from the solid breeder. Deuterium is chosen over protium because it is a fuel for the D-T reaction. Thus, proper design of the first wall can reduce the D and T leakage to the plasma to be less than the rate of consumption of D and T in the plasma.

Tritium leakage to the steam generators was a potential problem area in all of the BCSS solid breeder designs which used ferritic steel (HT-9) as a structural material. Uncertainties surrounding the effectiveness of oxide barriers led to a high degree of uncertainty in the analysis. The substitution of a vanadium alloy for HT-9 exaggerates this problem in two ways.

First, the orders of magnitude (~ 2) higher permeability of the base metal would make leakage of tritium from the primary helium coolant to the secondary (i.e., steam generator) coolant excessive. Secondly, embrittlement and corrosion concerns necessitate the minimization of the formation of oxide layers on the surfaces of vanadium alloys. Thus, vanadium alloys are not practical for the piping used to convect helium to the steam generators or for the steam generator tubes. A possible solution to this problem which is suggested in this report is to use a high-nickel alloy (possibly double-walled) for the primary coolant loop outside of the reactor vacuum chamber. This allows control of tritium leakage to the steam generators without impacting the performance of the blanket itself. It also results in a reduction in materials cost for the primary loop.

Beryllium was not used in the Li_2O -He BCSS design. A 3-D tritium breeding ratio of 1.11 was achieved without using Be as a neutron multiplier. Concerns over limited Be resources and high local heating rates both contributed to the decision to use Be as a multiplier only when it was necessary for breeding (e.g., LiAlO_2 design). However, the negative consequences of not using Be in the BCSS design are that the tritium breeding ratio is marginal, the energy multiplication is low, and there is very little margin for uncertainty or flexibility allowed in the Li_2O design without Be. In the TPSS design, Be is mixed with Li_2O within the breeder elements. This results in potentially higher tritium breeding and energy multiplication regardless of how much structure is required. The advantages of mixing the Be with the Li_2O rather than using a separate layer of Be are: it provides a more effective reducing environment which inhibits cladding oxidation and T_2O (DTO) formation; it results in a smaller peak heat generation per unit volume; and it enhances the effective thermal conductivity of the breeder. Indeed, it is a major factor in improving the performance of the Li_2O -He blanket as compared to the BCSS design. Aside from the question of the adequacy of Be resources, it remains to be demonstrated that the Be- Li_2O mixture can be fabricated (sintered or hot pressed) to the desired microstructure ($\sim 80\%$ dense with 1-10 μm grain diameter), that other compounds such as Li_2BeO are not formed to any significant degree, that there are no serious consequences of free Li formation, and that the kinetics for oxygen control are optimum.

The relative benefits and disadvantages of Li_2O as a breeder material are well documented in the BCSS report. As compared to LiAlO_2 , it has much higher lithium atom density, thermal conductivity, and bulk diffusion coefficient for tritium. The potential disadvantages are associated with its greater tendency to form solid-precipitate LiOT at low temperatures (resulting in unacceptable tritium inventory) and volatile LiOT at high temperature (resulting in mass transfer and corrosion concerns), to experience helium-induced swelling, and to undergo both grain growth and hot pressing at blanket operating temperatures. As has already been mentioned, the mixing of Be with Li_2O should minimize the LiOT formation. Another design change made to accommodate the special problems of Li_2O is to enclose the Li_2O in sealed tubes rather than the BCSS "purged plates". The tube geometry is much stiffer than the plate geometry and therefore more effective in resisting the Li_2O swelling. By sealing the tube, the potential problem of mass transfer is effectively eliminated. The use of both Be as a neutron multiplier and a vanadium alloy as the cladding material are essential to the sealed tube design. The vanadium is important for allowing the tritium to permeate to the primary coolant at a low pressure drop and the Be is important for both neutronics reasons (tubes give lower breeder volume fractions than plates) and tritium inventory reasons.

Table 4.4-1 summarizes the main design features of the TPSS Li_2O -He blanket. The 7 vppm of D_2 added to the helium coolant corresponds to 35 Pa for the He pressure of 5 MPa. After reaching steady-state, the Li_2O breeder will also be exposed to this D_2 partial pressure. This amount is consistent with the amount of hydrogen added to the helium purge in the BCSS design to enhance tritium desorption from pore-solid interfaces and to thereby minimize tritium inventory. However, as it is at least an order of magnitude less than that used in experimental studies (e.g., TRIO⁽³⁾ and VOM-15H⁽⁴⁾) which exhibited very low tritium inventories, it remains to be demonstrated that the tritium inventory is acceptable in an atmosphere containing only 35 Pa of a hydrogen isotope. As has already been emphasized, D_2 was chosen as a dopant over H_2 because of the inevitable leakage through the first wall to the plasma.

The mixture of Li_2O breeder and Be multiplier offers many benefits to blanket performance. The benefit of design flexibility is difficult to quantify, but very important. If the structure fraction needs to be increased for

TABLE 4.4-1.
SUMMARY OF Li_2O -He BLANKET DESIGN FEATURES AND BENEFITS

Component	Material	Design Feature	Benefits
Coolant/Purge	He (5 MPa) + 7 vppm D_2	Single loop for heat and tritium transport.	Design simplicity; allows high outlet temp.; low activation; chemically inert.
Breeder/Multiplier	Li_2O + 30 vol % Be	80% dense, hot pressed pellets	High energy multiplication (>1.3); high breeding potential (>1.2); design flexibility; high thermal conductivity ($>2x$); low breeder thickness (<0.4 m); low oxygen potential; low T inventory.
Breeder Cladding	V-alloy	Sealed, cylindrical tubes	Allow high coolant temperature ($>650^\circ\text{C}$); high restraint of Li_2O swelling; allow single coolant- purge system; eliminate Li_2O mass transfer; low activation.
First Wall	V-alloy	Lobe shaped, grooved, tungsten-coated (1 μm) on coolant side	Allows higher coolant temperature; minimize $\text{D}_2(\text{T}_2)$ leakage to plasma; low activation; long lifetime.

mechanical strength reasons and/or if the coolant fraction needs to be increased for thermal-hydraulic reasons, the designer can simply increase the Be volume fraction to maintain a reference breeding ratio (1.2 based on 1-D analysis) and a desirable energy multiplication factor.

The V-alloy, sealed-tube design also has a high payoff. Because of the high structure-temperature limit for this material, the coolant temperature can be allowed to increase up to a level which is attractive from power-conversion efficiency considerations. The sealed tube eliminates concerns over integrity of internal purge flow paths and Li_2O mass transfer. These were mentioned as problem areas in the BCSS report, and they may be even more serious than was alluded to.

With a V-alloy as a first wall material, both longer life and higher coolant temperatures can be realized than for the BCSS HT-9 design. The bulk nuclear heating in the V-alloy is 60% of what it would be in HT-9. Also, designs with reduced wall loading (e.g., 2.5 MW/m^2) can result in simpler first wall configurations and tend to help compensate for the thermal-hydraulic aspects of He as a coolant. The lobe shape is required to maintain the structural integrity of the first wall which is subjected to He at 5 MPa. However, it does present a problem with regard to plasma shaping, and it is probably inconsistent with the concept of using self-pumped limiter designs which call for the whole first wall to function as a limiter.

Table 4.4-2 summarizes the design issues which are discussed in the following sections. Of all of these the issues which need more quantitative treatment are the efficiency of tritium recovery from the primary coolant, the impact of the transition from the V-alloy to a high-nickel (possibly double-walled) piping system for the primary coolant outside the vacuum chamber, and the practicality of maintaining a moisture level of $\ll 0.2 \text{ vppm}$ in the helium coolant loop.

TABLE 4.4-2.
SUMMARY OF DESIGN ISSUES FOR THE Li_2O -He BLANKET AND THE TRITIUM AND HEAT REMOVAL SYSTEMS

Design Feature/Material	Issues
He	Thermal-hydraulic performance of He in the laminar-to-turbulent flow transition region; effect of higher system pressure; thermal stresses in plenum region.
Single Coolant/Purge Loop	Impact of tritium removal on power-conversion efficiency; tritium leakage to steam generators; moisture control; tritium and deuterium processing; Na impurities, vaporization and transport.
$\text{Li}_2\text{O}/\text{Be}$ Mixture	Fabricability; chemical reactions; impact of free Be and/or Li on breeder microstructure and tritium inventory, swelling, creep, and hot pressing properties.
V-alloy	Fabricability; radiation embrittlement; effect of alloying elements on permeability; hydrogen and oxygen embrittlement.
Sealed Tubes	Reliability and cost; method of support (e.g., grid spacers); lifetime.
Lobed First Wall	Incompatible with plasma shaping.
W-coated V-alloy	Fabricability; barrier effectiveness.
Double-Walled Piping and Steam Generator Tubes	Joining with V-alloy structures; effect on power-conversion efficiency; oxide barrier effectiveness; in-leakage of steam.

4.4.2 Design Configurations

Figure 4.4-1 is a schematic overview of the Li_2O -He blanket, the heat recovery system and the tritium recovery system. The major design feature that differs from the BCSS design is the extraction of both tritium and heat from a single helium loop. Material differences include the use of D_2 rather than H_2 in the helium, the mixing of Be with the Li_2O breeder, the use of a V-alloy as the structural material (with a W-coated first wall) within the vacuum chamber, and the use of a double-walled, high nickel alloy for the helium coolant piping and the steam generator tubes. A key feature of the tritium recovery system is the use of high temperature liquid sodium to trap both hydrogen isotopes and some of the oxygen in the primary coolant loop. This serves two purposes: by performing this step before the heat recovery, it minimizes the $\text{T}_2(\text{DT})$ partial pressure and leakage in the heat recovery system; and by doing it at high temperature, the thermal energy loss is minimized.

While the heat and tritium recovery systems are still in the conceptual stages, considerable effort has been expended to design the first wall and blanket for the Li_2O -He blanket. Figure 4.4-2 is a scaled drawing of the proposed first-wall/blanket/shield for this system with a conventional limiter. Starting with the plasma parameters given in Section 2 (major radius of 5.25 m, minor radius of 0.875 m, elongation of 2, and a vacuum scrape-off of 0.08 m) the first wall was designed to accommodate the plasma/scrape-off region. The nominal first wall area used for power conversion calculations is 300 m^2 . However, for first-wall/blanket calculations, a more precise number is recommended based on the space required for the limiter plenum and the lobe shape of the first wall modules. Assuming a FED/INTOR standard limiter design,⁽⁵⁾ the limiter plenum requires $\sim 23 \text{ m}^2$ opening at the first wall surface. Thus, the area for inboard and outboard blankets is $\sim 277 \text{ m}^2$. Multiplying this by $\pi/2$ to account for the lobe shape of each module gives an actual first wall area of $\sim 435 \text{ m}^2$ for first wall permeation studies.

The number of outboard blanket modules is 41 per sector, and the number of inboard blanket modules is 18 per sector. The width of the modules in the poloidal direction was scaled from the BCSS value of 0.29 m to 0.172 m based on the reduced circumference (poloidal) of the TPSS reactor. While this is a reasonable first approximation, it does not necessarily represent an optimum

Fig. 4.4-1. Overview of the Li_2O -He blanket, heat recovery, and tritium recovery systems.

Figure 4.4-2. Schematic of the first-wall/blanket/shield system for the Li_2O -He design. Toroidal view showing cross-section of one of the 16 sectors.

value. For example, the smaller this parameter is, the higher the structure fraction.

The lengths of the inboard and outboard modules in the toroidal direction were chosen to give 16 blanket sectors. This approach resulted in an inboard length of 1.52 m and a maximum outboard length of 2.60 m. The height of each module in the radial direction was chosen based on the neutronics calculations (Section 4.4.7). The resulting outboard module height is 0.68 m (0.06 m first wall, 0.40 m breeder, and 0.22 m plenum) and the inboard module height is 0.41 m (0.06 m first wall, 0.24 m breeder, and 0.11 m plenum). Again, these numbers represent a workable design with a 3D breeding ratio of 1.25, an energy multiplication factor of 1.32, and a minimum blanket thickness for a given Be volume fraction of 30%. As explained in Section 4.4.7, there is considerable room for optimization. If the Be fraction is allowed to increase to as much as 90%, an energy multiplication factor of 1.7 can be realized with a TBR of 1.2 and a thinner blanket. Such optimization is left to future systems studies.

A toroidal view of an outboard blanket module is shown in Fig. 4.4-3. The module contains eight rows of breeder tubes in the poloidal direction. The 17.25 mm OD tubes are close-packed in a hexagonal array with a triangular pitch of 18.25 mm. For a toroidal length of 2.60 m, this gives 1223 tubes per outboard module. The diameter of the tubes is limited by both temperature and geometric constraints once the module length and width are specified. Also shown in the figure are the breeder, structure and coolant fractions which are used in the neutronics calculations. The structure volume fraction in the breeder zone is comparable to the average value for the BCSS plate design. However, the He volume fraction is considerably higher (25% vs. 10% for the BCSS plate design) than the corresponding coolant volume fraction for the BCSS plate design. This is primarily the result of using tubes rather than plates. Additional optimization could be done on the tube pitch and module length and width to reduce the coolant volume fraction. The numbers in Fig. 4.4-3 merely serve as a reference point for neutronics, thermal-hydraulic, tritium, and structural calculations.

The inboard module is very similar to the outboard module. The main differences are that the plenum section is only 0.11 m long and the breeder section is only 0.26 m long. In addition, the shorter module length (1.52 m vs.

Figure 4.4-3. Schematic of an outboard module (toroidal view).

2.60 m for an outboard module) in the toroidal direction results in only 705 breeder tubes per inboard module. The resulting volume fractions are essentially the same for inboard and outboard modules.

Not shown in Fig. 4.4-3 is the plenum space within each breeder tube which is needed to contain the helium at a reasonable pressure. Figure 4.4-4 shows the details of a single tube with the plenum length, L_p , left as a design variable. A diametral breeder-cladding gap of 0.25 mm is designed into the element so that breeder-cladding contact due to differential thermal expansion is avoided during initial startup. A number of parameters and design trade-offs are involved in choosing the optimum plenum length for a given breeder length, L_b . The first consideration is helium release rate. Based on the FUBR-1A results,⁽⁶⁾ Li_2O releases ~80% of the helium generated for peak temperatures of 500-700°C. At 900°C, more than 99% of the helium generated is released. For design conservatism, the release rate is set equal to the generation rate ($\dot{n}_{\text{He}}$). Based on the results in Section 4.4.7, the average helium generation rate through the outboard blanket thickness is 144 moles/m³ (MW-Y/m²). The plenum temperature (T_p) is another important variable. The plenum pressure increases and the metal strength decreases as T_p is increased, while the gross thermal efficiency increases. Based on Sections 4.4.5 through 4.4.6, $T_p < 700^\circ\text{C}$. Cladding thickness (h_s) and inner radius (r_s) also enter into the design trade-off considerations. For neutronics purposes r_s should be large and h_s should be small to minimize the structure fraction. However, r_s is limited by breeder temperature constraints, and the ratio r_s/h_s should be as small as possible from a structural mechanics point of view. Lastly, the cladding stress limit, σ_ℓ , which depends on temperature and fluence, enters into the choice of plenum length.

The parameters $\dot{n}_{\text{He}}$, T_p , r_s/h_s , and σ_ℓ , along with the coolant pressure p_c , determine the relationship between cladding design lifetime (t_ℓ) and plenum length. As derived in Section 4.4.6, the relationship is

$$t_\ell = [(h_s \sigma_\ell / r_s + p_c) / (\dot{n}_{\text{He}} R T_p)] L_p / L_b, \quad [4.4-1]$$

where R is the ideal gas constant (8.32×10^{-6} MPa·m³/mole·K), $h_s = 0.25$ mm, $r_s = 3.375$ mm, $\sigma_\ell \geq 170$ MPa based on Section 4.4.6, $T_p < 973$ K, $p_c = 5$ MPa, $\dot{n}_{\text{He}} = 144$ moles/m³ (MW-Y/m²), and t_ℓ is in units of MW-Y/m². As a sample

Figure 4.4-4. Schematic of a breeder tube. The breeder length (L_b) is 0.40 m for an outboard tube and 0.26 m for an inboard tube. The plenum length is a design variable based on limiting the He pressure build-up.

calculation, L_p/L_b is taken as 0.55 based on Fig. 4.4-3 giving $t_\ell \gtrsim 5 \text{ MW-Y/m}^2$ (e.g., 2 full power years at 2.5 MW/m^2). As this value is lower than desired for a design goal, some consideration should be given to increasing the cladding thickness, decreasing the breeder pin radius, lowering the plenum temperature and/or increasing the plenum length. This subject is presented in more detail in Section 4.4.6, along with a discussion of σ_ℓ for V-alloys.

The shield thickness (1.02 m outboard, 0.73 m inboard) in Fig. 4.4-2 is taken from the BCSS $\text{Li}_2\text{O/Ferritic/He}$ design. Optimization of the shielding material and thickness for TPSS application is deferred to future efforts.

4.4.3 Materials Considerations

Three issues are addressed in this section. The first is an evaluation of the potential benefits and problem areas (e.g., compatibility) of mixing Be with Li_2O . As this idea was not considered in previous blanket studies, it is presented in some detail in this section. The second is the compatibility of V-alloys with He and the $\text{Li}_2\text{O}/\text{Be}$ mixture. The material presented on V-alloys/He is a summary of the BCSS work with an update including recent studies and a focus on the particular application for the proposed design. The third area is the compatibility of V-alloys with W, which is proposed as a permeation barrier for the first wall and the blanket-module side walls.

4.4.3.1 $\text{Li}_2\text{O}/\text{Be}$ Breeder

The tritium breeding and energy multiplication benefits of adding Be to Li_2O are well known. Previous design studies^(1,2) assumed that the Be would be in a discrete layer between the first wall and the Li_2O breeder. The TPSS calculations presented in Section 4.4.7 demonstrate that by mixing the Be with Li_2O , the problem of excessively high local heat generation rates can be avoided. Also, sensitivity studies are presented on the increase of tritium breeding ratio (TBR) and energy multiplication factor (EMF) and the decrease of blanket thickness with increasing Be volume fraction.

Two other benefits of mixing Be with Li_2O are enhanced thermal conductivity and oxygen control. Figure 4.4-5 shows the thermal conductivities of 100% dense Li_2O and Be, along with possible reaction products BeO and Li. The unirradiated Li_2O curve was obtained by extrapolation of the results of Takahashi and Kikuchi⁽⁷⁾ for material with densities of ~ 71 to 93% TD. The dashed curve for the estimated thermal conductivity of irradiated Li_2O is taken from the analytical work of Liu and Tam.⁽⁸⁾ The curves for unirradiated Be, BeO (98% dense polycrystalline), and Li (liquid) come from recommended values from Touloukian et al.^(9,10) It is assumed that the thermal conductivity of Be is not degraded by neutron damage (excluding He swelling). However, BeO exhibits a significant reduction in thermal conductivity for temperatures up to 800°C.⁽¹¹⁾ The dashed line in Fig. 4.4-5 represents the limiting value to the data for $400 \leq T \leq 800^\circ\text{C}$ for fast (>1 MeV) neutron fluences greater than a 3×10^{21} n/cm².

Figure 4.4-5. Thermal conductivities of dense Li_2O and Be, along with possible reaction products, BeO and Li.

It is clear from Fig. 4.4-5 that by mixing Be with Li_2O the thermal conductivity of the mixture will be significantly higher than that for Li_2O even if some BeO and Li are formed. To develop a model to predict the in-reactor thermal conductivity of the $\text{Li}_2\text{O}/\text{Be}$ mixture as a function of Be volume fraction (v_B), one should treat the effects of fabricated and swelling porosities, neutron damage, and degree of chemical reaction.

The free energy of formation of BeO is higher (in magnitude) than that of Li_2O (see Fig. 4.3.3-2). For the reaction

the ΔG_0 values are in the range of -7.6 to -9.9 kcal/mole in the temperature range of $775 \leq T \leq 1000\text{K}$. Thus, from an equilibrium point of view, the amount of Be converted to BeO depends on the relative number of moles of reactants in the mixture. However, from a kinetic viewpoint, once the Be forms an oxide layer, the reaction rate slows considerably and is limited by the rate of diffusion of Be ions through the BeO film. This diffusional process is temperature and time dependent, which means that the amount of BeO formed is also time and temperature dependent. Another critical parameter is the surface-to-volume ratio for the Be which depends on initial particle size. The higher this ratio, the more BeO that will form for a given temperature and time.

Until the kinetics of BeO formation are modeled, it is proposed that the chemical reaction be ignored to the first order and that the modeling of the effective thermal conductivity of the breeder include only the effects of irradiation and porosity for a simple $\text{Li}_2\text{O}/\text{Be}$ mixture. This approach should produce reasonable results for design scoping calculations, particularly for low (e.g., ≤ 30 vol. %) Be volume fractions. Let k_b be the effective conductivity of the breeder, k_L the conductivity of 100% dense Li_2O , k_B the conductivity of 100% dense Be, v_B be the Be volume fraction in the initial $\text{Li}_2\text{O}/\text{Be}$ powder mix, and P be the porosity volume fraction in the fabricated sample. For $v_B \leq 0.3$, the Maxwell-Eucken model⁽¹²⁾ is used to predict k_b according to:

$$k_b = f_p \left\{ \frac{1 + 2 v_B (1 - k_L/k_B) / (2 k_L/k_B + 1)}{1 - v_B (1 - k_L/k_B) / (2 k_L/k_B + 1)} \right\} k_L. \quad [4.4-3]$$

The factor f_p is taken as the porosity correction factor determined for Li_2O ⁽⁷⁾

$$f_p = (1-P)/(1+\beta P), \quad [4.4-4]$$

where

$$\beta = 1.45 - 8 \times 10^{-4} T,$$

and

T is in K.

At the other extreme of high Be volume fractions ($v_B \geq 0.7$), the Be will act as the continuous phase and the Li_2O will act as the dispersant. For this case, Eq. 4.4-3 is rewritten as

$$k_b = f_p \left\{ \frac{1 + 2 (1-v_B)(1-k_B/k_L)/(2k_B/k_L + 1)}{1 - (1-v_B)(1-k_B/k_L)/(2k_B/k_L + 1)} \right\} k_B \quad [4.4-5]$$

In the intermediate range ($0.3 < v_B < 0.7$), the effective thermal conductivity of the mixture depends on which component acts like the continuous phase and which acts like the dispersed phase. There is no general rule for determining at what volume fraction the transition is made. The approach adopted in this work is to extrapolate the lower branch (Eq. 4.4-3) of the Maxwell-Eucken equation up to $v_B = 0.4$ and the upper branch (Eq. 4.4-5) down to $v_B = 0.6$. At $v_B = 0.5$, the average of Eqs. 4.4-3 and 4.4-5 is used. A smooth curve is then constructed to connect the points at 0.4, 0.5, and 0.6. This approach does not impact the current work as a Be volume fraction of 30% is used for the reference solid breeder design.

Figure 4.4-6 shows the results for the effective conductivity of an 80% dense $\text{Li}_2\text{O}/\text{Be}$ mixture (at 700°C) as a function of Be volume fraction. The solid curve is for the as-fabricated material. The dashed curve represents the estimated degradation due to irradiation effects. For the dashed curve, the irradiated thermal conductivity of Li_2O is used, and an assumed 10% swelling is added to the 20% fabricated porosity to give an irradiated porosity fraction of 30%. As can be seen from the figure, a Be volume fraction of 30% causes an increase of a factor of ~ 2 in breeder conductivity for both the irradiated and unirradiated cases as compared to pure Li_2O .

Figure 4.4-6. Predicted values for the effective thermal conductivity of unirradiated and irradiated $\text{Li}_2\text{O}/\text{Be}$ breeder material with 20% porosity at 700°C .

With regard to the benefits of adding Be for oxygen control, the major issues are to minimize the tritium inventory due to solubility (and surface adsorption) and to minimize the corrosion/embrittlement of V-alloys at the breeder-cladding interface. As shown in Fig. 4.3.3-2 in a previous section, BeO has a higher (in magnitude) heat of formation (per atom mole of oxygen) than any of the breeder or cladding oxides of interest. This information, along with other data from Ref. 13, was used to generate the free energies of reaction listed in Table 4.4-3. The reactions are arranged in order from most reactive to least reactive. The first two (O_2 and H_2O) and the last (Li_2O) are of interest in this section. Clearly if there is any free oxygen or moisture in the solid breeder, the Be will react with these "oxides" more quickly than with Li_2O . The amount of Be that will react with Li_2O depends on the moisture and oxygen levels within the breeder tube, as well as the kinetics of Be diffusion through BeO.

It appears that Be will act as an effective reducing agent to control the oxygen activity within the breeder tube. What remains to be resolved are the degree to which Be will reduce the Li_2O during fabrication ($T \leq 750^\circ C$) and operation ($T \leq 900^\circ C$) and the effects of free Be and Li on the breeder microstructure. Another issue to be explored is the possibility of the formation of other compounds containing Be, Li, O, and/or T (D). Table 4.4-4 lists some physical properties of Li, Li_2O , LiOH, Be, and BeO. Because of the phase changes of Li and LiOH in the temperature ranges of interest, it is clear that formation of these products will have more serious consequences during fabrication and operation than the simple addition of Be.

The degree to which Be will reduce Li_2O during fabrication and in-reactor operation and the consequences of such reduction need to be investigated experimentally. The effectiveness of dispersed Be in inhibiting OT formation also requires experimental verification, although the arguments for this to be true are compelling based on the information in Table 4.4-3.

4.4.3.2 V-Alloy/He and V-Alloy/ Li_2O /Be

The known and estimated properties of V and V-alloys were reviewed in the BCSS interim⁽²⁾ and final⁽¹⁾ reports. Table 4.4-5 summarizes some basic properties and the BCSS assessment for the reference V-alloy (V-15Cr-5Ti) as compared to the other candidate structural materials. It can be seen from the

TABLE 4.4-3
HEATS OF REACTION FOR Be AND SEVERAL OXIDES OF INTEREST. VALUES
ARE DERIVED FROM THE HEATS OF FORMATION AT ATMOSPHERIC PRESSURE
PRESENTED IN THE JANAF TABLES⁽¹³⁾

Reactants	T, K	ΔG_o , kcal/mol BeO
Be + 1/2 O ₂	573	-131.9
	873	-124.8
	1173	-117.9
Be + H ₂ O	573	-80.4
	873	-71.1
	1173	-74.2
Be + 1/5 V ₂ O ₅	573	-62.2
	873	-56.1
	1173	-50.4
Be + 1/4 V ₂ O ₄	573	-58.6
	873	-57.2
	1173	-62.7
Be + 1/3 Cr ₂ O ₃	573	-53.8
	873	-52.9
	1173	-52.1
Be + 1/3 V ₂ O ₃	573	-46.9
	873	-45.7
	1173	-44.6
Be + VO	573	-41.2
	873	-40.1
	1173	-34.0
Be + 1/2 TiO ₂	573	-31.6
	873	-31.0
	1173	-30.6
Be + 1/3 Ti ₂ O ₃	573	-23.8
	873	-23.1
	1173	-22.3
Be + Li ₂ O	573	-6.2
	873	-9.1
	1173	-12.1

TABLE 4.4-4

SOME PHYSICAL PROPERTIES OF Li_2O , Be, AND POSSIBLE REACTION PRODUCTS. DATA TAKEN FROM REFS. 1, 11, AND 14 TO 16.

<u>Property</u>	<u>Li</u>	<u>MATERIAL</u>		<u>Li_2O</u>	<u>BeO</u>
		<u>LiOH</u>	<u>Be</u>		
Melting Temperature, °C	181	471	1277	1433	2550
Vapor Pressure at 900°C, Pa	1.48×10^3	(924°C BP)	1.0×10^{-8}	6.9×10^{-5}	4.0×10^{-15}
RT Density, g/cm ³	0.53	1.46	1.85	2.01	3.01
Thermal Expansion (RT to 900°C), %	-5.7	--	1.63	2.5	0.7

TABLE 4.4-5
SUMMARY OF BCSS PROPERTIES AND ASSESSMENT FOR V-15Cr-5Ti

Parameters	Austenitic Steel PCA-CW	Ferritic Steel HT-9	Vanadium V-15Cr-5Ti
Melting Temperature, °C	1400	1420	1890
Density, g/cm ³	8.0	7.8	6.1
Thermal Stress Factor, ^a MW/m ² . mm (500°C)	3.2	4.8	9.8
Nuclear Properties ^b			
dpa/(MW-y/m ²)	11	11	11
appm ₃ He/(MW-y/m ²)	174	130	57
W/cm ³ /(MW-y/m ²)	40	40	25
Status of Data Base	Extensive	Extensive - Unirrad. Limited - Irrad.	Limited
Design Stress Limit	190 MPa (500°C)	175 MPa (500°C)	230 MPa (500°C)
S _m (Irrad.) (>10 dpa)	175 MPa (550°C)	160 MPa (550°C)	230 MPa (700°C)
Maximum Allowable Temperature, °C (Irrad. Embrit.)	550	550	750
Radiation Lifetime (Swelling) (5%)	100 DPA (500°C) 150 DPA (400°C)	~ 200 DPA	> 200 DPA
Critical Design Issues	<ul style="list-style-type: none"> • Limited Lifetime (swelling) • High Thermal Stress Factor • Liquid Metal Corrosion • Radiation Creep • Operating Temp. Limit 	<ul style="list-style-type: none"> • Weld Procedure (PWHT) • DBTT above RT • Operating Temp. Limit • Liquid Metal Embrittlement • Ferromagnetic Properties 	<ul style="list-style-type: none"> • R&D Requirements • Weld Procedure (inert environment) • Oxidation Character. • High T Permeation Rates • Costs

^aA higher thermal stress factor indicates lower thermal stresses for a comparable thermal gradient.

^bNuclear properties are from calculations for first wall of a liquid lithium blanket.

table that the V-alloy has superior nuclear, thermal, and mechanical properties. However, because of the limited data base and limited experience with this alloy, it is considered high risk. Also, the 15 wt.% Cr and 5 wt.% Ti may not represent an optimum alloy composition for fusion reactor application. It was chosen for fast breeder reactor development because the Cr makes the alloy more creep- and corrosion-resistant and the Ti is effective in inhibiting irradiation-induced swelling. While the specific properties of V-15Cr-5Ti are used in the remainder of this solid-breeder section for the purpose of performing calculations, this does not imply that a decision has been made to restrict the design to the use of this alloy. Further research is needed to determine the optimum V-alloy for fusion reactors.

Two major problem areas in using V-alloys were highlighted in the BCSS study. The first is fabrication (e.g., welding), and the second is compatibility of V-alloys with impurities in He (e.g., O_2 and H_2O). Both of these areas are related to the adverse effects of oxidation. The subject of V-alloys/He compatibility is treated in the following by first reviewing the BCSS work and then by discussing the status of experimental work reported since the completion of the BCSS effort. Finally, the particular features of the TPSS $Li_2O/He/VCrTi/Be$ blanket which are designed to minimize the corrosion problem are addressed.

The 750°C temperature limit ($0.5 T_m - 50 K$) for V-15Cr-5Ti listed in Table 4.4-5 is based on avoiding He embrittlement due to transmutation alpha particles under fast neutron bombardment. However, it would be more conservative to restrict the temperature of any V-alloy surface in contact with He impurities to less than the minimum melting temperature of the oxides of vanadium. According to Ref. 17, these are: 658°C for V_2O_5 , 950°C (in vacuum) for VO, 1640°C for VO_2 , and 1970°C for V_2O_3 . While the formation of V_2O_5 is to be avoided in the design, a limit of 650°C is still imposed on any V-15Cr-5Ti/He interface. The 750°C limit is imposed in general and would be applicable, for example, to the plasma/first-wall interface.

Vanadium has a strong affinity for oxygen. For water vapor concentrations of 100-200 vppm (expected from steam generator leakage) an oxide film would form almost immediately at all temperatures of interest. The BCSS recommendation was that moisture levels should be kept below 0.1 vppm for the coolant at 5 MPa of pressure. As this was considered impractical for primary-

Figure 4.4-7 Increase in weight of unalloyed V, V-5Ti, V-15Cr, V-15Cr-5Ti, PCA S.S., and HT-9 materials on exposure at 725 K (a), 825 (b) and 925 K (c) to He + 10 ppm H_2O and at 825 K (d) to He + 1 ppm H_2 + 1 ppm H_2O . Nominal He pressure is 1 atm.

Figure 4.4-8 Microhardness profile for unalloyed V (a), V-5Ti (b), V-15Cr (c), and V-15Cr-5Ti (d) after 1000 h exposure to He + 10 ppm H_2O .

In applying the results of this experiment to the design case of interest, it should be noted that the experiments were conducted with He at atmospheric pressure, and the design case calls for 5 MPa of He. Therefore, the 10 vppm of H_2O corresponds to ~ 0.2 vppm H_2O in the design case. This increase in He pressure is expected to slow down the reactivity of water vapor on V surfaces. Also the presence of ~ 7 vppm D_2 added to the He coolant in the design case will slow down the reaction rate even further. Therefore, it is conservative to apply the experimental results for atmospheric He with 10 vppm of H_2O to the design case of 5 MPa He with 0.2 vppm H_2O and 7 vppm D_2 . The experimental results for He-10 vppm H_2O at 925 K (652°C) show a corrosion layer of 29 μm and an oxidation layer of 100 μm for rolled V-15Cr-5Ti. Extrapolating these results to 10^4 hours using the $t^{0.75}$ law suggested by the data implies a corrosion layer of ~ 160 μm and an oxidation layer of ~ 620 μm . For breeder cladding of 0.25 mm thickness and 650°C temperature, the total oxidation plus corrosion layer would be ~ 0.8 mm which is greater than the cladding thickness. Therefore, the moisture level in the helium coolant should be $\ll 0.2$ vppm to insure the structural integrity of the cladding. More experimental work is needed to quantify this limit for 5 MPa He with 7 vppm D_2 .

In the case of the first wall, it is proposed to coat the first wall with tungsten at the coolant interface to inhibit deuterium leakage from the coolant to the plasma. The tungsten barrier will also serve as an oxidation barrier. Also, this surface will be considerably cooler than the breeder cladding. Therefore, the performance of the breeder cladding, and not the performance of the first wall, sets the requirements for the maximum moisture (H_2O , D_2O , T_2O) in the He coolant.

Having limited the moisture level to $\ll 0.2$ vppm in the primary He coolant, one must consider whether or not this is practical for a commercial system. As was mentioned in Ref. 2, the 300 MW(e) GCFR design with in-leakage of 0.58 kg/h steam and a coolant cleanup rate of 1300 kg/h would have a moisture level of ~ 100 vppm in the He coolant. This is orders of magnitude higher than that needed for the TPSS design. Two design innovations which would help lower the moisture level are: the use of double-walled steam generator tubes to both decrease the number of failed tubes (and hence the rate of steam in-leakage) and to retard the out-leakage of tritium; and the

use of high temperature molten Na to remove hydrogen isotopes (T, D, H) and oxygen from the primary He. While this step would impact the gross and net thermodynamic efficiency of the system and add to the cost, the gain in these parameters from the 650°C coolant outlet temperature should still result in a more cost effective system than the BCSS design.

In summary, V-15Cr-5Ti should be compatible with He + 7 vppm D₂ coolant for a maximum structure-coolant interface temperature of 650°C if the moisture level can be kept to << 0.2 vppm. More experimental work and modeling are required to verify that V-alloys will not experience excessive corrosion and embrittlement at this moisture level. Also, the cost and possible thermal-efficiency degradation caused by the cleanup/recovery system and the double walled steam generator tubes need to be evaluated in detail to see if they offset the energy-conversion gains in going to 650°C helium outlet temperature.

With regard to the compatibility of V-15Cr-5Ti with Li₂O + 30 vol. % Be, Table 4.4-3 indicates that Be has a higher affinity for O₂ than does V, Cr, or Ti. Because of the presence of the Be and the 35 Pa of D₂ in the breeder capsule, no problems are anticipated with structure/breeder compatibility. This assessment needs to be quantified by means of detailed thermodynamic and kinetic calculations, as well as verified by experiments.

4.4.3.3 V-Alloy/W

The primary purpose of coating the coolant side of the first wall is to limit the leakage of hydrogen isotopes to levels acceptable for plasma operation. A secondary advantage of the coating may come from inhibiting the rate of oxidation of V-alloys due to O₂ and H₂O impurities in the helium coolant. Questions naturally arise as to the fabricability of such a combination of materials and the compatibility of the two materials. As the W layer is only ~ 1 μm thick while the base first-wall material is several mm, the major compatibility issues are the effects of differential stresses on the coating integrity and the metallurgical integrity of the two materials.

In terms of fabricability of V-15Cr-5Ti structures coated with W, the subject was briefly reviewed in the FED/INTOR program.⁽¹¹⁾ Plasma arc spraying and chemical vapor deposition are recommended over brazing for complex geometries. However, the tungsten coating for these methods tends to

be porous and brittle. For example, plasma arc sprayed coatings (produced in an inert gas environment) have as-deposited densities of $\sim 90\%$, tensile strengths of ~ 420 MPa, and ductile-to-brittle transition temperatures of 350 – 400°C . Brazing is the preferred method of producing denser, more ductile coatings. However, it would be impractical for thin coatings ($\sim 1\ \mu\text{m}$) and for the grooved first wall geometry (required for high neutron wall loadings).

As the first-wall/coating interface will operate at ~ 300 to 400°C , it should be assumed that the tungsten coating would be brittle at these operating temperatures. The thermal, mechanical, and irradiation compatibility of W and V-15Cr-5Ti are explored to estimate the magnitude of stresses at the interface. Table 4.4-6 contains some thermal and mechanical properties of unirradiated W⁽¹⁹⁾ and V-15Cr-5Ti⁽¹⁾ at 300°C . Recall that the bare V-alloy would have a tensile thermal stress at the coolant side of the first wall. The tungsten, with a thermal expansion $\sim 1/2$ that of V-15Cr-5Ti would be put in a state of tension, even if it were at the same temperature as the V-alloy. The thermal-elastic stress σ_c in a coating of thickness t_c can be approximated by the following formula:

$$\sigma_c = \left(1 - \frac{t_b E_b}{t_c E_c}\right)^{-1} (\bar{\alpha}_b - \bar{\alpha}_c) (T - T_R) \quad [4.4-6]$$

where the subscript b refers to the base material, the subscript R refers to a stress-free reference temperature, E is Young's modulus, $\bar{\alpha}$ is the mean thermal expansion coefficient, t is thickness, and T is temperature. Using the properties in Table 4.4-6 gives a thermal stress in the coating of ~ 700 MPa which exceeds the fracture stress of the brittle plasma-arc-sprayed W coating. Therefore, some microcracking of the coating can be expected.

Not very much is known about the irradiated properties of tungsten. Some data⁽¹¹⁾ exist which show that W becomes more brittle with irradiation. Thus, even if a better fabrication technique were developed to deposit the coating onto the V-15Cr-5Ti base material, some thermal stress cracking and loss of the coating effectiveness would be expected. While W has the desirable property of being relatively impermeable to H isotopes, it may not be the optimum coating for V-15Cr-5Ti in a fusion reactor application. Other materials will be considered in future work. In particular, a material like Al would be interesting because of its high resistance to both H_2 and O_2 permeation and its better matched thermal expansion coefficient.

TABLE 4.4-6

UNIRRADIATED PROPERTIES OF V-15Cr-5Ti⁽¹⁾ AND W^{(19)a}

<u>Property</u>	<u>V-15Cr-5Ti</u>	<u>MATERIAL</u>	<u>W</u>
Density, g/cm ³	6.16		19.3
Melting temperature, °C	1890		3410
Thermal conductivity, W/m·K	25		138
Specific Heat, J/g·K	0.52		0.14
Mean thermal expansion coefficient, μm/m·K	10		4.0
Young's modulus, GPa	198		350
Poisson's ratio	0.360		0.292
Yield strength, MPa	470		--
Ultimate tensile strength, MPa	700		~ 400 ^b
Ductile-to-brittle transition temperature, °C	<25		350-400 ^b

^aThermal and mechanical properties at 300°C unless otherwise specified.^bEstimated properties for plasma-arc-spray coating.

4.4.4 Tritium Containment/Recovery

This section is divided into three sub-topics. The first is a description of the proposed scheme for eliminating the separate purge stream for tritium recovery and for recovering tritium directly from the high-pressure helium coolant. The efficient recovery of both tritium and heat from a single helium stream is a major feasibility issue for the proposed design. The second sub-topic is the leakage of hydrogen isotopes from the D₂-doped He coolant through the first wall to the plasma. Calculations are performed for both a bare V-15Cr-5Ti first wall and a W-coated V-15Cr-5Ti first wall. First-wall inventory of hydrogen isotopes is also evaluated to insure that no problems with hydrogen embrittlement or excessive tritium retention are encountered. The third area included in this section focuses on tritium retention in the solid breeder and tritium release from the breeder tube to the helium coolant.

4.4.4.1 Blanket Tritium Recovery Via the Helium Coolant

The helium coolant, solid-oxide, in-tube blanket design has several unique features which have a significant impact on the design of the tritium recovery system: 1) this system is directly coupled to the self-pumped limiter via the helium coolant; 2) recovery of the bred tritium from the high-pressure helium coolant is the primary tritium recovery route; 3) the temperature of the blanket, tritium recovery system, and heat exchanger is to be maintained at temperatures $\geq 500^{\circ}\text{C}$ to achieve maximum efficiency for the fusion reactor; and 4) a high deuterium partial pressure is required in the helium to effect removal of tritium from the breeder tubes.

The combined effect of the first and fourth constraints is examined in Section 4.4.4.2. Since a deuterium partial pressure of ≥ 35 Pa is maintained in the high pressure helium, it is concluded that a tungsten coating on the back surface of the vanadium first wall is required to limit the deuterium permeation from the helium coolant to the plasma to ~ 0.1 kg/d. This amount was chosen since it corresponds approximately to the amount of deuterium burned each day in a 850-MW fusion reactor. Thus the deuterium permeation may be used as a means of fueling deuterium to the plasma.

This section describes an optimized tritium recovery system which addresses the features enumerated above. The three limiting conditions

considered in designing a tritium recovery system for this blanket design concept were: (1) maintaining a high efficiency in the fusion plant by minimizing any low temperature excursions in the tritium recovery system; (2) minimizing the tritium and the deuterium losses through the heat exchanger to the environment; and (3) recovering tritium and deuterium continuously so that the deuterium can be recycled back to the breeder.

Several alternatives were considered. The first was a conventional hydrogen recovery system used on a helium system. The tritium is oxidized forming tritiated water. The water is trapped on a desiccant which is subsequently regenerated. Last, the tritiated water would be reduced to the molecular form so that it could be refueled into the reactor. The main problems seen with this system were: (1) increased pumping power requirements since the total tritium/helium stream (~700 kg/s) will have to be passed through an oxidizing catalyst; (2) a reduced plant electrical efficiency since a substantial fraction of the helium coolant has to be cooled to $\leq$ room temperature; (3) some tritium loss across the heat exchanger due to the presence of unoxidized tritium; (4) increased tritium inventory since several desiccant or cooling beds are needed in this batch method; and (5) potential corrosion/embrittlement of the breeder-cladding, vanadium-alloy structure due to the presence of oxide species.

A second system considered was one in which all vanadium alloy surfaces were coated with palladium ($\leq 20 \mu\text{m}$). The palladium surface would prevent oxide species from contacting the vanadium. It would also eliminate the pumping power losses due to the pumping of the helium coolant through a catalyst bed to oxidize the tritium. This recovery system was rejected for two reasons: the erosion of the palladium coating due to vibration and rubbing between the breeder tubes and the expected capital cost of palladium (~\$20 million).

A third alternative was the use of metal getters. Their use would eliminate oxide species thus protecting vanadium. However, a low tritium recovery efficiency was expected since this system is based on two gas/solid reactions - adsorption and then desorption of the tritium from the getter.

The fourth alternative, the one chosen as the optimum recovery system, is a new concept not presented previously. It is a system in which liquid sodium is used to strip all tritium, deuterium, and oxygen species from the high

pressure helium. The hydrogen species are removed from the sodium by contacting the sodium with a molten salt. The hydrogen species are subsequently removed from the molten salt by electrolysis of the molten salt. Impurities, including oxygen, are removed from the sodium through the use of a cold trap on a small fraction ($\leq 10\%$) of the total sodium flow.

The advantages gained in using this fourth alternative as the recovery method are: (1) elimination of large pumping power losses since the helium can easily bubble through the sodium; (2) the ability to operate the breeder blanket and the tritium recovery system at a temperature $\geq 500^\circ\text{C}$, thus maximizing the total plant efficiency; (3) no appreciable material losses across the heat exchanger to the environment of either deuterium or tritium; (4) minimal tritium inventory in the tritium recovery system because a continuous process is used; and (5) minimal corrosion of the vanadium structural alloy because oxide species are removed.

A potential problem area with the proposed tritium recovery system is sodium vapor transport. This is aggravated by the high helium temperature (650°C) desired and by the bubbling of the helium through the sodium. As only 10% of the coolant is diverted through the tritium recovery system, it may be more desirable to send the coolant through the steam generator unit before the diversion to the tritium recovery system takes place. In this way, lower temperature helium and sodium (e.g., 325°C) could be used.

The tritium recovery system proposed is shown in Fig. 4.4-9. The gas species recovered at the isotope separation unit are routed separately, tritium directly to the plasma pellet injector and the deuterium back to the helium coolant. Each time the reactor is started, a small pumping system (all that is needed with the self-pumped limiter concept) is linked to the FCU (fuel cleanup unit) to collect any tritium and/or deuterium species removed from the plasma chamber.

The size of the tritium recovery system is a function of the flow rates to be handled and the capacity of the recovery media for hydrogen and oxygen. The flow rate for helium is 700 kg/s, for deuterium is ~ 0.005 kg/s, and that for tritium is ~ 0.002 g/s. The high removal rate required for deuterium (1000 times that for tritium) drives the size of the blanket tritium recovery system. Using Eq. 4.4-7 and the known amount of deuterium to be handled (5 g/s), the minimum amount of sodium needed at 500°C to process the deuterium is

Figure 4.4-9 Schematic of tritium recovery system for solid oxide/helium breeder -- no purge stream.

~262 kg. A second criterion for sizing the system is the volume of helium to be contacted per second. This is 700 kg/s or ~7000 L/s. To have the helium contact an equivalent volume of sodium will require ~7000 kg of sodium. The total vessel volume for this latter quantity is ~7 m³.

It is assumed that for efficient transfer the sodium will contact an equivalent volume of molten salt. At a salt density of ~2 g/ml, ~14,000 kg of molten salt is required. This would be another 7 m³ volume. The electrolysis unit would be of equivalent or smaller size. The blanket tritium recovery system thus occupies a modest volume, <25 m³.

The solubility of hydrogen as NaH in sodium⁽²⁰⁾ is

$$\log S_H \text{ (appm)} = 6.067 - 2880/T . \quad [4.4-7]$$

The solubility of oxygen as NaH in sodium⁽²¹⁾ is

$$\log S_O \text{ (appm)} = 6.967 - 2809/T . \quad [4.4-8]$$

Experimental tests are required to determine the optimum molten salt and the associated electrolyte efficiency for recovering hydrogen species in this proposed system. The appropriate processing time on the cold trap also needs to be determined. In addition, the design of the helium/sodium contactor must be optimized to minimize sodium vapor transport to the helium system.

4.4.4.2 Tritium Containment and Inventory for the First Wall

Because of the deuterium (D) added to the helium coolant, its permeation through the first wall could be a serious feasibility issue. Another factor of concern is the hydrogen (H, D, T) concentration in the vanadium first wall. If the concentration exceeds a certain limit, embrittlement of vanadium may occur.

To model this problem, the first wall is subjected to two simultaneous boundary conditions. The boundary near the plasma side is exposed to an intense D-T particle flux while the back surface facing the helium coolant is subjected to the deuterium partial pressure in the helium coolant. The model assumes a deuterium or tritium atom implantation flux J_1 to a depth δ in the wall which is much less than the wall thickness d . This implantation depth

depends primarily on the energy of D-T atoms which is assumed in the 100's of eV range.

It is assumed that gas molecules leave either surface by recombination-limited desorption according to

$$J = K_r C^2, \quad [4.4-9]$$

where C is the dissolved hydrogen isotope concentration near the surface. The recombination coefficient K_r is given by⁽²²⁾

$$K_r = \frac{8 \alpha}{K_{s_o}^2 \sqrt{\pi kMT}} \exp \left(\frac{2E_s - E_x}{kT} \right) \quad [4.4-10]$$

where

α = sticking coefficient (≈ 1 for clean surface)

K_{s_o} = pre-exponential Sievert's constant

k = Boltzmann Constant

M = Molecular weight of molecule formed by recombination

T = temperature in K

E_s = energy of solution for hydrogen in metal

$E_x = E_s + E_d$ (E_d = diffusion energy)

and

$$E_x = \begin{cases} 0 & E_s + E_d < 0 \\ E_s + E_d & \text{otherwise} \end{cases}$$

On the coolant side, it is assumed that nonequilibrium flow exists at the wall surface, i.e., recombination-limited desorption and dissociation-limited adsorption. The molecular adsorption rate J_d is related to the deuterium gas pressure P above the surface by a dissociation coefficient K_d according to

$$J_d = K_d P \quad [4.4-11]$$

where

$$K_d = K_g^2 K_r, \quad [4.4-12]$$

and K_g is the Sievert's constant.

The governing equations for the surface currents can then be written as

$$J_1 = K_{r1} C_1^2, \quad [4.4-13]$$

$$J_2 = K_{d2} P - K_{r2} C_2^2, \quad [4.4-14]$$

also

$$J_1 = J_2 + J_i, \quad [4.4-15]$$

where the subscripts 1 and 2 refer to the front (plasma) and the back (coolant) surfaces, respectively and J_i is again the implantation flux. The surface current J_2 then represents the net deuterium flux going into the wall (and then to the plasma) from the helium coolant. The above equations were solved numerically using efficient iteration techniques to yield very accurate solutions.

A vanadium (V) wall coated with a very thin layer of tungsten (W) can mathematically be treated as if the wall were pure vanadium with an effective sticking coefficient, α_{eff} , in the surface recombination factor K_r . This effective sticking coefficient can easily be shown to be

$$\alpha_{\text{eff}} = \alpha \left(\frac{\rho_v}{\rho_w} \right)^2 e^{-(E_s^w + E_d^w)/KT}, \quad [4.4-16]$$

where ρ is the density of the material. In this calculation, α_{eff} is calculated at the higher temperature value of the first wall.

Results and Discussions

The calculations presented in this section are for a vanadium first wall of 290 m² surface area and 2.5 mm thickness. The D-T particle flux is on the order of 2×10^{24} particles per second, assuming 100% first wall as a

Figure 4.4-10. Permeation of deuterium (D) from the He coolant to the plasma for a bare V first wall and a W-coated (coolant side) V first wall.

limiter. The deuterium permeation rate from the helium coolant to the plasma side is shown in Fig. 4.4-10 for different deuterium partial pressures in the coolant. The calculation is shown for both a pure vanadium wall and for vanadium coated with a very thin layer of tungsten on the coolant side. This thin layer acts as a permeation barrier for the deuterium in the coolant. As can be seen from Fig. 4.4-10, the permeation of deuterium (with partial pressure in the required range of $10 + 100$ Pa in helium coolant) through an uncoated vanadium wall is in the 10's of kg/d which is clearly unacceptable. However, for a tungsten-coated vanadium, the permeation rate to the plasma side is about 2 + 3 orders of magnitude lower which may be acceptable from the design point of view. The tritium inventory in the wall is on the order of a few grams in the entire wall.

Another important factor is the hydrogen isotope concentration in vanadium. If the concentration exceeds a certain limit, it may cause embrittlement of vanadium and subsequent failure of the structure. Figure 4.4-11 shows the maximum hydrogen isotope (D) concentration in the vanadium wall for different deuterium partial pressures in the coolant. For the case of vanadium with no coating, and at the lower temperature portions of the wall, the concentration of the hydrogen isotope exceeds 100 wppm which may cause embrittlement in the required range of partial pressures. However for the higher temperature portions of the wall, the hydrogen isotope concentration is lower than 100 wppm. For tungsten-coated vanadium the concentration is well below the limit that may cause embrittlement. Table 4.4-7 summarizes the permeation and the concentration calculations along with the reference values used in the calculations. As a general conclusion it seems that a perfect thin layer of tungsten coating on the vanadium wall near the coolant side will substantially reduce both the deuterium permeation through the wall and the hydrogen isotope concentration in the vanadium wall to an acceptable limit.

Based on the BCSS review,⁽¹⁾ embrittlement of V and V-alloys may become a problem at hydrogen concentrations of ~ 100 wppm. As the primary hydrogen isotope in the vanadium will be deuterium for this reactor design, a design limit of 200 wppm is imposed. At the low temperature of 300°C , this limit still necessitates that a W coating be used. Thus, it is concluded that a coating such as W must be used in the design to avoid both excessive D_2 leak-

Figure 4.4-11 Maximum hydrogen isotope concentration in the first wall as a function of D_2 partial pressure in the He coolant. For bare V, deuterium is the primary hydrogen isotope in solution. For coated V, about half of the wppm is T and half is D.

TABLE 4.4-7.

HYDROGEN ISOTOPE CONCENTRATION IN VANADIUM

DT particle flux = 2×10^{24} #/s (100% first wall as a limiter)

Wall thickness = 2.5 mm

Wall area = 290 m²

(P _{D₂}) Pressure (Pa)		0.1	1.0	10.100.	
T = 700°C	without coating	concentration (wppm) ^a	1.4	4.3	13.644
		permeation (g/d) ^b	7.9×10^2	3.4×10^3	1.2×10^4
	with W coating	concentration (wppm) ^c	.44	.45	.48.70
		permeation (g/d) ^b	.25	2.76	27.8279
T = 300°C	without coating	concentration (wppm) ^a	23	73	231732
		permeation (g/d) ^b	7.38×10^3	2.96×10^4	10^5
	with W coating	concentration (wppm) ^c	6.48	6.49	6.567.3
		permeation (g/d) ^b	.34	3.6	36.2363

^a Concentration of D₂ due to P_{D₂}^b Flux of D₂ to plasma due to P_{D₂} in coolant (back diffusion caused by T-implementation not included)^c Concentration of D and T

age to the plasma and excessive embrittlement of the vanadium structural material.

The above analysis was performed for a pure V first wall and a "perfect" W coating. The permeation rate of hydrogen isotopes through V-15Cr-5Ti might be less than it is for pure V. However beneficial this might be, the effect is overshadowed by the fact that the W coating will not act as a perfect layer. If plasma-arc-spraying is used as a fabrication method for coating the first wall, then the W coating will be brittle at the lower temperature and the density of the coating will be ~ 90% (see Section 4.4.3.3). A sensitivity study was performed to determine the minimum barrier factor required to meet the design constraints. It was concluded that a barrier factor of ~ 500 would be required. It is doubtful that the porous, brittle W resulting from plasma-arc-spraying would meet this requirement. Thus, either a better fabrication technique would have to be developed or a more-compatible (with V) coating would have to be explored. Aluminum is an interesting possibility as it has a low permeability and its thermal expansion coefficient is matched reasonably well with that of V. However, its low melting point, while an advantage in fabrication, would cause problems during transients. More work is required in this area.

4.4.4.3 Solid Breeder and Cladding

Of the solid breeder materials (e.g., Li_2O , LiAlO_2 , Li_4SiO_4 , Li_2ZrO_3 , etc.) considered for fusion reactor blankets, Li_2O appears to have the highest tritium bulk diffusion coefficient. Based on the work of Guggi et al.,⁽²³⁾ the diffusion coefficient for tritium in single-crystal Li_2O is

$$\ln (D/\text{cm}^2 \text{ s}^{-1}) = - (5.93 \pm 0.48) - (19.52 \pm 1.01) \text{ kcal/mol/RT}, \quad [4.4-17]$$

where $R = 1.986 \times 10^{-3} \text{ kcal/mol}^\circ\text{K}$ and T is temperature in K. For reasonably small grain diameters ($< 50 \mu\text{m}$), the diffusive inventory in Li_2O tends to be insignificant compared to the inventory associated with other mechanisms such as solubility and surface adsorption. As a sample calculation, let I_d be the tritium diffusive inventory in wppm. The steady-state value of I_d can be found from:⁽¹⁾

$$I_d = [r_g^2 / (15 \bar{D})] \dot{G},$$

[4.4-18]

where r_g is the grain radius in cm, $\bar{D}$ is the temperature-averaged diffusion coefficient in cm^2/s , and $\dot{G}$ is the tritium generation rate in wppm/s. Taking the extreme case of a large grain size ($r_g = 25 \times 10^{-4}$ cm), a low temperature ($T = 683$ K), and the BCSS tritium generation rate of $\sim 2 \times 10^{-5}$ wppm/s, gives a diffusive inventory of only $I_d \sim 6 \times 10^{-3}$ wppm. For the BCSS $\text{Li}_2\text{O}/\text{He}$ blanket, this would correspond to a mere 3g of T_2 .

Table 4.4-8 summarizes the tritium retention in Li_2O and LiAlO_2 from four sets of irradiation experiments. With the exception of the VOM-15H in-situ tritium recovery test, all the remaining tests (closed capsules) with Li_2O resulted in inventories considerably greater than one would predict based on bulk diffusion. The same is true for the LiAlO_2 tests. The low tritium inventories in VOM-15H and TRIO-1 were achieved with open capsule tests with a He purge stream doped with from 0.1 to 1.0% (340 to 3400 Pa) H_2 . As a sealed tube is proposed for the TPSS solid breeder design, it is important to review the conditions under which these high inventories were measured, the mechanisms responsible for the high inventories, and the special features of the TPSS design which are included to minimize the tritium inventory.

Possible mechanisms which could be responsible for the high tritium inventory in the Li_2O closed-capsule tests are solubility, surface adsorption (and/or slow kinetics for surface combination of molecular elements and molecular desorption), and irradiation effects such as trapping of tritium in He bubbles. The formation and precipitation of LiOT as a separate phase could result in a high tritium inventory. As summarized in Ref. 2, in order to avoid precipitation of LiOT as a separate phase, the partial pressure of LiOT above the breeder should be less than

$$P_{T_2O} \leq \begin{cases} (3.49 \times 10^{11} \text{ Pa}) \exp (-30.8 \text{ kcal/mol/RT}) & \text{for } T \leq 744 \text{ K} \\ (1.39 \times 10^8 \text{ Pa}) \exp (-20.1 \text{ kcal/mol/RT}) & \text{for } T > 744 \text{ K} \end{cases} \quad [4.4-19]$$

At the low temperature of 683 K, this pressure corresponds to 48 Pa, and at the high temperature of 1173 K, it corresponds to 3.4×10^4 Pa.

TABLE 4.4-8

SUMMARY OF TRITIUM RETENTION IN FOUR SOLID BREEDER IRRADIATION EXPERIMENTS

Experiment	Density (% TD)	Grain-Size (μm)	Temperature ($^{\circ}\text{C}$)	^6Li Burnup (at.%)	Retained Tritium	
					%	wppm
<u>Li_2O</u>						
TULIP/EBR-II	85-89	40-50	608	1	<9.7 ^a	<182
(Sealed Capsule)	85-89	40-50	625	1	<8	<124
	85-89	40-50	608	3	<6.5	<364
	85-89	40-50	625	3	<1.4	< 78
	85-89	40-50	470	1	<10.8	<259
FUBR-1A/EBR-II	85	4	500	1.3	7.0	82
(Sealed Capsule)	85	7	700	1.3	2.2	25
	85	17	900	1.3	0.7	8.2
VOM-15H/JRR-2	86	<10	750	0.23	0.1-0.2	0.5-0.9
(In-Situ)						
<u>$\gamma\text{-LiAlO}_2$</u>						
FUBR-1A/EBR-II	85	<1	500	1	29	190
(Sealed Capsule)	85	<1	700	1	6.0	40
	85	<1	900	1	2.6	17
TRIO-1	65	0.2	650	0.18	0.003	<0.1
(In-Situ)						

EA403-4 retained 50% tritium and 45% helium generated during EBR-II irradiation.

Assuming that the partial pressure of T_2O is below the limit specified by Eq. 4.4-19, then the mole fraction of dissolved $LiOT$ in Li_2O is given by

$$\log (X_{LiOT}) = (0.427 + 1.7 \times 10^{-4}T) \log (9.86 \times 10^{-5}P_{T_2O}) \\ + (-17.677 + 2.502 \times 10^{-2}T - 9.62 \times 10^{-6}T^2), \quad [4.4-20]$$

where P_{T_2O} is in Pa. Using the limiting pressures and temperatures from Eq. 4.4-19 in Eq. 4.4-20 leads to 1.31×10^{-2} wppm at 683 K and 48 Pa and to 1.37×10^3 wppm at 1173 K and 3.4×10^4 Pa. Therefore at the low temperature, it is possible that precipitation of $LiOT$ as a separate phase could account for the high tritium inventory in the Li_2O closed capsule tests. At the higher temperatures, the solubility of $LiOT$ in Li_2O is high enough to possibly account for the high inventories observed.

Characterizing the surface adsorption/desorption of Li_2O is very difficult to do without some basic data because of the complex nature of the mechanisms involved. The data for several other oxides are reviewed in order to estimate the order of magnitude of the surface effects on tritium inventory. Figure 4.4-12 summarizes the data of Morimoto et al. (24, 25) for the amounts of chemisorbed water on the surfaces of Al_2O_3 , Fe_2O_3 , TiO_2 , ZnO , and SiO_2 . The data can be used in two ways. First, the results are directly applicable to fabrication because they indicate the range of temperatures which must be used to "dry out" such materials after fabrication. Second, they indicate the potential for surfaces to trap OH groups when the O and H are generated within the sample. In the fusion application, the T is generated by nuclear means from Li and the O is available because of the Li burnup. If the samples are heated in the presence of an H_2O partial pressure, additional chemisorption occurs. As the temperature is reduced in the presence of the H_2O partial pressure, then physisorption also occurs. For example, the ratio of physisorbed H_2O to chemisorbed OH was observed to be from 1:2 to 1:1 for the samples studied by Morimoto et al.

A semi-empirical model was used to fit the $\gamma-Al_2O_3$ data of Morimoto et al. A reasonably good fit to the $\gamma-Al_2O_3$ chemisorbed OH in vacuum (Fig. 4.4-12) was obtained by the correlation:

Figure 4.4-12 Chemisorption of water vapor. by several oxides. Samples were heated for 4 hours in vacuum (1.33×10^{-3} Pa) at the indicated temperature. (24,25)

$$N_{cv} = 7.1 \times 10^{18} (1064/T - 1), \text{ H atoms/m}^2, \quad [4.4-21]$$

where T is in K. The additional chemisorbed OH in the presence of H₂O partial pressure can be represented by

$$N_{ca} = \frac{21 (P/P_o)}{(1-P/P_o)(1+20P/P_o)} [10^{19} - 7.1 \times 10^{18} (1064/T-1)], \text{ H atoms/m}^2, \quad [4.4-22]$$

where P is the H₂O partial pressure and P_o is the saturation pressure at the temperature T. Finally, the number of H atoms associated with physisorption of H₂O can be represented by

$$N_p = \frac{21 (P/P_o)}{(1-P/P_o)(1+20P/P_o)} (2N_{mp}), \text{ H atoms/m}^2, \quad [4.4-23]$$

where N_{mp} is the surface density of liquid H₂O molecules. For H₂O, N_{mp} varies from 6×10^{18} molecules/m² at room temperature to 2.8×10^{18} molecules/m² at 374°C (the triple point for water). In terms of standard models for physisorption, Eq. 4.4-23 is in the form of a Type II adsorption isotherm.⁽²⁶⁾ Some question arises as to how to calculate N_{ca} and N_p for temperatures above 374°C because the saturation pressure is 2.21×10^7 Pa at this temperature and is not defined for higher temperatures. The data base does not include heating to higher temperatures in the pressure of water vapor. The data for (N_{ca} + N_p) were determined after cooling to 20°C following a pre-heat under essentially vacuum conditions. Intuitively, one would expect the amount of chemisorbed water to increase with water vapor partial pressure even at higher temperatures. To reflect this dependency, P_o is set at 2.21×10^7 Pa, and N_{mp} is set at 2.8×10^{18} molecules/m² for T $\geq$ 374°C. While this decision does not contradict the results of Morimoto et al., it is somewhat arbitrary, and it does require experimental verification.

Equations 4.4-21 to 4.4-23 are applied to the fusion breeder problem by assuming that the hydrogen isotope of interest is tritium (T). Let A_g be the specific pore-solid surface area in m²/g for the breeder material. To convert to wppm of T in the breeder, the number of H atoms/m² is simply multiplied by A_g times the atomic weight of tritium (3 g/g.atoms) times 10⁶ divided by Avogadro's number (6.023×10^{23} atoms/g.atom) to give

$$I_a = 35.3 (1064/T-1) A_s$$

$$\begin{aligned}
& + \frac{21 P/P_o}{(1-P/P_o)(1+20P/P_o)} [49.8 - 35.3(1064/T-1)] A_s \\
& + \frac{21 P/P_o}{(1-P/P_o)(1+20P/P_o)} (9.96 \times 10^{-18} N_{mp}) A_s, \text{ wppm} \quad [4.4-24]
\end{aligned}$$

Sample calculations were performed for the TRIO-1 and FUBR-1A γ -LiAlO₂ materials and the FUBR-1A Li₂O material. A crude estimate was made from photo micrographs to determine A_s for the TRIO-1 material. The specific pore-solid surface area associated with the large particles ($\sim 50 \mu\text{m}$ in diameter) was only $\sim 0.02 \text{ m}^2/\text{g}$ while the fine pore-solid surface area associated with the grains ($\sim 0.2 \mu\text{m}$ in diameter) was $0.45 \text{ m}^2/\text{g}$. Thus, to the first order, $A_s \sim 0.5 \text{ m}^2/\text{g}$ for the γ -LiAlO₂ used in TRIO. Figure 4.4-13 shows a comparison between the model predictions (dashed lines) for I_a and the values determined from the TRIO data (solid lines) for I_a . The experimental values were determined by subtracting the calculated diffusive inventory from the reported total inventory. The model predictions were calculated from Eq. 4.4-24 assuming $P/P_o \ll 1$ (i.e., using only the first term). This is because of the high temperatures involved and the purge flow conditions.

The agreement between the model calculations and the TRIO data for I_a is remarkably good. In general, the predictions are within the error bars for the data. For the case of run 20 when O₂ was added to the purge, the model underpredicts the data. If any oxygen atoms are adsorbed to the LiAlO₂ surface during this run, then the chemisorbed tritium in the form of OT would be greater.

The specific surface areas for the FUBR-1A pellets are $0.5 \text{ m}^2/\text{g}$ for 85% dense γ -LiAlO₂, $0.04 \text{ m}^2/\text{g}$ for 95% dense γ -LiAlO₂, and $0.085 \text{ m}^2/\text{g}$ for 85% dense Li₂O.⁽²⁷⁾ Table 4.4-9 lists the model predicted values for diffusive, solubility, and adsorption inventories for the FUBR samples along with the total inventory measured. Because the FUBR samples were cooled before the measurements were taken, a temperature of 30°C was assumed for the physisorption term in the adsorption model. At this temperature, the partial pressure of T₂O is a critical variable. As this pressure is not known, the pressure is back calculated such that the calculated tritium retention ($I_d + I_s + I_a$) agrees with the measured inventory. Therefore the test of the models lies in the evaluation of how reasonable the back-calculated T₂O partial pressures are.

The calculation is straightforward for the γ -LiAlO₂ cases because solubility is ignored. The calculated diffusive inventory is negligible for the two higher temperatures and only 4% of the measured inventory at the lowest temperature. The amount of chemisorbed T (in the form of OT) is calculated to be small at the operating temperatures (~ 0 at 863°C, 1.34 wppm at 716°C, and 6.96 wppm at 490°C). As the temperature is reduced to the assumed 30°C after operation, the partial pressures needed to bring the total physisorbed and chemisorbed retentions into agreement with the data are 88 Pa for the first case, 350 Pa for the second case and 3,100 Pa for the third case. These pressures are high relative to Hollenberg's⁽⁶⁾ calculated value of T₂ partial pressure (30 Pa). Also, he assumed that the Cs getter tab was 100% effective in reducing the T₂O. If the Cs getter was not 100% effective, then the back-calculated T₂O partial pressures are plausible.

For the Li₂O cases, the calculation is more complicated because of the effect of solubility inventory. The calculated diffusive inventory is negligible for all three cases even allowing for grain growth. The high temperature case is easily rationalized by assuming 10 Pa of T₂O at the operating temperature. The solubility calculated from Eq. 4.4-20 is the dominant retention mechanism under these conditions. The adsorption inventory is negligible at the operating temperature and only 0.7 wppm at 30°C and 10 Pa. The middle temperature case requires a T₂O partial pressure of 420 Pa at the operating temperature to yield 13.7 wppm for I_g. The adsorption inventory is only 0.29 wppm at the operating temperature and 11.3 wppm at 30°C and 420 Pa T₂O pressure. For the low temperature case, the formation and precipitation of LiOT at 490°C and 270 Pa is the dominant retention mechanism.

In summary, the proposed models give reasonable predictions for the TRIO γ -LiAlO₂ cases with on-line tritium recovery by means of a purge stream. The adsorption model needs to be generalized to include the effects of purge chemistry (H₂ and O₂ dopants) and transient kinetics. The model predictions for FUBR-1A γ -LiAlO₂ cases are plausible but not convincing. The degree to which the adsorption model applies depends on the effectiveness of the Cs getter tab in controlling the oxygen level in both the gas phase and at the pore-solid surface. Note that the getter is not in contact with the breeder. It would only affect the breeder inventory indirectly by reducing the T₂O in the plenum space. The FUBR Li₂O results can be rationalized by a combination of the

Figure 4.4-13 Estimation of the "surface adsorption" inventory for TRIO-1 runs 9-27 as a function of purge chemistry and average LiAlO_2 temperature. Solid lines are from data.⁽³⁾ Dashed lines are based on model calculations (Eq. 4.4-24).

TABLE 4.4-9
COMPARISON BETWEEN MEASURED TRITIUM RETENTION (I) AND CALCULATED
VALUES FOR THE DIFFUSIVE (I_d), SOLUBILITY (I_s), AND SURFACE
ADSORPTION (I_a) INVENTORIES FOR THE γ -LiAlO₂ AND Li₂O MATERIALS
USED FUBR-1A.

Material	Average Temp. °C	Measured I, wppm	Calculated Retention, wppm			Assumed P _{T₂O} , at 30°C, Pa
			I_d	I_s	I_a	
γ -LiAlO ₂	863	17	<0.00	--	17	88
	716	40	0.04	--	40	350
	490	190	7.90	--	180	3,100
Li ₂ O	867	8.2	<0.00	7.5	0.7	10
	697	25	<0.00	13.7	11.3	420
	490	82	<0.00	72.5	9.5	270

solubility and adsorption models. The range of partial pressures required (10 Pa to 420 Pa) are reasonable. Except for the 697°C case, the "solubility inventory dominates. The term is used loosely in this context as it includes LiOT precipitates at the low operating temperature.

Three design features are used in the TPSS $\text{Li}_2\text{O}/\text{He}$ blanket which would tend to minimize the tritium inventory in the solid breeder. By mixing Be with the Li_2O , better control of the oxygen activity at the level of the breeder would be realized. This would reduce both the solubility and adsorption inventories considerably. Secondly, by doping the purge (and hence the breeder tube plenum) with deuterium, the desorption of tritium in the form of DT is enhanced. Finally, the use of V-alloy cladding allows permeation of tritium at a very small T pressure buildup within the breeder tube plenum. Under these conditions, the retention in the TPSS design should be closer to the values observed in the purge flow tests (< 1 wppm) than the high values observed in the closed-capsule tests.

Once the tritium is released from the breeder in the form of DT, it is transported across the highly permeable V-alloy cladding with very little pressure build-up in the capsule. The only data reported are from the experimental work of Heinrich et al. (as discussed in Ref. 28) for the permeability of hydrogen through pure V at 450-550°C. Unfortunately, the H_2 partial pressure used was ~ 1 atm which is too high for the region of design interest. The equation which gives a reasonable fit to the data is

$$(P_m)_H = 2.06 \times 10^{-4} \exp(-750/T), \text{ mol/m}\cdot\text{s}\cdot\text{Pa}^{1/2}, \quad [4.4-24]$$

where T is in K. Rather than use this data, the permeability for tritium and deuterium is calculated from the product of the solubility and the diffusivity. Cantelli et al. (29) measured the diffusion coefficient for deuterium in pure V as:

$$D_D = 3.1 \times 10^{-8} \exp(-847/T), \text{ m}^2/\text{s} . \quad [4.4-25]$$

Assuming that the diffusion of tritium can be determined by ratioing the square roots of molecular weight, the following would be a reasonable representation of D_T .

$$D_T = 2.5 \times 10^{-8} \exp(-847/T), \text{ m}^2/\text{s} . \quad [4.4-26]$$

The solubility of hydrogen in V is well known. Based on the work of Veleckis and Edwards,⁽³⁰⁾ the solubility of hydrogen isotopes in V can be represented by

$$S = 0.138 \exp(3490/T), \text{ mol/m}^3 \cdot \text{Pa}^{1/2} . \quad [4.4-27]$$

Using the appropriate products to find the permeability of deuterium (P_{mD}) and tritium (P_{mT}) gives

$$P_{mD} = 4.3 \times 10^{-9} \exp(2640/T), \text{ mol/m} \cdot \text{s} \cdot \text{Pa}^{1/2} \quad [4.4-28]$$

and

$$P_{mT} = 3.5 \times 10^{-9} \exp(2640/T), \text{ mol/m} \cdot \text{s} \cdot \text{Pa}^{1/2} . \quad [4.4-29]$$

Based on the parameters for the D and S data base, these equations are considered valid for hydrogen partial pressures $\leq 10^4$ Pa and temperatures $\leq 657^\circ\text{C}$.

The peak tritium generation rate in the breeder is 7.6×10^{-6} moles $T_2/\text{m}^3 \cdot \text{s}$ (see Section 4.4.6). The wall thickness is 0.25 mm, and the minimum permeability (at 650°C) is 7.51×10^{-8} moles $T_2/\text{m} \cdot \text{s} \cdot \text{Pa}^{1/2}$. Under these conditions, the increase in T_2 partial pressure from coolant to breeder would be negligible (less than 2×10^{-3} Pa). Thus the T_2 (or DT) partial pressure within the breeder tube would essentially be the same as the coolant partial pressure which is determined by the tritium recovery system.

Sample calculations were performed to determine if the deuterium concentration in the breeder tubes or the module side walls would present an embrittlement problem. For an assumed D_2 partial pressure of 35 Pa, the D_2 concentration in these materials would exceed the 200 wppm limit for temperatures of less than 400°C . With a He inlet temperature of 325°C , some embrittlement problems might be encountered for the breeder-module side walls near the He inlet. The breeder cladding has temperatures above 400°C along its entire length.

4.4.5 Thermal Hydraulics

A number of topics are considered in this section. In the first subsection, the role of helium coolant pressure on net thermodynamic efficiency is examined. This subsection is concluded with a discussion of the gross and net thermodynamic efficiencies expected for the TPSS $\text{Li}_2\text{O}/\text{He}$ design. In the second subsection, calculations are presented for sizing the coolant inlet and outlet manifolds. The thermal-hydraulics analysis for the BCSS He-cooled first wall is reviewed in the third subsection. This forms the basis for the scaling of results to the TPSS materials and operating conditions. The fourth subsection concentrates on the temperatures and temperature limits in the TPSS $\text{Li}_2\text{O}/\text{He}$ first wall and breeder tubes.

4.4.5.1 The Effect of Helium Pressure and Temperature on Thermodynamic Efficiency

A review of the literature was carried out to study the performance of He coolant as a function of temperature and pressure. The operating conditions for High Temperature Gas-Cooled Reactors (HTGR) and Gas-Cooled Fast Reactors (GCFR) are examined in detail.

The operating conditions of a number of HTGR and GCFR designs are summarized in Tables 4.4-10 to 4.4-15.⁽³¹⁻³⁶⁾ An examination of the operating conditions shown in these tables indicates that pressures as high as 11.7 MPa (1700 psia) have been considered. However, the maximum calculated cycle efficiency depends on the maximum operating temperature. The net cycle efficiency, which includes primary-coolant pumping power losses, depends on system pressure. For example, Table 4.4-11 shows that the calculated cycle efficiency of 45% corresponds to an operating pressure of 6.0 MPa (870 psia) and 850°C for a direct conversion (He-turbine) power cycle. Further examination of the tabulated results shows that the cycle efficiency is limited by the steam cycle efficiency of the binary power plants.

The effect of the operating pressure depends on whether the operating system consists of a binary cycle (via steam turbines) or a direct cycle (via closed cycle gas turbines). For the steam cycle, the effect of the pressure is related to the frictional pressure losses in the helium loop. For the second case, the frictional pressure losses in the helium loop as well as the expansion ratio in the gas turbines affect the cycle efficiency. The other

effects of the pressure on the blanket performance are independent of the type of power conversion system. This is related to the coolant velocity in the various blanket components such as the coolant channels, inlet and outlet headers, valves, fittings, bends, recirculators, and the heat exchangers. The velocity of the coolant, which is directly dependent on the system pressure, may produce such undesirable effects as flow-induced vibration, noise, erosion, and other motion-dependent phenomena. A discussion of the role of the operating pressure on the cycle efficiency is presented below.

Let us consider a direct cycle power conversion system schematically shown in Fig. 4.4-14.⁽³⁵⁾ An ideal cycle efficiency for this system can be calculated with the following assumptions.

1. The working fluid is an ideal gas. (Note that helium behaves essentially like an ideal gas at the reactor operating conditions.)
2. Frictional pressure losses are zero.
3. Expansion and compression processes are isentropic.

Figure 4.4-15 shows the state of the fluid in a temperature/entropy diagram, where

- 1-2 represents compression of the gas from pressure P_1 to pressure P_2 .
- 2-3 heating of the gas in the reactor from temperature T_2 to temperature T_3 .
- 3-4 expansion of the gas from pressure P_3 to pressure P_4 through the gas turbine.
- 4-i cooling of the gas before recompression.

Figure 4.4-14 Typical Flow Diagram with Direct Cycle.

Figure 4.4-15 T-S Diagram -- He-Cooled Direct Cycle.

TABLE 4.4-10.
HTGR REACTOR PLANT PARAMETERS⁽³¹⁾

	Peach Bottom	Fort St. Vrain	770 MW	1160 MW	1540 MW
Inlet Pressure (psia)	320	703	740	740	740
Inlet Temperature (°C)	343	406	318	318	318
Outlet Temperature (°C)	750	785	740	740	740
Net Electric Power (MW)	40	330	770	1160	1540
Net Efficiency (%)	34.8	38.8	39	39	39

TABLE 4.4-11
CONCEPTUAL DESIGN FOR A 600 MWe NUCLEAR POWER PLANT
WITH HIGH TEMPERATURE REACTOR AND HELIUM TURBINE⁽³²⁾

	Design A	Design B
Thermal Power (MW)	1333	1428
Electric Output (MW)	600	600
Number of Intercoolers	2	1
Maximum Pressure (psia)	870	870
Maximum Coolant Temperature (°C)	850	850
Minimum Temperature to Compressor (°C)	22	35
Net Efficiency	45	42

TABLE 4.4-12
THE GCFR DEMONSTRATION PLANT DESIGN⁽³³⁾

Helium Inlet Pressure (psia)	1250
Reactor Inlet Temperature (°C)	312
Reactor Outlet Temperature (°C)	542
Thermal Power (MW)	824
Electric Power (MW)	310
Thermal Efficiency (%)	37.6

TABLE 4.4-13
GCFR WITH A VANADIUM CLAD PIN CORE⁽³⁴⁾

	Oxide Fuel	Carbide Fuel
Inlet Pressure (psia)	1422	1707
Inlet Temperature (°C)	410	390
Outlet Temperature (°C)	720	700
Thermal Power (MW)	2449	2778
Electric Power (MW)	1000	962
Plant Efficiency (%)	40.8	36

Turbine Operating Data:

Expansion Ratio	2.585
Number of Intercoolers	3
Inlet Temperature to Compressor (°C)	20
Turbine Internal Efficiency (%)	90

TABLE 4.4-14
GAS-COOLED FAST BREEDER REACTOR DEMONSTRATION PLANT⁽³⁵⁾

Helium Inlet Pressure, psia	1250
Helium Inlet Temperature, °C	323
Helium Outlet Temperature, °C	550
Steam Pressure at 495°C, atm	80
Condenser Pressure, mm Hg	76
Plant Efficiency, %	36
Cladding: Stainless Steel	

TABLE 4.4-15
THE PROGRESS OF THE GBR ASSOCIATION
IN THE DESIGN AND DEVELOPMENT OF THE GAS-COOLED FBR⁽³⁶⁾

Coolant Inlet Pressure, psia	1305
Net Electric Power, MW	1200
Coolant Inlet Temperature, °C	260
Coolant Outlet Temperature, °C	560
Thermal Efficiency, %	35

The temperatures of the gas, T_1 , T_2 , T_3 , and T_4 , correspond to the points 1, 2, 3, and 4. Note also that for a frictionless flow system, $P_2 = P_3$ and $P_4 = P_1$. For a system as described above, it can be shown that

$$P_2/P_1 = (T_2/T_1)^{\frac{\gamma}{\gamma-1}} \quad [4.4-30]$$

where γ = ratio of the specific heats. The thermodynamic efficiency, η_t is given by

$$\eta_t = 1 - \left(\frac{P_2}{P_1}\right)^{\frac{1-\gamma}{\gamma}} \quad [4.4-31]$$

Thus, the ideal cycle efficiency depends only on the pressure ratio (hence, the temperature ratio). Therefore, high cycle efficiency can be obtained by using higher pressure ratios. However, since the pressure ratio and the temperature ratio are uniquely related for an isentropic process, the pressure ratio cannot be increased without correspondingly raising the temperature ratio as shown in Table 4.4-16 for the following condition:

Reactor inlet temperature = 350°C
 Turbine inlet pressure = 50, 75, 100, 125 atm
 Turbine outlet pressure = 25 atm

TABLE 4.4-16
 PERFORMANCE CHARACTERISTICS OF AN IDEAL TURBINE

Pressure Ratio	Turbine Inlet Temperature, °C	Cycle Efficiency, %
2	483	24.2
3	616	35.6
4	725	42.7
5	818	47.5

It can be observed from Table 4.4-16 that the reactor outlet (turbine inlet) temperature must be increased to achieve higher efficiency.

Pressure calculations for the Advanced Gas Reactor (AGR), HTGR, and GCFR indicate that at the system pressure (P) of 50 atm, the overall pressure drop (ΔP) across all components is of the order of 10% of the system pressure. With this value of pressure loss and assuming the compressors and the turbines are to be 85-90% efficient, the system efficiency (for $T_{\text{inlet}} = 300^\circ\text{C}$, $T_{\text{outlet}} = 650^\circ\text{C}$) is found to be ~25% at $P = 50$ atm. (Note that the points 4' and 2'

in Fig. 4.4-15 represent inefficiency of the turbine and the compressor.) Since ΔP is inversely proportional to the operating pressure, one can calculate an approximate cycle efficiency at other pressures. The cycle efficiencies, η_c , at $P = 50, 100, \text{ and } 150 \text{ atm}$ are given in Table 4.4-17. Thus, it can be observed from Table 4.4-17 that by using higher and higher pressures one achieves only marginally higher η_c .

Thermodynamic efficiency of the binary system (helium-cooled reactor with steam turbine power conversion) depends primarily on the efficiency of the steam cycle. The effect of helium pressure enters indirectly in terms of the power required to recirculate the coolant (He). The total pressure drop for the binary systems is only about 50% of the pressure drop for the direct conversion cycle. Hence, the pumping power required for a binary system is somewhat less than that of a direct conversion cycle.

The major adverse effect of high coolant pressures on blanket performance is related to the structural material fraction and coolant velocity in blanket components. For the same design stress levels, higher operating pressures require components with thicker walls. The thicker walls lead to lower breeder material fraction for a given size blanket module. This may result in lower tritium breeding ratio. Parametric studies were carried out to study the effect of pressure and coolant tube dimensions on the blanket performance. The computer program that was developed for a He-cooled lithium blanket⁽³⁸⁾ was used to carry out the parametric analyses. The blanket modules were assumed to be 3 m long, and the pressure drop across valves, fittings, bends, inlet and outlet headers was assumed to be equal to 2 velocity (coolant channel) heads. The results of the parametric studies are summarized in Tables 4.4-18 and 4.4-19.

The information presented in Table 4.4-18 shows that the pumping power becomes less as the pressure is raised. However, the material and operating costs and the associated design complexities become larger as the pressure is raised. A detailed design and cost analysis would be necessary to determine the optimum operating pressure. Table 4.4-19 summarizes how the coolant tube size affects the breeder volume fraction for a system pressure of 50 atm. For a given tube size, operation at higher pressures shows similar results. The results presented in Fig. 4.4-16 show that the maximum coolant velocities are on the order of 150 m/s. No adverse effects are expected to occur at these

velocities. It should be pointed out that the values presented in the above tables and figures are not directly applicable to designs (plate-type and breeder-in-tube type) currently under consideration. However, the general implication of the coolant pressure on the blanket performance should be valid.

TABLE 4.4-17
CYCLE EFFICIENCY FOR DIRECT CONVERSION CYCLE

Pr. (atm)	η_t (%)
50	25.4
100	30.9
150	31.4

($\Delta P = 10\%$ of P at 50 atm)
 $\Delta P \propto 1/P$

Compressor Efficiency	90%
Turbine	90%

T_{in}/T_{out}	300/650°C
------------------	-----------

TABLE 4.4-18
RATIO: PUMP. POWER/THERM. POWER, % ($LiAlO_2$)

Tube Dia. mm	Operating Pressure, psia			
	750	1000	1500	2000
6	14.1	7.38	3.12	1.73
8	5.37	2.93	1.28	0.71
10	2.78	1.54	0.68	0.39

Figure 4.4-16 Helium Velocity for LiAlO_2 Blanket.

TABLE 4.4-19
BREEDER VOLUME FRACTION (LiAlO_2)
(OPERATING PRESSURE = 50 atm)

Tube Dia., mm	Breeder Mat. Fraction, %
6	76
8	69
10	64

As pointed out in an earlier section, the high velocity coolant may produce many undesirable effects such as flow-induced vibrations, noise, erosion, and other motion-related effects. An examination of the conditions that lead to flow-induced vibrations shows that these undesirable effects occur in a flow situation where the coolant flows normal to tube banks or tube bundles. This situation does not exist in a solid breeder blanket module. However, large coolant velocities can exist in some blanket components. In order to calculate the critical velocity (V_c) above which tube failure may occur, the following values were used to calculate V_c .

	Case A	Case B
Diameter, mm	8	1000
Wall Thickness, mm	0.75	25
Length, m	3	15

The critical velocity, V_c , is given by⁽³⁹⁾

$$V_c = (\pi/L) \sqrt{EI/(\rho A)} \quad [4.4-32]$$

where L = pipe length

E = modulus of elasticity

ρ = density of fluid

A = cross sectional area

and

I = area moment of inertia.

Using coolant pressure = 50 atm and coolant temperature = 450°C, Eq. 4.4-3 gives the critical velocity as 460 m/s for Case A and 5635 m/s for Case B. Note that the sonic velocity of helium at the above operating conditions is 1585 m/s (5200 ft/s). Coolant velocities approaching the above calculated critical velocity do not exist in the blanket components. Hence, the adverse effects resulting from high coolant velocities are not a problem for the He-cooled solid breeder blanket components.

In summary, an assessment of the operating pressure on helium-cooled solid breeder blanket designs was carried out to determine whether the overall power-conversion cycle efficiency can be improved by using coolant pressures significantly higher than those considered in some of the recent blanket design studies (e.g., Blanket Comparison and Selection Study). The results of a survey of the literature and some simplified analytical calculations show that only marginal gains can be obtained by using pressure much higher than 70-80 atm. The use of high pressures, which reduces the coolant velocity, leads to minimization of the adverse effects such as flow-induced vibrations, noise, and erosion. The major benefits that can be gained from the high pressure operation are the reduced pumping power losses. However, these benefits may be overshadowed by the accompanied higher material fractions associated with high pressure operations and may result in designs with an unacceptable tritium breeding ratio. Higher cycle efficiency can be achieved by increasing the operating temperature. Unfortunately, the current material technology imposes limits on the operating temperatures which are far below the levels at which the beneficial effects become perceptible. Hence, from the results of this study it may be concluded that only marginal gains can be achieved by using helium coolant pressures significantly above 70-80 atm.

This subsection is concluded with a discussion of the gross and net power conversion efficiencies one would expect for the TPSS design. Several power cycles were analyzed in Appendix B of the BCSS interim report.⁽²⁾ The power cycles chosen for study were based on compromises necessary to satisfy coolant inlet and outlet temperatures consistent with pinch point requirements. Table 4.4-20 lists the coolant conditions, steam conditions, and efficiencies for the BCSS Li₂O/He design along with several higher performing steam cycles. The BCSS design is limited in power-conversion efficiency by the coolant inlet

and outlet temperature which in turn are limited by the HT9 first wall and the breeder maximum temperatures. Design A shows the beneficial effects of increasing the coolant outlet temperature by a mere 30°C (510°C to 540°C). Design B shows the sensitivity of the efficiencies to an increase of 50°C in coolant inlet temperature. In design C, both coolant inlet and outlet temperatures are increased, along with an increase in steam pressure.

In the TPSS design, the improvement in breeder thermal conductivity and the use of closed capsules reduces the impact of the breeder temperature limit. The use of a V-alloy results in an increase in first wall temperature limit and a decrease in first wall bulk heating. By increasing the coolant inlet temperature from the BCSS 275°C to 325°C and by increasing the coolant outlet temperature from 510°C to 650°C, it is estimated that a power conversion efficiency of ~ 44% could be realized. This is comparable to values realized for a fossil fuel plant. In addition to the improvement in power conversion efficiency, the TPSS design with lower neutron wall loading, with tubes instead of plates, and with no separate purge system has higher net efficiency. The net plant efficiency should increase to the level of ~ 36 to 38%. However, possible pressure losses caused by the tritium recovery system and the larger temperature drops across the walls of double-walled steam generator tubes would tend to lower the net power-conversion efficiency. For the purposes of this study, a net power-conversion efficiency of 40% and a net plant efficiency of 36% are assumed. More detailed analyses of a point design are required to better calculate these numbers.

4.4.5.2 Coolant Manifold Sizes and Pressure Losses

The inside diameters of the coolant inlet and outlet manifolds were calculated for the helium-cooled Li₂O design for reactor thermal power varying from 1000 MW to 3000 MW. For the range of operating parameters used in the calculation, the diameters of the coolant manifolds were found to be ~ 85 cm for helium. From these results, the approximate manifold sizes can be calculated for other operating conditions. Twelve equal blanket sectors were assumed for these calculations. The assumed operating conditions and the results are given in Table 4.4-21.

The coolant velocities assumed for the above calculations are somewhat arbitrary. As the manifold diameter is inversely proportional to the square

TABLE 4.4-20
COOLANT CONDITIONS, STEAM CONDITIONS, AND EFFICIENCIES FOR THE
BCSS $\text{Li}_2\text{O}/\text{He}$ DESIGN AND SEVERAL HIGHER PERFORMANCE DESIGNS

<u>Parameters</u>	<u>DESIGN</u>			
	<u>BCSS</u>	<u>A</u>	<u>B</u>	<u>C</u>
Blanket Thermal Power, MW	5382	5382	5382	5382
Primary Coolant Pumping Power, MW	237	<237	<237	<237
He Coolant:				
$T_{\text{in}}, ^\circ\text{C}$	275	275	325	300
$T_{\text{out}}, ^\circ\text{C}$	510	540	510	600
P, MPa	5	5	5	5
Steam:				
$T_{\text{cond}}, ^\circ\text{C}$	32	32	32	32
$T_{\text{feed}}, ^\circ\text{C}$	204	204	260	260
$T_{\text{out}}, ^\circ\text{C}$	460	490	460	538
P, MPa	8.3	8.3	12.4	16.5
Extractions	6	6	6	6
Reheats	1	1	1	0
Pinch Point, $^\circ\text{C}$	12	14	10	10
Power Conversions				
Efficiency, ^a %	39.2	40.0	41.8	42.9
Net Efficiency, ^b %	36.4	37.1	39.0	40.1
Net Plant Efficiency, %	32.0	~ 33	~ 35	~ 36

^aIncludes secondary system losses.

^bIncludes primary and secondary system losses.

root of the coolant velocity, the manifold diameter can be calculated for other velocities. Also, the same proportionality is valid for coolant ΔT . These results may be used to calculate the manifold sizes for any other coolant temperature differences. For helium, the effect of temperature on coolant density needs to be taken into account. The calculations for helium assume no pressure drop in the system. The effect of pressure drop can be accounted for because the manifold diameter is inversely proportional to the square root of the absolute pressure. For example, if there is a 5% drop in pressure across the blanket, the outlet coolant manifold sizes for helium would be increased by $1/\sqrt{0.95}$. Because the exact dependence of the physical properties on temperature and pressure has not been taken into account, the estimated manifold sizes should be considered as approximate.

Based on the coolant manifold sizes listed in Table 4.4-21, the relative pressure drop and pumping power requirements are now calculated. The relative pressure loss (ΔP) calculations for helium coolant compared with water, lithium, and lead-lithium can be performed using

$$\Delta P = \left[\frac{2 f L_e}{g_c D_e} + \frac{\sum K_i}{g_c} \right] u^2 \rho ,$$

where

- f = Fanning friction factor
- L_e = Equivalent length of flow channel
- D_e = Equivalent diameter
- K_i = Empirical loss coefficient expressed as number of velocity heads
- u = Velocity
- ρ = Density
- g_c = Conversion factor.

The loss coefficient K_i represents the pressure losses in component i , which may be valves, fittings, bends, entrance and exit effects, etc. The value of K_i varies from less than 1.0 to several hundred depending on the complexity of the flow path. The relative magnitude of pressure losses can be found from the value of $u^2 \rho / D_e$. For He at 50 atm and a velocity of 100 m/s, $u^2 \rho / D_e = 10.2$.

From an operational point of view, the pumping power requirement and not the pressure loss is the critical factor. The relative pumping power requirement is proportional to $\Delta p \times \text{volumetric flow rate } (v)$. This gives the term $(u^2 \rho / D_e) v$ which is 13724 for He and is at least a factor of 10 higher than for other coolants considered in the TPSS study. For example, the pumping power for He is ~ 25 times that for H_2O in the manifold region.

It should be noted that the relative pumping power requirements as mentioned above are for the manifold sections only. The pressure losses across the coolant manifolds are expected to be only a small part of the total pressure loss. Hence, when the total pressure losses are taken into account, the above conclusions may not be valid. Since the total pressure loss is design-dependent, no firm conclusions can be made from these preliminary manifold size calculations.

TABLE 4.4-21
ASSUMED OPERATING CONDITIONS AND MANIFOLD-
SIZING RESULTS FOR THE He-COOLED Li₂O DESIGN

<u>Parameter</u>	<u>REACTOR POWER, MW_r</u>		
	<u>1000</u>	<u>2000</u>	<u>3000</u>
Velocity, m/s	100	100	100
Inlet Temp., °C	300	300	300
Outlet Temp., °C	600, 650	600, 650	600, 650
Inlet Pressure, MPa	5.1, 7.6	5.1, 7.6	5.1, 7.6
Inlet Manifold Diameter, cm			
ΔT = 300°C	36.2	46.1	56.1
ΔT = 350°C	30.2	42.6	52.2
Outlet Manifold Diameter, cm			
P _i = 5.1 MPa			
ΔT = 300°C	49.2	69.6	85.3
P _i = 5.1 MPa			
ΔT = 350°C	46.8	66.3	81.2
P _i = 7.6 MPa			
ΔT = 300°C	40.2	56.8	69.6
P _i = 7.6 MPa			
ΔT = 350°C	38.3	54.1	66.3

4.4.5.3 Thermomechanical Problems Related to Helium-Cooled First Wall Designs

For the BCSS helium-cooled, lithium-oxide breeder blanket, analyses have been presented for the first wall which incorporate some special features to enhance the heat transfer characteristics without exceeding the mechanical design limits.^(1,2) A schematic of the first wall design is shown in Fig. 4.4-17, and the thermomechanical model used in BCSS is shown in Fig. 4.4-18. An examination of the first wall design from a heat transfer standpoint shows that the design corresponds to that of an extended surface (e.g., finned wall or finned tube). However, the complex flow path the coolant assumes cannot be analyzed with any degree of certainty. The overall flow pattern appears as two equal flow streams entering the first wall coolant region, then each turning 90° before mixing and entering the blanket plenum region. This sudden change of direction of the two flow streams is likely to lead to the presence of stagnant and recirculating regions. The gross flow pattern of the two fluid streams is shown in Fig. 4.4-19. Even for an approximate thermomechanical analysis of Region "A" (see Fig. 4.4-19), it is necessary to know whether a stagnation area exists at the center of the fluid streams before they turn 90° to move towards the plenum region and whether the flow pattern in this region oscillates with a characteristic frequency.

A review of the literature revealed no analytical solution or experimental data (except as noted below) for flow situations similar to that which exist in the BCSS design. Also, discussions were held with Prof. B. T. Cho⁽⁴⁰⁾ for a possible analytical solution. The simplest case of the current design may be thought of as the converse of the flow phenomena that occur in stagnation plane flow (Fig. 5.9 of Ref. 41). The type of flow that is occurring in the BCSS design may be considered to be similar to a mixing tee with 180° angle between the two inlet legs. With this assumption, the experimental data from the mixing tee tests at ANL^(42,43,44) may shed some light on the frequency and amplitude of flow oscillation. As the above experiments were not designed to measure the heat transfer characteristics of mixing tees, the thermal-hydraulic behavior at the stagnant region cannot be modeled. However, the frequency and the amplitude of temperature fluctuations may serve as guides for modeling the transient phenomenon of the first wall. The ANL experiments showed 20% to 80% variations in fluid temperature differences at the inlet to the mixing tee [i.e., $(\Delta T_{\text{fluctuations}}/\Delta T_{\text{inlet}}) \sim 0.20-0.80$]. In

Figure 4.4-17 Tokamak/He/ Li_2O Blanket Design.⁽¹⁾

Figure 4.4-18 Two Dimensional Bellows First Wall Configuration.

Figure 4.4-19 Schematic of Flow Stream.

addition, the ANL experiments showed the existence of fluidic "flip-flop" even when the two streams were at equal temperatures. Since no heat transfer measurements were made, it would be difficult to select the magnitude of fluctuations in the heat transfer coefficient. For this study, the variations were assumed to range between 25% and 100% of the values used in the BCSS design calculations.⁽¹⁾ The power spectral density (PSD) plots for the ANL mixing tee experiments showed the characteristic frequency to vary over a range of values (<0.1 to ~0.8 Hz) depending on the relative magnitude of the fluid velocity in the two incoming legs. When the flow velocities were approximately equal (balanced flow), the characteristic frequency was found to be ~0.4 Hz. Hence, the experimental data provide a basis for choosing a characteristic frequency.

An additional uncertainty in flow modeling is the relative value of the heat transfer coefficients in the helium channel and at the bottom plate (see Fig. 4.4-18). The heat transfer coefficient in the helium channel is expected to be much larger than that at the bottom plate. This is due to the fact that the flow is turbulent in the helium channel, whereas the fluid may be stagnant near the bottom plate. It is possible that there may be some recirculating fluid regions near the bottom plate. In accordance with assumptions used in the BCSS analysis, the heat transfer coefficient over the bottom plate was assumed to be ~1/45 of that in the helium channel.

Thus, the basis for the thermal analysis of the bottom plate may be summarized as follows:

Coolant	Helium
Coolant inlet temperature	320°C
Coolant Velocity	54 m/s
Heat transfer coefficient:	
He-channel	8599 (W/m ² -K)
Bottom plate	199 (W/m ² -K)
Surface heat flux	0.25, 0.5, 1.0 MW/m ²
Volumetric heating in structure (PCA)	55 W/cm ³
Characteristic frequency	0.1, 0.2, 0.4 Hz

A schematic of the thermal model is shown in Fig. 4.4-20. Note that the analytical model is essentially similar to that used in BCSS (see Fig. 4.4-18). A two-dimensional thermal analysis was carried out using a 96-node model. The transient temperature variations were estimated by successively varying the computation time until the node temperatures approached quasi-steady state values. For the conditions used in this analysis, it took ~20 s for the structure to approach quasi-steady state. The transient temperature response of a few selected nodes (nodes shown in Fig. 4.4-20) are plotted in Figs. 4.4-21 to 4.4-25.

From a thermomechanical point of view the peak surface temperatures and the temperature gradients are of interest. Also, the amplitude and the frequency of temperature oscillations are necessary for an understanding of the fatigue problem when considered over the life of the structure. The peak temperatures corresponding to the plasma side and the coolant side (Nodes #64 and #10) are plotted in Fig. 4.4-21 to 4.4-23 for the three frequencies corresponding to surface heat fluxes of 1.0 MW/m^2 . The structure temperatures for surface heat fluxes of 0.5 MW/m^2 and 0.25 MW/m^2 are plotted in Figs. 4.4-24 and 4.4-25. Also, the coolant temperatures are shown in the above plots. Several observations may be made from the results plotted in Figs. 4.4-21 to 4.4-23. As expected, the amplitude of fluctuations increases as the frequency decreases. The amplitude of fluctuations varies from 55°C to less than 30°C for the three characteristic frequencies. An examination of temperature fluctuations corresponding to the coolant side and the plasma side shows that the fluctuations are only slightly out of phase. When the phase shift is taken into account, the magnitude of fluctuations increases to $\sim 60^\circ\text{C}$ from 50°C for the worst case that is examined in this analysis.

The maximum surface temperature and temperature gradient for surface heat fluxes of 1.0, 0.5 and 0.25 MW/m^2 are shown in Table 4.4-22. For PCA structures, the design is unsuitable when the surface heat flux is 1 MW/m^2 , since the maximum temperature exceeds 700°C . Assuming that PCA structure can withstand temperatures up to 550°C , then PCA can be used for cases where the surface heat flux is 0.5 MW/m^2 or lower. The temperature gradients ($^\circ\text{C/mm}$) vary from 44°C/mm to 14°C/mm for the three cases. The temperature gradients near the coolant channel are higher than those near the surface (plasma side) due to inclusion of the volumetric heating in the PCA structure (assumed to be

Figure 4.4-20 Thermal Model.

Figure 4.4-21 Transient Temperature Response for Surface Heat Flux = 1.0 MW/m^2 , frequency = 0.1 Hz .

Figure 4.4-22 Transient Temperature Response for Surface Heat Flux = 1.0 MW/m^2 , frequency = 0.2 Hz.

Figure 4.4-23 Transient Temperature Response for Surface Heat Flux = 1 MW/m², frequency = 0.4 Hz.

Figure 4.4-24 Transient Temperature Response for Surface Heat Flux = 0.5 MW/m^2 , frequency = 0.4 Hz.

Figure 4.4-25 Transient Temperature Response for Surface Heat Flux = 0.25 MW/m², frequency = 0.4 Hz.

constant at 55 W/cm^3 for all cases). The coolant temperature variations shown in Figs. 4.4-21 to 4.4-25 are of no consequence to this analysis.

TABLE 4.4-22

SUMMARY OF ANALYTICAL CALCULATIONS (CHARACTERISTIC FREQUENCY = 0.4 Hz)

	Surface Heat Flux, MW/m^2		
	1.0	0.5	0.25
Maximum surface temperature, $^{\circ}\text{C}$	702	549	468
Temperature gradient, $^{\circ}\text{C/mm}$			
Near surface	44	25	14
Near coolant channel	64	39	24

The period of oscillation is consistent with the assumed characteristic frequencies. The transient temperature plots show that it takes 20 to 30 s for the structure temperatures to approach a quasi-steady state. An examination of the amplitude of temperature fluctuations show that the fluctuations are small, being less than 60°C even for the maximum surface heat flux.

Calculations were also carried out for HT-9 as the structure. The temperature responses are essentially similar to those of PCA, except that the peak temperatures are somewhat lower due to HT-9 having higher thermal conductivity. For example, the maximum surface temperature at the surface heat flux equal to 1 MW/m^2 is found to be 640°C .

One significant observation that can be made from these data is that although the heat transfer coefficients were assumed to vary four-fold, the fluctuations in the structure material temperatures are only modest. The fluctuations are less than 60°C even for the case with a surface heat flux of 1 MW/m^2 . No stress analyses have been carried out using the results of this analysis. It would be necessary to perform such analyses to determine whether thermal stresses would present any problems. The magnitude of strain corresponding to the maximum temperature fluctuations shows that the strain is only $\sim 0.12\%$ for PCA. Hence, fatigue is unlikely to present difficulties. However, this problem needs additional study.

It must be emphasized that the above calculations are based on numerous assumptions. There are not sufficient data to support either the characteristics frequency or the periodic variations in the heat transfer coefficient, as the BCSS design is significantly different from the experimental setup used in the ANL mixing tee tests. Although the assumptions used in these analyses appear to be conservative, it cannot be concluded that the results represent the worst case scenario. Only experimental data can validate the conclusions.

4.4.5.4 Thermal-Hydraulics of the TPSS Li₂O/He Design

In the previous subsections, the reactor efficiencies, coolant manifold sizing, and first-wall thermal-hydraulics were discussed for BCSS and TPSS Li₂O/He designs. Based on extrapolations of the results of sensitivity studies, it was suggested that a power-conversion efficiency (includes energy losses in the steam cycle system) of 44% and a net efficiency (includes energy losses in both helium and steam systems) of 40% could be achieved for a coolant inlet temperature of 325°C and a coolant outlet temperature of 650°C. Thermal results were also presented for PCA and HT-9 first-wall designs which (by use of scaling relationships) can be applied to the TPSS VCrTi first wall. In this subsection, the first-wall and breeder-tube temperatures are calculated and compared to temperature limits for V-15Cr-5Ti and Li₂O. Relationships are developed as a function of coolant flow rate, neutron and surface wall loading, and breeder tube dimensions to allow some iteration in the neutronics and thermal-hydraulic design parameters for use in future optimization studies. It is sufficient in this subsection to show that the proposed design is feasible and to suggest ways of optimizing the design.

The coolant mass flow rate ($\dot{M}$) is determined by the design value for the coolant temperature rise (ΔT_c) which is based on thermodynamic efficiency considerations. The relationship for ΔT_c is

$$\Delta T_c = Q_{th} / (\dot{M} C_p), \quad [4.4-33]$$

where $Q_{th} = A [Q_s + F_{em} Q_n]$, C_p = helium specific heat, Q_n is neutron wall loading, Q_s is surface heat flux, and F_{em} is the neutron energy multiplication factor. Solving for $\dot{M}$ gives

$$\dot{M} = A [Q_s + F_{em} Q_n] / (C_p \Delta T_c) \quad [4.4-34]$$

Using $C_p = 5.22 \text{ MJ/Mg} \cdot ^\circ\text{C}$, $A = 300 \text{ m}^2$, $\Delta T_c = 325^\circ\text{C}$, and $F_{em} = 1.32$ gives

$$\dot{M} = 0.177 [Q_s + 1.32 Q_n] \quad [4.4-35]$$

For the TPSS designs with a standard limiter, $Q_s \sim 0.25 Q_n$ so

$$\dot{M} = 0.278 Q_n \quad [4.4-36]$$

A simple scaling relationship for the maximum coolant temperature (T_{cl}) in the first wall region is

$$T_{cl} = T_{in} + 59(\dot{M}_B/\dot{M})(A/A_B)(0.583 Q_s + 0.0833 Q_n), \quad [4.4-37]$$

where T_{in} is the coolant inlet temperature in $^\circ\text{C}$, $\dot{M}_B$ and $\dot{M}$ are the BCSS and TPSS He mass flow rates, respectively, A_B and A are the BCSS and TPSS first wall areas, respectively, Q_s is the surface heat flux and Q_n is the neutron wall loading. Equation 4.4-37 is valid for HT-9 first wall material with no change in first wall thickness or geometry. To apply it to the TPSS V-alloy first wall, the bulk heating term ($0.0833 Q_n$) is multiplied by 0.625 based on the information in Table 4.4-5. Substituting $\dot{M}_B = 4.4 \text{ Mg/s}$, $A_B = 780 \text{ m}^2$, and $A = 300 \text{ m}^2$ into Eq. 4.4-37 gives

$$T_{cl} = T_{in} + (100/\dot{M})(0.583 Q_s + 0.0521 Q_n), \quad [4.4-38]$$

where $\dot{M}$ is in Mg/s , Q_s is in MW/m^2 , and Q_n is in MW/m^2 to give T_{cl} in $^\circ\text{C}$. One further step is taken to relate Eq. 4.4-38 to the reference TPSS $\text{Li}_2\text{O/He}$ design. Using Eq. 4.4-36 and $Q_s \sim 0.25 Q_n$ gives $T_{cl} - T_{in} = 71^\circ\text{C}$.

The analysis presented in subsection 4.4.5.3 indicated that for a surface heat flux of 1 MW/m^2 and a coolant temperature of 320°C , the maximum temperature in an HT-9 first wall is 640°C . In applying these results to a V-15Cr-5Ti first wall, it should be noted that the thermal conductivity of this material is about the same as that of the HT-9, but the bulk heating is ~ 0.625 of that for HT-9. Taking this into account, a simple scaling relationship (which does not include differences in coolant velocity or heat transfer coefficients) for the maximum first wall temperature is

$$(T_{fw})_{\max} = T_{cl} + 256 Q_s + 12.7 Q_n, \text{ } ^\circ\text{C}.$$

[4.4-39]

For the reference case of $T_{in} = 325^\circ\text{C}$, $T_{cl} = 396^\circ\text{C}$, and $Q_s \sim 0.25 Q_n$, the maximum first-wall temperature is

$$(T_{fw})_{\max} = 396 + 76.7 Q_n.$$

[4.4-40]

Using 750°C as a first-wall temperature limit at the plasma side based on He embrittlement fixes the maximum neutron wall loading at 4.6 MW/m^2 for the reference case. For higher wall loadings, the mass flow rate would have to be increased and the energy multiplication would also have to be increased to achieve the same power-conversion efficiency within the first wall material constraints.

The final step in the thermal-hydraulic analysis is to check the breeder and cladding temperatures to insure that they do not exceed design limits. For the breeder region, the maximum structure limit is taken as $T_{sl} = 650^\circ\text{C}$ based on the lowest melting temperature for vanadium oxide. The TPSS limit for Li_2O is $T_{bl} = 800^\circ\text{C}$ based on mass transfer (LiOT) considerations. However, with the closed tube design, this value can be increased.

Figure 4.4-38 of Section 4.4.7 shows the nuclear heating rate per unit volume for both the (70% Li_2O + 30% Be) breeder and the V-15Cr-5Ti structure at a neutron wall loading of 2.5 MW/m^2 . Under these conditions, the maximum breeder heating rate is 28.0 MW/m^3 and the maximum structure heating rate in the breeder region is 16.6 MW/m^3 . For design conservatism in the temperature calculation, it is assumed that both the structure and breeder nuclear heating occur in the breeder. The maximum volumetric heating rate for an 80% dense 70% Li_2O + 30% Be mixture can be expressed as

$$q'_{\max} = 12.5 Q_n, \text{ MW/m}^3.$$

[4.4-41]

Similarly, the minimum heating rate in the outboard breeder region is

$$q'_{\min} = 1.6 Q_n, \text{ MW/m}^3.$$

[4.4-42]

If an exponential power variation is assumed to simplify the analysis, then the radial variation of power in an outboard breeder tube can be expressed as

$$q''' = q'''_{\max} \exp(-\alpha\eta), \quad [4.4-43]$$

where η is a normalized length (0 for plasma side of breeder and 1 for plenum side) and $\alpha = \ln(q'''_{\max}/q'''_{\min}) = 2.06$.

The cladding surface, breeder surface, and breeder centerline temperatures are now calculated for an outboard breeder tube to see if the temperature constraints are met. First, the mass flow rate per tube ($\dot{m}$) is calculated approximately from the relationship:

$$T_{\text{out}} = T_{\text{cl}} + \frac{A_b H_b}{\dot{m} C_p} \int_0^1 q''' d\eta \quad [4.4-44]$$

where A_b is the breeder pellet cross-sectional area, H_b is the breeder height and T_{out} is the coolant outlet temperature. Substituting Eq. 4.4-43 into Eq. 4.4-44 and solving for $\dot{m}$ gives

$$\dot{m} = \frac{A_b H_b q'''_{\max}}{\alpha C_p (T_{\text{out}} - T_{\text{cl}})} [1 - \exp(-\alpha)]. \quad [4.4-45]$$

For the reference case, $A_b = 2.14 \times 10^{-4} \text{ m}^2$, $H_b = 0.4 \text{ m}$, $q'''_{\max} = 12.5 Q_n \text{ MW/m}^3$, $\alpha = 2.06$, $C_p = 5.22 \text{ MJ/Mg}\cdot\text{K}$, $T_{\text{out}} = 650^\circ\text{C}$, and $T_{\text{cl}} = 396^\circ\text{C}$ to give $\dot{m} = 0.342 Q_n$ in g/s. This expression for $\dot{m}$ is used in the following paragraph to estimate a heat transfer coefficient for calculating cladding surface temperature.

The bulk coolant temperature along a breeder tube is given by the relationship

$$T_c = T_{\text{cl}} + \frac{A_b H_b q'''_{\max}}{\dot{m} C_p \alpha} [1 - \exp(-\alpha\eta)]. \quad [4.4-46]$$

The cladding surface temperature at any nondimensional distance η from the plasma side of the breeder is found from the following approximate relationship:

$$T_s = T_c + [q'''_{\max} r_b / (2h_c)] \exp(-\alpha\eta) \quad [4.4-47]$$

where r_b is the breeder radius and h_c is the cladding-coolant heat transfer coefficient. The value of h_c depends on the Reynold's number (R_e) which in turn depends on the mass flow rate according to

$$R_e = \dot{m} d_h / (A_c \mu), \quad [4.4-48]$$

where d_h is the hydraulic diameter, A_c is the coolant cross-sectional flow area associated with one breeder tube, and μ is the helium viscosity. For 17.25 mm diameter rods arranged in a triangular array with a 18.25 mm pitch, the hydraulic diameter is 4.04 mm and the flow area is 54.7 mm². At a mean bulk coolant temperature of 523°C, the viscosity of He is 3.71×10^{-2} g/m·s giving the following expression for Re :

$$R_e = 680 Q_n. \quad [4.4-49]$$

As with the BCSS design, this is a problem area because as Q_n goes from 2.5 to 10, the Reynold's number goes from the laminar flow regime to the laminar-turbulent transition regime. For laminar flow, a value of $h_c = 1000$ W/m²·K was suggested with an enhancement factor of 3 to 4 due to entrance (20 to 30 cm) effects. The turbulent flow correlation gives $h_c \sim 2600$ W/m²·K for $R_e \sim 4500$ for the BCSS design.⁽¹⁾ The same value 2600 W/m²·K is used in the TPSS study. However, this area is highlighted for further investigation.

Substituting $h_c = 2600$ W/m²·K and $r_b = 8.25 \times 10^{-3}$ m into Eq. 4.4-47 and using the reference values for A_b , H_b , q'_{max} , $\dot{m}$, C_p , and α in Eq. 4.4-46 gives

$$T_s = 687 - (291 - 19.8 Q_n) \exp(-2.06 \eta), \text{ } ^\circ\text{C}. \quad [4.4-50]$$

For the range of neutron wall loadings $1 \leq Q_n \leq 10$ MW/m² considered in this study, the maximum value of T_s occurs at $\eta = 1$ ("shield" or "back" side of the breeder). The 750°C temperature limit based on He embrittlement of the cladding is satisfied for the reference parameters for the full range of Q_n values considered. However, recall that the first wall constraint of 750°C on the plasma side is only satisfied for $Q_n \leq 4.6$ MW/m² under the reference conditions. At this wall loading, the maximum cladding temperature is 662°C which is close to the 650°C limit based on cladding oxidation. A modest reduction

in coolant inlet temperature and/or a slight increase in mass flow rate would bring the cladding structure temperature within the specified 650°C limit with very little impact on the thermal efficiency.

The breeder surface temperature is constrained to be $\geq 410^\circ\text{C}$ based on LiOT precipitation causing excessive tritium inventory. Based on one-dimensional heat transfer considerations, this breeder surface temperature T_{bs} can be represented by

$$T_{bs} = T_s + [q'''_{\max} r_b / (2h_g)] \exp(-\alpha\eta) \quad [4.4-51]$$

where h_g is the breeder-cladding heat transfer coefficient (i.e., gap conductance). Even for $h_g = \infty$ (perfect conductance across the gap), the condition $T_{bs} \geq 410^\circ\text{C}$ is satisfied. For the BCSS assumed value of $h_g = 3000 \text{ W/m}^2\cdot\text{K}$, the breeder surface temperature under reference conditions is

$$T_{bs} = 687 - (291 - 37 Q_n) \exp(-2.06 \eta). \quad [4.4-52]$$

For $Q_n > 7.87 \text{ MW/m}^2$ the minimum breeder temperature occurs at $\eta = 1$ while for $Q_n \leq 7.87$ the minimum breeder temperature occurs at $\eta = 0$ under the assumed reference conditions.

The breeder centerline temperature was constrained to be less than 800°C in the BCSS study based on mass transfer (LiOT) concerns. With the closed capsule, the upper temperature limit can be raised to at least 900°C based on the successful FUBR-1A irradiation at this temperature. The formulation for the centerline temperature is

$$T_{bc} = T_{bs} + [q'''_{\max} r_b^2 / (4 k_b)] \exp(-\alpha\eta), \quad [4.4-53]$$

where k_b is the effective, in-reactor thermal conductivity of the breeder mixture. Using $k_b = 4.23 \text{ W/m}\cdot\text{K}$ and reference values for $q'''_{\max}$, r_b , and α in Eq. 4.4-53 gives

$$T_{bc} = 687 - (291 - 87.3 Q_n) \exp(-2.06 \eta). \quad [4.4-54]$$

For $Q_n \leq 3.33$, the maximum breeder temperature is $T_{bc} \leq 687^\circ\text{C}$ and occurs at

$\eta=1$. For higher values of Q_n , the maximum breeder temperature occurs at $\eta=0$. The 900°C breeder temperature limit is satisfied for $Q_n \leq 5.77 \text{ MW/m}^2$.

Table 4.4-23 summarizes the reference TPSS $\text{Li}_2\text{O}/\text{He}$ geometrical and operating conditions based on neutronics and thermal efficiency considerations. Figure 4.4-26 summarizes the maximum first wall, breeder cladding, and breeder centerline temperatures (as compared to limiting quantities) for these parameters, along with the minimum breeder temperature. Clearly, the first wall constraint is the most limiting condition on neutron wall loading. More detailed analysis of the effects of first wall geometry and flow rate on the maximum first wall temperature is needed before design modifications can be made to increase the neutron wall loading beyond 4.6 MW/m^2 . The breeder cladding temperature is less sensitive to neutron wall loading under the assumptions of this analysis (i.e., mass flow rate increases with neutron wall loading). The 650°C temperature limit can easily be satisfied by lowering the coolant inlet temperature a few degrees or increasing the coolant flow rate by a few percent. Finally, the maximum breeder temperature of 900°C is satisfied for $Q_n \lesssim 6 \text{ MW/m}^2$. Higher wall loadings can be used if the breeder radius is decreased.

This subsection demonstrates that the proposed reference $\text{Li}_2\text{O}/\text{He}$ design with Be multiplier and V-15Cr-5Ti is feasible from a thermal-hydraulics point of view. Relationships are also presented which allow optimization of the design for future studies. In particular, the design becomes more attractive as the Be volume fraction is increased from the nominal value of 30% used in this study.

Figure 4.4-26 Thermal-hydraulic results for the IPSS Li₂O/He design for the maximum temperatures in the first wall, $(T_{fw})_{max}$, breeder cladding, $(T_s)_{max}$, and breeder, $(T_b)_{max}$. Also shown are the minimum breeder temperature, $(T_b)_{min}$, the coolant inlet and outlet temperature, and the temperature limits. The reference conditions in Table 4.4-23 were assumed.

TABLE 4.4-23
REFERENCE PARAMETERS FOR THE TPSS Li₂O/He DESIGN BASED ON THE FIRST ITERATION
OF NEUTRONICS, THERMAL EFFICIENCY, AND THERMAL HYDRAULIC ANALYSES.

<u>Parameter</u>	<u>Value^a</u>
Surface Heat Flux, MW/m ²	0.25 Q _n
Coolant Conditions:	
Inlet Temperature, °C	325
Outlet Temperature, °C	650
Pressure, MPa	5
Mass Flow Rate, Mg/s	0.278 Q _n
Efficiencies	
Power Conversion, %	44
Net Power Conversion, %	40
Net Plant, %	36
Breeder Tube:	
Be Volume Fraction, %	30
Maximum Heating Rate, MW/m ³	12.5 Q _n
Breeder Diameter, mm	16.50
Cladding Diameter, mm	17.25
Pitch, mm	18.25
Outboard Breeder Length, mm	400
Inboard Breeder Length, mm	260

^aQ_n is the neutron wall loading in MW/m²

4.4.6 Stress Analysis

Several potential stress analysis problems relating to the BCSS $\text{Li}_2\text{O}/\text{He}$ design are analyzed in 4.4.6.1. These problems include thermal stress in the manifold channel walls, collapse of the HT-9 breeder-plate cladding, and creep of the solid breeder into the purge flow channel. The first of these is also relevant to the proposed TPSS $\text{Li}_2\text{O}/\text{He}$ design. The latter two are not issues for the proposed tube design. Stress analysis problems relating to the TPSS tube design are presented in 4.4.6.2.

4.4.6.1 Review of BCSS Stress-Related Problems

The present section is concerned with several stress-related issues in the Li_2O plate design that were not analyzed in the BCSS. The first is the thermal stress in the manifold channel walls due to a large temperature drop between adjacent channels. This problem has been analyzed in detail in Section 3.2.3c. The results show that a temperature difference of 255°C between adjacent channels that was used in the BCSS design will violate the maximum thermal stress criterion for HT-9. The other issues are related to the possible blockage of the helium purge flow channels in the solid breeder caused by either the structural collapse of the HT-9 cladding or creep of the solid breeder material itself. The analysis of the collapse of the HT-9 cladding by creep is made assuming that the solid breeder is rigid, and the analysis of the creep deformation of the solid breeder channels is made assuming that the cladding remains rigid. Both these assumptions should give conservative results.

The HT-9 cladding of the solid breeder is subjected to a coolant pressure of 5 MPa on one side, and a small pressure of 0.1 MPa in a series of small channels provided for the helium purge stream on the breeder side. Since the breeder is in a high fluence zone, a potential exists for the cladding to collapse onto the breeder because of irradiation-induced creep and block the helium purge flow channels. It is estimated that at $500\text{--}550^\circ\text{C}$ the contribution to deformation from thermal creep should be much smaller than that from irradiation-induced creep.

The analysis has been carried out by treating the cladding as a beam, either simply supported or clamped at both ends, spanning across the width of the purge flow channels. The fractional volume changes of the channels can be

shown to equal

$$\frac{\Delta V}{V_0} = - \frac{4p}{5\pi} \left(\frac{d}{h}\right)^3 A\phi t \quad [4.4-55]$$

for the simply supported case and

$$\frac{\Delta V}{V_0} = - \frac{2p}{15\pi} \left(\frac{d}{h}\right)^3 A\phi t \quad [4.4-56]$$

for the clamped case, where p is the pressure, d is the width of the purge flow channel, h is the thickness of the cladding, ϕt is the fluence, and A is the coefficient in the irradiation induced creep equation. The computed fractional volume change of the purge flow channel per unit dpa is plotted in Fig. 4.4-27. The actual volume change of the channel should be somewhere between the two lines shown in the figure. For example, if the width of the purge flow channel (d) and the thickness of the cladding (h) were 1.5 mm and 0.25 mm respectively, then in 150 dpa the reduction of purge flow volume is between 0.81% and 7.2%. The maximum stresses in the cladding corresponding to the same two boundary conditions are shown in Fig. 4.4-28 which also shows, by means of dashed lines, the allowable stresses as limited by 5% irradiation induced creep strain as well as those limited by thermal creep considerations for a life of 3×10^4 h. For the example considered earlier, if one considers the limit set by irradiation-induced creep, the maximum stress is within the allowable for the clamped edge case, but violates the limit slightly for the simply supported case. Note that even if one discards the irradiation-induced creep strain limit, the thermal creep limit itself would not permit the use of a design with d/h any larger than about 7.

The other possible mechanism by which the helium purge flow channels might become blocked is creep of the solid breeder itself. A conservative analysis of this situation was made by assuming that an axial compressive stress equal to μp , where μ is the Poisson's ratio, is generated in the breeder because the free Poisson's expansion of the breeder due to the coolant pressure p acting on the rigid cladding and transmitted to the breeder is blocked by friction. The breeder can then be modelled as a plate with edge notches, as shown in Fig. 4.4-29, subjected to a compressive stress μp . The elastic displacement at the edges of the channels is further assumed to be approximately equal to that of a plate with a central hole, i.e.,

Figure 4.4-27 Relative volume change (in per cent) of helium purge flow channels in 150 dpa due to a collapse of the HT-9 cladding by irradiation-induced creep at a coolant pressure of 5 MPa as a function of the ratio of the width of the channel and the thickness of the cladding.

Figure 4.4-28 Variation of the maximum stress in the HT-9 cladding due to a coolant pressure of 5 MPa for two boundary conditions and as a function of the ratio between the width of the channel and the thickness of the cladding. The maximum allowable pressures as determined by material strength and irradiation induced creep or thermal creep considerations are indicated by dashed lines.

Figure 4.4-29 Possible models of the solid breeder and cladding (rigid) subjected to a coolant pressure. The model on the right, where the solid breeder is fully constrained by friction, has been analyzed in the present study.

$$u_r = - \frac{\mu p d}{2E} (1 + 2 \cos \theta), \quad [4.4-57]$$

where u_r is the radial displacement, θ is the angle with respect to the applied stress direction, and E is the elastic modulus. The fractional volume change due to this displacement is given by

$$\frac{\Delta V}{V_o} = \frac{4}{\pi d^2} \int_0^{2\pi} u_r d/2 d\theta = - \frac{2\mu p}{E} . \quad [4.4-58]$$

To apply Eq. [4.4-58] to the creep case, it is noted that the creep rate equation at 500°C of a Li_2O solid breeder with a grain size of 10 μm , and with a density equal to 80% of maximum theoretical density is estimated to be

$$\dot{\epsilon}_c = 2.08 \times 10^{-12} \sigma, \text{ s}^{-1} \quad [4.4-59]$$

where σ is the applied stress. Since this equation is linear in stress, Eq. [4.4-58] can be generalized to the creep case as follows:

$$\frac{\Delta V}{V_o} = - 2\mu p \times 2.08 \times 10^{-12} t \quad [4.4-60]$$

where t is the time. Substituting $\mu = 0.5$ and $p = 5 \text{ MPa}$ into Eq. [4.4-60], it can be shown that the fractional volume change of the purge flow channels is 0.04% per 10,000 h, which is negligible. In summary, the maximum stress due to a temperature difference between adjacent channels in the manifold of the BCSS solid breeder blanket violates the allowable thermal stress criterion. Significant blockage of the helium purge flow channels by the collapse of the HT-9 cladding by irradiation-induced creep will not occur in 150 dpa if the ratio of the width of the channel to the thickness of the cladding does not exceed 7. Such a collapse by creep will be automatically excluded if the maximum stresses in the cladding are limited to the usual allowable time or fluence dependent stress. The estimated creep rate of the solid breeder is sufficiently low so that blockage of the purge flow channels by a creep of the solid breeder is extremely unlikely.

4.4.6.2 Li₂O/He Tube Design Analysis

The lifetime of the breeder cladding is limited by deformation due to irradiation-induced swelling and creep and by stress rupture considerations. For the reference design, a diametral strain of 5.8% will result in contact between adjacent breeder tubes. While ample coolant space will remain upon contact, hot spots will be induced at the contact points. As this situation should be avoided, a design limit of 5% is imposed on the tube diametral strain.

Initially, the cladding is subjected to a compressive stress due to the situation where the coolant pressure (5 MPa) is higher than the He plenum pressure (0.1 MPa). As the breeder swells and the plenum pressure increases due to helium release, the stress will eventually become tensile, and the cladding diameter will expand. Figure 4.4-30 shows the helium generation rate per unit of fuel volume as a function of normalized position along the length of the breeder in the outboard region. The He generated from both the (n,α) reaction for Li and the (n, 2n) reaction for Be are included. The average generation rate is $\dot{n}_{\text{He}} = 144 \text{ (moles/m}^3\text{)}/(\text{MW-Y/m}^2\text{)}$, where the results have been normalized to the neutron wall loading for analytical convenience.

The design approach adopted is to assume conservatively that 100% of the helium is released to the breeder plenum. Based on the FUBR-1A, closed-capsule tests,⁽⁶⁾ the release rate varies from 77% to 99% of the generation rate as the temperature is increased from nominally 500°C to 900°C. The following analysis includes the effect of gas pressure due to the helium coolant and the helium released to the plenum. Both the decrease in plenum volume with breeder swelling and breeder/cladding mechanical interaction due to fuel swelling are ignored until more information on Li₂O creep, swelling, and hot pressing become available.

Let L_b be the breeder length and L_p be the plenum length. The breeder cladding is assumed to have a mean radius of r_s and a thickness of h , where $h/r_s \ll 1$ (~0.03 for reference case). The plenum pressure can be represented as a function of first wall fluence by

$$P_p = P_{p0} + \dot{n}_{\text{He}} R T_p t (L_b/L_p), \quad [4.4-61]$$

Figure 4.4-30 Profile of helium generation rate in the outboard blanket normalized to a neutron wall loading of 1 MW/m^2 . Both the He from the (n, α) reaction in Li and the $(n, 2n)$ reaction in Be are included. The results are for a 70% Li_2O /30% Be mixture with a porosity fraction of 20%.

where R is the universal gas constant (8.32×10^{-6} MPa·m³/mole·K), T_p is plenum temperature in K, t is time or first-wall fluence in MW-Y/m², and P_{po} is the initial plenum pressure (taken as 0.1 MPa). The net pressure difference (ΔP) on the cladding is simply the difference between the plenum pressure and the coolant pressure (P_c):

$$\Delta P = (P_{po} - P_c) + \dot{n}_{He} R T_p t (L_b/L_p). \quad [4.4-62]$$

Using the thin-walled formula, the hoop stress can be expressed as:

$$\sigma_\theta = (r_s/h_s) \Delta P. \quad [4.4-63]$$

Equations 4.4-62 and 4.4-63 can be combined to determine the breeder cladding lifetime (t_ℓ) in terms of the plenum length and a stress limit (σ_ℓ) on the cladding:

$$t_\ell = [(h_s \sigma_\ell / r_s + P_c - P_{po}) / (\dot{n}_{He} R T_p)] (L_p/L_b). \quad [4.4-64]$$

In order to apply Eq. 4.4-64, or any other lifetime criteria, it is necessary to determine the temperature and fluence (in dpa) profiles along the cladding length. Figure 4.4-31 shows these profiles with the fluence (or damage) rate expressed in dpa per MW-Y/m². The shape of the curve is from the neutronics calculations (see Section 4.4-7). The results were normalized to a first-wall damage rate of 11 dpa/(MW-Y/m²). This value is higher than some previously used values because of the contribution of the titanium in the V-15Cr-5Ti alloy. While the damage rate profile has been normalized to a first-wall neutron fluence of 1 MW-Y/m², the temperature profile in Fig. 4.4-31 was obtained from Eq. 4.4-50 for the specific case of $Q_n = 2.5$ MW/m². The cladding temperature increases from ~450°C to ~650°C from the front to the back of the breeder region. For higher wall loadings and mass flow rates, the temperature at the "plasma-end" of the cladding would increase much more than at the "plenum-end" of the cladding. At a first-wall loading of 10 MW/m², the cladding temperature would vary from ~590°C to ~675°C.

Various stress and strain limits for V-15Cr-5Ti are presented in Refs. 2 (Table IV-5) and 1. A volumetric swelling limit of 5% is imposed. This corresponds to ~220 dpa at the peak swelling temperature of ~525°C. The

Figure 4.4-31 Damage rate and temperature profiles in the outboard breeder tube cladding. The damage rate is normalized to 1 MW/m² whereas the temperatures are for the specific case of 2.5 MW/m².

maximum design stress (S_m) for unirradiated material (based on 1/3 the ultimate strength) is in the range of 220 to 235 MPa for temperatures in the range of 450 to 650°C. The same values are recommended for irradiated material. Substituting 220 MPa for σ_ℓ in Eq. 4.4-64, along with reference conditions of $L_b = 0.4$ m, $L_p = 0.22$ m, $r_s = 8.5$ mm, $h_s = 0.25$ mm, $T_p = 650^\circ\text{C}$ (923 K), $P_c = 5$ MPa, $P_{po} = 0.1$ MPa, and $\dot{n}_{\text{He}} = 144$ (moles/m³)/(MW-Y/m²), gives a design lifetime of $t_\ell = 6.9$ MW-Y/m² which corresponds to a maximum damage of 76 dpa at the plasma side of the breeder. Therefore, $S_m = 220$ MPa is a more limiting condition for the breeder cladding than is $\Delta V/V_0 = 5\%$. In terms of full power years (fpy) for the reference case of $Q_n = 2.5$ MW/m², the lifetime is ~2.8 fpy.

As a final check of the design constraints, it is noted in Ref. 2 that the thermal creep strain is limited to 1%, and the sum of thermal and irradiation creep strains is limited to 5%. Actually, for breeder claddings, the total (swelling plus creep plus thermoelastic expansion) diametral strain is limited to 5% in order to avoid significant pin-to-pin contact. For the temperature and stress range of interest, radiation induced creep dominates. Based on Table IV-5 of Ref. 2, a constant stress of 165 MPa causes 5% creep strain in 100 dpa. For the breeder cladding, the hoop stress varies from -167 MPa to +220 MPa in 76 dpa. This is equivalent to a time-averaged stress of only 51 MPa for the radiation creep relationship with a creep rate proportional to $\sigma^{1.5}$. Thus, the net creep strain is <0.1%, and the criterion of $S_m = 220$ MPa is the most limiting constraint.

In summary, the limiting stress is 220 MPa which limits the plenum pressure to 11.5 MPa for a cladding radius of 8.5 mm and thickness of 0.25 mm. The exposure limit in terms of first-wall fluence units is 6.9 MW-Y/m². A higher design life can be achieved by decreasing the breeder radius and/or increasing the breeder-cladding thickness. The Be volume fraction would have to be increased with either of these changes to compensate for the increase in structure-to-breeder fraction and still maintain the desired tritium breeding ratio and energy multiplication factor.

4.4.7 Neutronics Analysis

The only solid-breeder concept being considered in the current TPSS study is a helium-cooled Li_2O blanket design with V-15Cr-5Ti alloy as its structural material. This design neutronically features (as compared to its design predecessor, the BCSS helium-cooled Li_2O design); (1) use of V-15Cr-5Ti instead of ferritic steel, HT9 as the structural material; and (2) use of Li_2O -Be sphere-pac mixture instead of Li_2O sphere-pac as the breeding material.

The neutronic issues addressed here are: (1) the impact on tritium breeding ratio (TBR) of the use of the Li_2O -Be mixture as well as the V-15Cr-5Ti structure; (2) the performance improvement on the neutron energy multiplication; and (3) the characteristic change of nuclear heating due to use of beryllium in the breeding zone. These issues also affect the design for the radiation shield which circumscribes the blanket region.

The neutronics analysis presented in this section was carried out based on the tokamak reactor model used for the BCSS helium-cooled blanket design. A vertical cross-sectional view of the model is reproduced in Fig. 4.4-32, and the system dimensions and material compositions are shown in Fig. 4.4-33. The neutron and photon transport was studied making use of a three-dimensional, continuous energy Monte Carlo code, MCNP. The associated nuclear data library has been generated from the latest ENDF/B-V version.

Figure 4.4-34 compares the TBRs for three design concepts:

- (1) HT9/ Li_2O /He (BCSS reference design)
- (2) V-15Cr-5Ti/ Li_2O /He (alteration of the structural material to V-15Cr-5Ti)
- (3) V-15Cr-5Ti/ Li_2O -Be/He (TPSS design)

One notices that the tritium breeding is enhanced by ~ 0.07 due simply to the use of V-15Cr-5Ti instead of HT9 ferritic steel. Evidently the breeding enhancement has been brought about by substantially less neutron loss in the vanadium-base structure. Regarding the use of Li_2O -Be sphere-pac mixture (both at their 80% theoretical densities) several observations can be made based on the result of Fig. 4.4-34. First, the natural-lithium content can afford maximal or near maximal TBR. This indicates that the neutron spectrum in the Li_2O -Be mixture is not excessively softened and ^7Li is making a sizable

Figure 4.4-32 A vertical cross-section of the tokamak model used for neutronic analyses.

Major Radius (cm)	413	443	445	456	468	480	486	506	700	894	914	920	965	977	999	1009	1039
	Shield	Vacuum	Manifold	Bank-2	Bank-1	First Wall	Scrape Off	Plasma		Scrape Off	First Wall	Bank-1		Bank-2	Manifold	Vacuum	Shield
	10	2	11	12	12	6	20	194	194	20	6	45		12	22	10	30

Material Compositions (vol-2)

	Inboard				Outboard			
	V15C-5Ti HT9	He	Li ₂ O/Be*		V15C-5Ti HT9	He	Li ₂ O/Be*	
First Wall	12	88	---		12	88	---	
Bank-1	8	11	81(64.8)		8	11	81(64.8)	
Bank-2	29	8	63(50.4)		29	8	63(50.4)	
Manifold	20	80	---		20	80	---	
Shield	80 (Fe1422)	20 (H ₂ O)			80 (Fe1422)	20 (H ₂ O)		

Figure 4.4-33 System description of Li₂O/Be/He/V15C-5Ti design.

Figure 4.4-34 Comparison of tritium breeding ratios for several helium-cooled Li_2O blanket designs (BCSS/STARFIRE model with $\text{H}_2\text{O}/\text{Cu}-2\text{Be}$ limiter; MCNP-ENDF/B-V; 10,000 neutron histories).

contribution to tritium production. The result appears very encouraging in terms of eliminating possible costs involved in lithium enrichment as well as undue consumption of the resource-limited lithium that would otherwise be required. The relatively hard-neutron-spectrum characteristic to these sphere-pac mixture designs makes a vivid contrast to designs which employ a separate beryllium multiplier in a heterogeneous fashion. In such a design, one always finds the optimal TBR at substantially higher ^6Li enrichment, e.g., at 60% as in the STARFIRE design.

Another observation to be made from Fig. 4.4-34 is that the use of sphere-pac mixture drastically increases the TBR. For example, the (90% Li_2O + 10% Be) design can increase the TBR of the V-15Cr-5Ti/ Li_2O design by ~ 0.075 and the TBR of the HT9/ Li_2O design by ~ 0.15 . In the case of the (70% Li_2O + 30% Be) design, the respective TBR increases come to ~ 0.2 and ~ 0.27 . In fact the attainable TBR varies almost linearly with beryllium fraction in the mixture as illustrated in Fig. 4.4-35. Evidently such a high TBR is attained by the large $\text{Be}(n,2n)$ reaction rate in the mixture. As such these blanket designs can also yield a significantly high system energy multiplication. As illustrated in Fig. 4.4-35, the fifty-fifty mixture, for instance, results in a multiplication factor of ~ 1.38 which is compared to ~ 1.17 for the BCSS helium-cooled Li_2O blanket design; $\sim 18\%$ more system thermal power in the mixture design.

The high performance blanket designs investigated here bear a great deal of potential for blanket optimization in terms of energy multiplication, tritium production, etc. For example one can design a substantially high-energy-multiplication blanket while the breeding is kept at a reasonable level. Figure 4.4-36 explores such design variations. Shown in the figure are the TBR and EMF (Energy Multiplication Factor) variations with ^6Li content in the first blanket region (45 cm outboard and 12 cm inboard) while the second blanket region (12 cm both on the inboard and outboard) contains the naturally abundant lithium in Li_2O . The mixture composition is assumed to be (10% Li_2O + 90% Be) at their 80% theoretical densities. It is clearly observable that one has a large control on achievable TBR while the EMF is maintained at a quite high level ranging from 1.6 to 1.7. As an example, for a breeding goal of $\text{TBR} = 1.2$, one requires a 1.1% depleted- ^6Li Li_2O breeder in the first blanket region in order to obtain an EMF of 1.69. According to the

Figure 4.4-35 Effect of Li₂O-Be mixture compositions on tritium breeding and system neutron energy multiplication (BCSS/STARFIRE model with H₂O/Cu-2Be limiter; Be/Li₂O = 80% TD; ⁶Li in Li₂O = 74%; MCNP/ENDF/B-V; 10,000 neutron histories).

Figure 4.4-36 Variation of TBR and EMF with depleted ^6Li content in (10% Li_2O + 90% Be) mixture in the first blanket region (BCSS/STARFIRE model with $\text{H}_2\text{O}/\text{Cu}$ -2Be limiter; $\text{Be}/\text{Li}_2\text{O} = 80\%$ TD; MCNP/ENDF/B-V; 5000 neutron histories).

power balance analysis presented elsewhere in this report, the ratio of the net electric power difference (due to different energy multiplications) to the fusion-neutron thermal power can be expressed as

$$\frac{\Delta P_{\text{net}}}{P_n} = (\eta_G - 0.01) \Delta \epsilon = 0.44 \Delta \epsilon, \quad \left(\frac{\text{MWe}}{\text{MWth}} \right) \quad [4.4-65]$$

where the η_G is the gross thermal efficiency and $\Delta \epsilon$ is the EMF difference. The η_G is assumed to be 45% in the present TPSS study. Based on this equation one finds that the system with 1.69 EMF mentioned earlier can generate an electric power of ~304 MW more than does the system with an EMF of unity, for a fusion-neutron power of 1000 MWth. Even compared to a system having an EMF of 1.2 (which was the assumption actually used in the power balance calculation), the 1.69 EMF system can generate ~216 MWe more power for the same neutron power. The power increase shown here amounts to ~40% of the net electric power (~540 MWe) calculated for this system. Namely, this system can actually result in a system power output of ~760 MWe instead of 540 MWe.

Although some problems associated with the use of depleted-lithium breeder (e.g., high burnup) must be resolved before such a concept proves viable, the calculation shown above indicates very strong economic incentive for such a design approach.

Another design alternative that one can possibly realize utilizing the high breeding margin in the Li_2O -Be mixture concept is to optimize the blanket thickness. Table 4.4-24 lists several design variants for two different sphere-pac mixtures; (90% Li_2O + 10% Be) and (70% Li_2O + 30% Be). The two reference cases, HT9/ Li_2O (Case 1) and V-15Cr-5Ti/ Li_2O (Case 2) are also shown for the sake of comparison.

When the (90% Li_2O + 10% Be) mixture is used, one can reduce either the inboard blanket to 12 cm (Case 3B) or the outboard to 45 cm (Case 3C) both for a TBR criterion of 1.20. The thickness saving amounts to 12 cm in the inboard or 13 cm in the outboard, relative to Case 1. On the other hand, when the (90% Li_2O + 30% Be) mixture is used, one can completely eliminate breeding from the inboard blanket (Case 4B). It is also possible to reduce both inboard and outboard blankets by 12 cm and 18 cm, respectively (Case 4c) to yield the same 1.2 TBR. As shown in the table, Case 4C requires only 48% of

TABLE 4.4-24
POSSIBLE DESIGN VARIATIONS*

	Case-1	Case-2	Case-3A	Case-3B	Case-3C	Case-4A	Case-4B	Case-4C
System	HT9/Li ₂ O	V/Li ₂ O	V/(90% Li ₂ O + 10% Be)			V/(70% Li ₂ O + 30% Be)		
Inboard								
Blanket (cm)	24	24	24	12	24	24	0	12
Total ^a (cm)	41	41	41	29	41	41	17	29
Outboard								
Blanket (cm)	57	57	57	57	45	57	57	39
Total ^b (cm)	85	85	85	85	73	85	85	67
TBR	1.11	1.18	1.26	1.20	1.20	1.38	1.20	1.20
Relative Li ₂ O Inventory	1.0	1.0	0.9	0.85	0.75	0.7	0.6	0.48
Inb./Outb. Net Be Thickness (cm)	--	--	1.4/3.5	0.8/3.5	1.4/2.9	4.1/10.6	0/10.6	2.3/7.5

^aInboard Total = FW (6 cm) + BLK + Plenum (11 cm).

^bOutboard Total = FW (6 cm) + BLK + Plenum (22 cm).

*Li₂O (nat. Li), Be: @ 80% T.D.

The computation is based on MCNP/ENDF/B-V with 10,000 neutron histories.

the Li_2O inventory of Case 1 which yields a TBR of only 1.11. The estimated beryllium requirement results in ~ 2.3 cm in the inboard region and ~ 7.5 cm in the outboard for Case 4C. It is important to note that the reduction of the breeding thickness shown here not only helps minimize the tritium inventory, but also enables to place a blanket/shield material which is more effective for the radiation sheilding.

After a series of neutronic and thermal hydraulic analyses, the reference system has been designed as follows:

First Wall	6 cm	12% V-15Cr-5Ti + 88% He
Blanket 1		12% V-15Cr-5Ti + 25% He + 63% ($70\% \text{Li}_2\text{O} + 30\% \text{Be}$)
inboard	24 cm	
outboard	40 cm	
Blanket 2	17 cm	95% V-15Cr-5Ti + 5% He
		outboard only
Manifold		20% V-15Cr-5Ti + 80% He
inboard	11 cm	
outboard	22 cm	

This system yields a TBR of 1.25 ($\pm 1\%$) as estimated by MCNP. The spatial dependence of tritium production profile is shown in Fig. 4.4-37 for a neutron wall load of 2.5 MW/m^2 . The maximum tritium production rate, which occurs near the outboard first wall, is estimated to be $\sim 1.15 \times 10^{13}$ triton/cc/s in the Li_2O -Be mixture. Due to the toroidal effect of the system configuration, the corresponding production rate in the inboard region is about 8% or less, resulting in $\sim 1.03 \times 10^{13}$ triton/cm³/s. Note that this estimate based on a one-dimensional analysis does not account for the MHD source shift due to the high- β plasma regime. The source shifting is expected to yield a larger poloidal variation of the tritium production profile.

The breakdown of nuclear energy deposition in this system is as follows: 17.04 MeV in the first wall/blanket, 1.48 MeV in the shield and 18.52 MeV in the whole system, resulting in an energy multiplication factor (EMF) of 1.32

Figure 4.4-37 Spatial variation of tritium production rate (BCSS/STARFIRE 1D toroidal model; ANISN/ENDF/B-IV; neutron wall load = 2.5 MW/m²).

per 14.06 MeV D-T fusion neutron. The EMF shown here is about 13% higher than that for the BCSS helium-cooled Li_2O blanket design (EMF = 1.17).

The energy deposition of 1.48 MeV in the shield is somewhat high, due to the relatively thin blanket design, particularly in the inboard region. It appears desirable and possible to recover most of the energy in the non-breeding blanket region (or so-called "hot shield" region) which circumscribes the breeding blanket. A further study needs to be carried out for more detailed system optimization.

Finally, Fig. 4.4-38 illustrates the local nuclear-heating profiles of the Li_2O -Be mixture (@ 80% T.D.) and the solid V-15Cr-5Ti structure. One notices a relatively low heating rate in the breeder mixture despite of the large neutron multiplication by beryllium. The maximum nuclear heating rate of $\sim 28 \text{ MW/m}^3$ in the Be- Li_2O mixture is about one half of the heating rate usually seen in separate-multiplier blanket designs. One also notices a very gradual variation of heating profile inside the blanket. This is due to relatively hard neutron spectrum in the present blanket design and makes a vivid contrast to the steep heating variation usually observed in separate-multiplier designs. The low heating rate in the Li_2O -Be mixture shown here suggests a great deal of potential for using this blanket design concept in the high wall-load regime.

Figure 4.4-38 Spatial variation of nuclear heating rates for $7\text{Li}_2\text{O}-3\text{Be}$ mixture and V-15Cr-5Ti structure (BCSS STARFIRE 1-D toroidal model; ANISN/ENDF/B-IV; neutron wall load = 2.5 MW/m^2).

4.4.8 Special Issues

A number of special issues have been raised throughout the body of Section 4.4. The purpose of this section is to summarize these issues in a manner consistent with the format of Table 4.4-2.

4.4.8.1 Helium Coolant

A number of thermal-hydraulic problem areas (associated with the geometry of breeder module and the use of He coolant) were highlighted in Section 4.4. With regard to cooling of the first wall, the BCSS first-wall geometry and heat-transfer coefficients were used to perform the analyses in subsections 4.4.5.3 and 4.4.5.4. The change in heat transfer coefficient with He mass flow rate needs to be explored further, both analytically and experimentally. Associated with this problem is the optimization of the first wall geometry (e.g., base thickness, fins, grooves, etc.) for each wall loading and mass flow rate. A specific problem area addressed in 4.4.5.3 is the impact of possible stagnation and oscillating flow regimes when the coolant leaves the first wall region to enter the breeder region. The analysis employed to investigate this problem contained many arbitrary assumptions. Some experimental work is warranted to justify the preliminary conclusion that the impact of the stagnation/oscillation regime is minimal.

In the breeder region, the Reynold's number varies from ~700 to ~7000 as the mass flow rate is varied proportionally with the neutron wall loading. This range of Reynold's numbers includes the laminar flow regime (with long entrance effects) and the laminar-to-turbulent flow regime. As with the BCSS Li₂O/He design, the issue of the appropriate heat transfer coefficient to use is a significant one. In particular, does the heat transfer coefficient of 2600 W/m²·K really represent a lower bound for this parameter under the TPSS conditions?

Potential thermal stress problems in the inlet/outlet plenum region of the breeder were discussed in Section 4.6 for the BCSS HT-9 design. The same problems exist for the TPSS V-alloy design. In future studies, attention needs to be focused on resolving this design problem.

4.4.8.2 Single Coolant/Purge Loop

A conceptual design for removing tritium and deuterium as well as oxygen impurities from high pressure and high temperature helium is presented in subsection 4.4.4.1. Additional calculations are required to support this design concept. In particular, the pressure drop across this system needs to be determined to allow the calculation of net power-conversion efficiency. Also, the residual T and O levels need to be established for the coolant exiting from this system. Recall that a moisture limit of $\ll 0.2$ vppm was imposed on the coolant entering the first-wall/breeder regions because of V-alloy corrosion concerns. Finally, vaporization and transport of Na at 650°C needs to be addressed.

4.4.8.3 Li₂O/Be Mixture

A number of issues have been raised regarding the mixing of Be with Li₂O. The first issue is whether or not the mixture can be hot-pressed to the desired microstructure (i.e. porosity fraction and Li₂O grain diameter). The second issue is the nature and extent of the chemical reactions which may occur during fabrication and operation. The degree to which compounds such as BeO, Li₂BeO, Li₂O $\cdot$ (BeO)_n, etc. form may have a significant impact on the effective thermal conductivity of the mixture. In the reference TPSS Li₂O/He design, the effective conductivity of the 70% Li₂O/30% Be mixture is estimated to be twice that of pure Li₂O. A simple out-of-reactor thermal conductivity experiment is recommended to resolve the fabrication and thermal performance issues. In addition, the effectiveness of dispersed Be (in the presence of He + 7 vppm D₂) in minimizing V-alloy corrosion and tritium inventory needs to be established experimentally.

4.4.8.4 V-Alloy

The problems and uncertainties associated with the use of V-alloys are many while the potential benefits of increased power-conversion efficiency and reduced cost of electricity are high. The first issue is concerned with finding the optimum V-alloy for the Li₂O/He design application. Sample calculations were performed for the V-15Cr-5Ti alloy throughout this report because of its demonstrated corrosion-resistance (due to Cr) and swelling resistance (due to Ti). However, it remains to be determined whether this

alloy has the necessary in-reactor ductility required at the fluence levels of interest. Even with the relatively good corrosion resistance of this alloy, moisture levels of less than $\ll 0.2$ vppm appear to be required to inhibit oxygen embrittlement. Considerable work remains in determining the optimum V-alloy from the perspective of fabricability, compatibility with He (+ 7 vppm D_2 + moisture) coolant and Li_2O/He breeder, swelling-resistance, and in-reactor ductility.

4.4.8.5 Sealed Tubes

The proposed sealed-tube design is attractive because it eliminates the need for a separate purge system for the breeder and concerns over Li_2O (LiOT) mass transport. However, a number of design details need to be addressed. The method of support (e.g. grid spacers) and the sizing of the breeder tube plenum need more attention. In addition, the lifetime (including hot spot factors), reliability and fabrication costs for such a large number of tubes ($\sim 10^6$) needs to be determined.

4.4.8.6 Lobed First Wall

The lobed shape was chosen in the ECSS Li_2O/He design for the first-wall part of the blanket modules to withstand the 5 MPa of He coolant pressure. Unfortunately, this geometry appears incompatible with other advanced TPSS concepts such as plasma shaping and self-pumped, whole-first-wall limiter. Some consideration should be given to other first-wall geometries which would have the desired strength and still be compatible with TPSS concepts.

4.4.8.7 W-Coated V-Alloy First Wall

As discussed in Section 4.4.4.2, tungsten was chosen as a coating for the first wall because of its extremely low permeability to hydrogen isotopes. However, the methods (e.g. plasma arc spraying, chemical vapor deposition) which may have to be used to deposit a thin ($\sim 1\mu m$) coating of W on V-alloys, the brittleness of W under irradiation, and the thermal-strain mismatch between W and V-alloys may lead to a barrier effectiveness which is much less than desired. Advanced fabrication techniques may have to be developed in order to fabricate a reasonably dense, impermeable W barrier. Even with such a barrier in place, thermal stress cracking of the W under reactor operating

conditions may reduce the barrier factor to less than the value of 500 required by design considerations. Aluminum is more compatible with V-alloy in terms of thermal strain mismatch. It would be easier to fabricate, and it would also be more effective in protecting the base material from oxygen and moisture. However, the low melting temperature of Al may present a problem during transients. More work needs to be performed to find an optimum coating material for the V-alloy first wall as the proposed design requires a significant reduction in D_2 permeation rate to the plasma from that which would be achieved with the base material.

4.4.8.8 Double-Walled Piping and Steam Generator Tubes

It is impractical to use V-alloys for the primary coolant path from the blanket to the tritium recovery system, from the recovery system to the steam generator, and from the steam generator back to the blanket. This is because of the high cost and high permeability of V-alloys as well as the strict environmental controls required. A high-temperature nickel is recommended for the coolant pathway from the blanket to the tritium recovery system. Depending on the rates of permeation of T and D through this piping system, it may be necessary to coat or oxidize the outside of these walls or to use double-walled piping. Further analysis is required in this area.

Because most of the tritium is recovered before the coolant reaches the steam generator, tritium leakage to the steam may not be a serious problem. However, in-leakage of steam to the helium coolant through failed steam generator tubes would have dire consequences for the V-alloy used in the first wall/blanket region. It is strongly recommended that double-walled tubing be used for the steam generator tubes. While the double-walled tubes would result in a higher temperature drop from the He to the steam and a decrease in power-conversion efficiency, this would be more than compensated for by the benefits from decreasing the in-leakage of steam to the He. A cost and reliability analysis is required in this area to insure that the limiting moisture level of 4×10^{-3} vppm (2×10^{-2} Pa) can be met at a reasonable cost.

4.4.9 Evaluation

The main features and parameters for the helium-cooled, lithium-oxide design with beryllium multiplier and vanadium-alloy structural material are summarized in Table 4.4-25. While the specific numbers in the table apply to a "reference" design, it should be emphasized that the purpose of this year's effort was to generate design concepts which offered the potential for significant improvements in performance as compared to previous solid breeder designs (e.g. BCSS). The establishment of the reference parameters in the table served the purpose of demonstrating the feasibility of the $\text{Li}_2\text{O}/\text{He}/\text{V-alloy}/\text{Be}$ design concept and of allowing detailed analyses development to suggest ways of improving the performance of such a blanket. The parameters in table 4.4-25 represent the results of a first iteration of the neutronics, thermal-hydraulics, and structural analyses with the materials constraints. Further iterations would generate an optimized point design.

The reference values in Table 4.4-25 are for the 2.5 MW/m^2 wall loading. The first wall and breeder temperatures are well within the materials design constraints. For a coolant outlet temperature of 650°C , the breeder cladding is within 6°C of its temperature limit. As the neutron wall loading and surface heat flux are increased, the reference design can tolerate a maximum neutron wall loading of 4.6 MW/m^2 before the 750°C first wall temperature constraint is exceeded. As the wall loading is increased, the coolant mass flow rate is increased proportionately to maintain the same coolant ΔT . Further increases in wall loading beyond 4.6 MW/m^2 may be realized by increasing the mass flow rate to values higher than 1.3 Mg/s (see the column under limiting values). However, to maintain the same coolant ΔT , the energy multiplication would have to be increased beyond 1.32 by increasing the Be volume fraction in the breeder. Conceptually, there is no problem with doing this. As discussed in 4.4.7, energy multiplication factors of 1.7 can be obtained for Be volume fractions of 90%.

In evaluating the TPSS $\text{Li}_2\text{O}/\text{He}$ design, it is useful to compare the reference and potential performance parameters to the BCSS $\text{Li}_2\text{O}/\text{He}$ design. Table 4.4-26 summarizes this comparison. The goal in designing the TPSS $\text{Li}_2\text{O}/\text{He}$ first-wall/blanket was to establish a geometry and set of materials that would achieve high power-conversion efficiency for a variety of neutron wall loadings. The TPSS reference wall loading is 2.5 MW/m^2 . With no change

TABLE 4.4-25

SUMMARY OF $\text{Li}_2\text{O}/\text{He}/\text{V-ALLOY}/\text{Be}$ DESIGN PARAMETERS FOR "REFERENCE" DESIGN

<u>Parameters</u>	<u>Reference Value</u>	<u>Limiting Value</u>
Configurational:		
Sectors	16	-
Outboard Modules/Sector	41	-
Inboard Modules/Sector	18	-
Outboard Breeder Tubes/Module	1223	-
Inboard Breeder Tubes/Module	705	-
Performance:		
Neutron Wall Loading, MW/m^2	2.5	4.6
Surface Heat flux, MW/m^2	0.6	1.2
Neutron Energy Multiplication	1.32	-
Thermal Power, MW	1170	2180
Power-Conversion Efficiency, %	~44	-
Net Power-Conversion Efficiency, %	~40	-
Net Plant Efficiency, %	~36	-
Tritium Breeding Ratio	1.25	-
Helium Coolant:		
T_{in} , $^{\circ}\text{C}$	325	-
T_{out} , $^{\circ}\text{C}$	650	-
P_{He} , MPa	5	-
P_{D_2} , Pa	35	-
$\dot{M}$, Mg/s	0.7	1.3

TABLE 4.4-25 (Continued)

<u>Parameters</u>	<u>Reference Value</u>	<u>Limiting Value</u>
V-Alloy First Wall:		
Shape	BCSS	-
Thickness	BCSS	-
$T_{\min}$, °C	325	-
$T_{\max}$, °C	588	750
V-Alloy Breeder Cladding:		
Outer Diameter, mm	17.25	-
Thickness, mm	0.25	-
$T_{\min}$, °C	446	487
$T_{\max}$, °C	656	662
Lifetime, FPY	2.8	1.5
70% Li ₂ O + 30% Be Breeder:		
Porosity Fraction, %	20	-
Pellet Diameter, mm	16.5	-
$T_{\min}$, °C	489	566
$T_{\max}$, °C	696	798
Outboard Length, m	0.40	-
Inboard Length, m	0.24	-

TABLE 4.4-26

EVALUATION OF THE TPSS REFERENCE AND POTENTIAL PERFORMANCE PARAMETERS FOR THE $\text{Li}_2\text{O}/\text{He}$ BLANKET AS COMPARED TO THE BCSS $\text{Li}_2\text{O}/\text{He}$ PERFORMANCE PARAMETERS

<u>Potential</u>	<u>BCSS</u>	<u>TPSS</u>	
		<u>Reference</u>	<u>Potential</u>
Neutron Wall Loading, MW/m^2	5.0	2.5	5.0
Surface Heat Flux, MW/m^2	1.0	0.6	1.25
Neutron Energy Multiplication (3D)	1.17	1.32	1.7
Tritium Breeding Ratio (3D)	1.11	1.25	1.2
Coolant Conditions:			
T_{in} , $^{\circ}\text{C}$	275	325	325
T_{out} , $^{\circ}\text{C}$	510	650	650
P, MPa	5	5	5
Efficiencies:			
Power Conversion, %	39.2	44	44
Net Power Conversion, %	36.4	40	40
Net Plant, %	32	36	36
Outboard Module Thickness, m	0.85	0.66	0.67
Inboard Module Thickness, m	0.41	0.41	0.29

in the materials or the configuration, the wall loading can be increased to 4.6 MW/m^2 simply by increasing the mass flow rate proportionately to maintain a coolant ΔT of 325°C . This wall loading is limited by the peak first wall temperature of 750°C at the plasma/first-wall interface. A first wall loading of 5.0 MW/m^2 with a surface heat flux of 1.25 MW/m^2 can be tolerated with higher increases to the mass flow rate and subsequent increases to the energy multiplication factor (EMF) to maintain a ΔT of 325°C . Increasing the Be volume fraction would be sufficient to increase the EMF. Beyond $\sim 5 \text{ MW/m}^2$ of neutron wall loading, a significant redesign of the first wall would be required to tolerate the high surface heat fluxes within the material temperature constraint. Thus, with regard to neutron wall loading and surface heat flux, the TPSS design is competitive with the BCSS design.

The major gains in performance of the TPSS design come in the areas of neutron energy multiplication and tritium breeding (both because of the addition of Be to the Li_2O breeder) and power conversion efficiency (because the V-alloy can withstand higher coolant outlet temperatures than the BCSS HT-9). Energy multiplication factors as high as 1.7 can be achieved without the undesirable large increase in tritium breeding ratio by optimizing the Be volume fraction, the Li-6 enrichment, and the blanket thickness. The coolant outlet temperature is basically limited to $\sim 650^\circ\text{C}$ by the breeder-cladding temperature constraint. However, this is a significant improvement over the BCSS HT-9 limited coolant-outlet temperature of 510°C . While no detailed cost estimate has been made for the TPSS $\text{Li}_2\text{O}/\text{He}$ design, the BCSS cost estimate⁽¹⁾ can be used as a guide. For the BCSS design, if the net plant efficiency was increased to 36% and the energy multiplication was increased to 1.32 with no increases in the cost of components, then a savings of $\sim 25\%$ ($\sim 940\$/\text{kWe}$) could be realized in the total capital cost. The actual savings for the TPSS $\text{Li}_2\text{O}/\text{He}$ design would be less because of the increased material and fabrication cost for the V-alloy.

Additional benefits from the TPSS design come in the area of reduced inboard and outboard thicknesses for the blanket modules. The payoff for this improvement comes from decreased power to the magnets to generate the same magnetic field in the plasma.

The major disadvantage for the proposed TPSS $\text{Li}_2\text{O}/\text{He}$ design is that the lobe shape for the first wall renders this design incompatible with the plasma

shaping and whole-first-wall, self-pumped limiter concepts. The same would be true for the BCSS $\text{Li}_2\text{O}/\text{He}$ design. In addition, a number of technical issues need to be resolved for the TPSS $\text{Li}_2\text{O}/\text{He}$ design. These are discussed throughout the report and are summarized in Section 4.4.8. The major problem areas are listed below:

1. The use of V-alloys requires that the moisture levels in the He coolant be $\ll 0.2$ vppm for the temperatures of interest. It needs to be demonstrated analytically and/or experimentally that the tritium and heat recovery systems can meet such a constraint.
2. The fabricability and in-reactor thermal and chemical performance of hot-pressed $\text{Li}_2\text{O}/\text{Be}$ pellets need to be demonstrated.
3. A V-alloy with optimum swelling, corrosion, and in-reactor ductility properties needs to be developed for fusion reactor applications. The fabricability of such an alloy needs to be demonstrated.
4. An optimum first-wall coating (coolant side) needs to be found to inhibit excessive leakage of hydrogen isotopes from the He coolant to the plasma. Problems are anticipated with the proposed tungsten coating because of the inadequacies in the current methods available for applying the W coating to the complex first wall geometry, the radiation-induced embrittlement of tungsten, and the thermal-strain mismatch of W and V-alloys.
5. A detailed cost analysis needs to be performed to insure that the gains in power generation and power-conversion efficiency are not offset by the added costs of using V-alloys and Be and by the added costs of the proposed tritium recovery and steam generator (e.g. double-walled tubes) systems.

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4.5 Self-Cooled Flibe Blanket Concept

4.5.1 Concepts and Issues

A self-cooled Flibe blanket using V-alloy as the structural material has been developed as part of ANL's effort on TPSS/Mini-MARS work. Flibe was used as the coolant for the molten salt reactor at ORNL⁽¹⁾ which was successfully operated for over 10 years. In the early stage of the fusion work, Flibe was suggested as the breeding material in a helium cooled blanket.⁽²⁾ The deficiency of the breeding potential, primarily due to the low lithium concentration, requires additional neutron multiplier which complicated the blanket design.⁽³⁾ The very low tritium solubility makes tritium containment more difficult.⁽⁴⁾ The high material melting temperature causes additional operating problems. Therefore, with the exception of work at LLNL,⁽³⁾ the work on Flibe in fusion reactor applications has basically stopped.

There are a number of attractive features for Flibe, such as:

1. Low pressure operation.
2. Inert to water and air.
3. Low activation.
4. High temperature stability.
5. Low electrical conductivity.

If these features can be incorporated into a self-cooled blanket, a simple, compact, high performance and reliable blanket may be possible.

The idea of Li-doping of Flibe was developed. If lithium can be dissolved in Flibe, the excess lithium will react with tritium to form LiT which will be dissolved in Flibe. Thus, the tritium activity can be reduced. The tritium can be recovered by electrolysis process which has been demonstrated. The addition of lithium can also reduce the fluorine activity so that the corrosion problem may be reduced.⁽⁵⁾ Interestingly, neutronic analyses conducted by E. T. Cheng⁽⁶⁾ identified that Flibe can breed with a breeding ratio of 1.2 for a TMR geometry if V-alloy or HT-9 is used as the structural material. Further analysis shows that it can breed in a tokamak geometry as well, even when including the effect of the impurity control system. Therefore, a self-cooled Flibe blanket suddenly becomes much more attractive.

More detailed analysis uncovered some attractive features and some potential problems. A highly compact and efficient primary loop can be developed with a conversion of 46%. The blanket is designed to be inherently safe for at least 4.7 MW/m^2 neutron wall loading. Since no reflector is needed, the mass utilization is very high. A similar blanket for Mini-MARS has a high mass utilization figure of 138 KWe/tonne, the highest among the four blanket candidates. A very simple blanket can be designed.

There is doubt about the lithium doping idea for tritium control. The issues cannot be resolved until experimental verification is available. However, if V-alloy is used for the structural material, a large fraction of tritium can be permeated back to the plasma. By the permeation through the first wall a tritium partial pressure of $<10^{-4}$ Torr can be maintained. If this is the case, the primary blanket tritium recovery system is combined with plasma exhaust. A highly efficient tritium clean up system will still be required to reduce the tritium permeation problem. A counter-current extraction system can be used for tritium cleanup.

A number of issues exist, some of which are material dependent and some are design dependent. The more critical issues which required further evaluation include tritium management, V/Flibe compatibility, high temperature operating procedures and power cycle optimization.

4.5.2 Design Configurations

The blanket configuration is very similar to that of the self-cooled liquid metal blanket, as shown in Fig. 4.5.2-1. Since the first wall surface heat flux is high, the cooling of the first wall becomes important. Therefore, the coolant will enter the front part of the blanket region, cool the first wall, and return through the bulk of the blanket to recover the energy from nuclear heating. Thus, the first wall coolant will have high velocity and low temperature, which provides efficient cooling to the first wall. In the bulk blanket, the nuclear heating is deposited directly in the coolant so that heat transfer is not a critical consideration.

The primary coolant system is divided into four separate loops, each one with a steam generator. For the reference design, no IHX is required because of the inertness of Flibe toward water. However, an IHX, or some form of a double-walled steam generator may be required for adequate tritium containment. The size of the steam generator is very small due to the large temperature difference between the Flibe and steam. Due to its compactness and the inertness of the Flibe, the steam generator can be placed right next to the reactor, as in the case of the HTGR. Thus, the cost of piping as well as the inventory of Flibe, can be minimized.

Figure 4.5.2-1 Flow path for self-cooled Flibe blanket.

4.5.3 Material Considerations

Flibe can be corrosive in the as received form. This is primarily due to the presence of free fluorine and TFR; the problem will be more critical if tritium is bred. Since the free energy of formation of TF is similar to that of VF_4 , VF_5 , and FeF_2 on a per g atom of F basis, it is expected that vanadium or iron will react and dissolve in the salt. Mo, W, Ni are more stable; this is the reason that the molten salt reactor used a high nickel alloy as the structural material. The free energy of formation of various fluorides have been tabulated as shown in Table 4.5.3-1.⁽¹⁾ Unfortunately, high nickel alloy cannot be used in a fusion environment due to both radiation enhanced induced embrittlement and unfavorable neutronics characteristics.

Transmutation of beryllium (as BeF_2 in Li_2BeF_4) leads to corrosion of any system metal since the disappearance of Be^{2+} is equivalent to the increase of fluorine activity by the reactions

and

The best material to reduce the fluorine activity is Li or Be due to the stability of LiF and BeF_2 , as can be seen from the free energy of formation data listed in Table 4.5.3-1. A recent experiment has shown that, after addition of Be to Flibe, the corrosion of 316SS is reduced by almost a factor of 50, as shown in Fig. 4.5.3-1.⁽⁵⁾ The addition of Be in Flibe to reduce the fluorine activity is very similar to the addition of hydrogen in water to reduce the oxygen activity and thus reduces the corrosion rate. Since the free energy of formation for vanadium fluoride is very similar to that of iron fluoride, it is expected that the addition of Be in Flibe will have a similar effect in the vanadium system as in the steel system. The actual compatibility problem of vanadium alloys in flowing Flibe with excess Be has to be experimentally measured.

The Flibe exits from the blanket at 650°C. This high temperature is very similar to that proposed for a molten salt reactor (705°C). Therefore, the technology developed for MSR is applicable. The structural material used in MSR is high nickel alloy for its high temperature properties and corrosion

resistance. Therefore, the primary loop will be fabricated by two dissimilar metals, i.e., V-alloy for the blanket and high nickel alloy for the piping. A potential problem exists for impurity transport of C, N, and O from the high nickel alloy to the blanket. It is known that V-alloy is susceptible to embrittlement by impurity pick up. The severity of the problem caused by the bi-metallic loop must be investigated. Fig. 4.5.3-2 shows the effect on the DBTT (ductile brittle transition temperature) of vanadium as a function of impurity level.⁽⁷⁾ It can be seen that a significant change of DBTT can occur with a fraction of one percent impurity.

TABLE 4.5.3-1
FREE ENERGIES OF FORMATION OF FLUORIDES

	$\Delta G_{1000^\circ K}^f$ (kcal/g-atom of fluorine)
MoF ₆ (g)	-50.2
WF ₆ (g)	-56.8
NiF ₂ (d)	-55.3
VF ₅ (g)	-58
VF ₄ (cr)	-66
HF(g)	-66.2
FeF ₂ (d)	-66.5
NbF ₅ (g)	-72.5
CrF ₂ (d)	-75.2
TaF ₅ (g)	-82.2
TiF ₄ (g)	-85.4
LiF(l)	-125.2
BeF ₂ (l)	-106.9

Figure 4.5.3-1 Weight change versus exposure time for type 316 stainless steel in LiF-BeF_2 salt at the maximum loop temperature of 650°C .

Figure 4.5.3-2 Effect of interstitial elements on the DBTT of vanadium.

4.5.4 Tritium Containment/Recovery

One of the most difficult problems associated with using Flibe is tritium management. The hydrogen isotopes dissolve in Flibe in molecular form and, therefore, obey Henry's law;

$$X = K_H P$$

in which K_H is Henry's law constant = .23 wppm/atm,⁽¹⁾ X is concentration, and P is pressure. Due to the linear dependency, the solubility of tritium in Flibe is very low at low T_2 pressure. Tritium can also exist as TF; however, excessive TF will cause a material compatibility problem. The increase of the T_2 partial pressure per pass in the blanket is .9 Torr for the Flibe, compared to 10^{-5} Torr for LiPb and 10^{-12} Torr for Li. For this reason, the tritium containment problem with the self-cooled Flibe blanket becomes a critical issue.

Different possible solutions have been studied to reduce the tritium activity for the Flibe blanket. Each of these provide a possible solution for tritium containment. However, without experimental verification, the validity of the methods is uncertain.

The first method considered is to tie up the tritium with lithium. It is known that LiT, which is relatively stable, will dissolve in the molten salt. If Be is dissolved in the salt for fluorine activity control, there may be a small but significant amount of lithium released to accomplish the dual function, i.e., reduce both the fluorine and tritium activity. When Be is dissolved in Flibe, the following reaction may occur

$$\Delta G \quad -460.02 \text{ kcal} \quad -427.6 \text{ kcal.}$$

Although the reaction is not favored by thermodynamic considerations, as indicated by ΔG values, the reaction will proceed because

$$\Delta G = -RT \ln \frac{[\text{BeF}_2]^2 [\text{Li}]^2}{[\text{Be}]}$$

$$= 32.42 \text{ kcal/mole}$$

If Be added = 0.01 and $[\text{Be}] = x$,

$$[\text{BeF}_2] = [\text{Li}] = 2(.01 - x)$$

It can be shown that $[\text{Be}] = 7.5 \times 10^{-3}$

$$\text{and } [\text{Li}] = 5 \times 10^{-3}.$$

Therefore, even though thermodynamically unfavorable, significant amount of free lithium can be released if Be is added to the salt. This free lithium is expected to tie up the excess tritium to reduce the tritium activity.

The LiT dissolved in the salt can be recovered easily by an electrolysis process, which has been demonstrated as the second half of molten salt tritium recovery process from lithium.⁸ The dissociation potential of LiT is around 1.5 volt while the dissociation potential of FLiBe is 4.7 volts. This difference in dissociation potential can be used to dissociate LiT, while the FLiBe remains stable. Such a tritium recovery system has been demonstrated experimentally.

If tritium cannot be tied up by lithium, the tritium will exist in atomic form. By using V as the structural material, tritium can permeate back to the plasma. The tritium permeation rate as a function of the partial pressure has been calculated. For a 2.5 MW/m^2 neutron wall loading blanket, a tritium partial pressure of 2×10^{-5} Torr can be maintained just by permeation. This partial pressure is still too high for the steam generator. Therefore, a tritium cleanup system is required to reduce the tritium concentration by a factor of 10. Since the amount of tritium involved in the cleanup system is small and the partial pressure is reasonably high, the cleanup system is easy to design.

An intermediate loop may also be considered to improve tritium containment. A salt to salt IHX has been developed for the MSR and can be adopted here. The impact on thermal efficiency should be small. The IHX will not only improve tritium containment, but also improve safety and reliability of the system. The cost associated with the intermediate loop has yet to be evaluated.

4.5.5 Thermal Hydraulics

The heat transfer characteristics of Flibe are fairly similar to those of water, as shown in Table 4.5.5-1. The key difference is the viscosity which is 70 times higher. Fortunately, neither heat transfer nor pressure drop depends strongly on viscosity. A typical heat transfer coefficient for Flibe is $1.5 \text{ W/cm}^2 - ^\circ\text{C}$, while not as good as that of water, is much better than that of helium. Since the pressure drop is reasonable and the volumetric heat capacity is large, the power required to circulate the coolant is small.

Since the melting temperature of the low viscosity form of the Flibe is 460°C , the blanket inlet temperature of 550°C is selected. A reasonable coolant temperature rise of 100°C is used to be consistent with the power cycle considerations. The reference case of the TPSS reactor has a thermal power of 1735 MW and fusion power of 1425 MW, with a neutron wall loading of 3.8 MW/m^2 . The surface heating load is assumed to be 76 W/cm^2 . With this set of parameters, and assuming a blanket length of 2 m, the thermal hydraulics parameters for the blanket are calculated and summarized in Table 4.5.5-2.

The high temperature and the large volumetric heat capacity results in a very compact and highly efficient power conversion system. The parameters of the power conversion system are summarized in Table 4.5.5-3. The surface area of the steam generator is only 2300 m^2 , as compared to $3.9 \times 10^4 \text{ m}^2$ for MARS. The small size is primarily due to the large pinch point ΔT of 150°C , compared to only $\sim 10^\circ\text{C}$ for MARS. The thermal power is divided into four parallel loops, with each loop handling 434 MW. The primary coolant pipe has an I.D. of 64 cm, which is a very reasonable size. The cost of the primary loop, due to its compactness and low pressure, should be very modest. The operation parameters of the primary loop are very similar to those of the molten salt reactor as indicated in Table 4.5.5-4.

TABLE 4.5.5-1
COMPARISON OF HEAT TRANSFER CHARACTERISTICS BETWEEN FLIBE AND WATER

	Li ₂ BEF ₄ (600°C)	H ₂ O (260°C)
Cp, J/G - °C	2.8	5.0
ρ, G/cm ³	1.92	0.79
μ, CP	7.5	0.11
K, W/cm - °C	0.01	0.006
ρCp, J/cm ³ - °C	5.4	4.0

At same velocity $H_{H_2O} = 2.5 H_{salt}$

$$\Delta P_{H_2O} = .5 \Delta P_{salt}$$

for $V = 500 \text{ cm/sec}$, $D = 1 \text{ cm}$

$$H = 1.5 \text{ watt/cm}^2 - ^\circ\text{C}$$

if $Q = 100 \text{ W/cm}^2$, $\Delta T = 67^\circ\text{C}$

TABLE 4.5.5-2
BLANKET THERMAL HYDRAULIC PARAMETERS

Blanket Power	1735 MW
Coolant Temperature	550/650°C
Coolant Flow Rate	2.23×10^7 kg/hr
First Wall Channel Thickness	1 cm
Blanket Thickness	60 cm
First Wall Coolant Velocity	2.1 m/sec
Blanket Coolant Velocity	.04 m/sec
First Wall Heat Transfer Coefficient	$7500 \text{ W/m}^2 - ^\circ\text{C}$
Maximum First Wall Coolant Temperature	580°C
Maximum First Wall Temperature (coolant side)	641°C
Maximum First Wall Temperature (plasma side)	750°C
Maximum First Wall Thickness	5 mm
First Wall Sputtering Life	5 FPY

TABLE 4.5.5-3
POWER CONVERSION SYSTEM PARAMETERS

Thermal Power	1735 MW
Primary Coolant Temperatuer	550/650°C
Structural Temperature	750°C
Primary Coolant Flow Rate	2.2×10^7 kg/hr
Maximum Blanket Pressure	.3 MPa
ΔT pinch point	150°C
Number of Loops	4
Diameter of Coolant Pipe	.64 m
Coolant Velocity	2.5 m/sec
Length and Diameter of SG	25 m x 1 m
Steam Generator Area	2300 m ²
Steam Condition	31 MPa, 566°C/566°C RH
Thermal Efficiency	46%

TABLE 4.5.5-4
COMPARISON OF MSR AND FUSION

	<u>MSR*</u>	<u>FUSION</u>
$T_{in}, ^\circ C$	565	550
$T_{out}, ^\circ C$	705	650
$V_{max}, m/sec$	2.6	2.5
$\Delta P_{n.t.}, KPa$	124	100
$Q''_{inx}, W/cm^2$	55	
$Q''_{sg}, W/cm^2$	36	67
Maximum Design Pressure, MPa	.5	.3
Steam Generator Size (Dm x Lm)	.5 x 22 (121 MW _e)	.8 x 25 (250 MW _e)
Gross Efficiency	46%	46%
Net Cycle Efficiency	44.5%	45.5%

*Molten Salt Reactor.

4.5.6 Neutronics

This section presents the neutronic analysis for the self-cooled Flite blanket designs. In the past, Flite blankets have been considered not viable for fusion application due to insufficient tritium-breeding capability unless the breeding is enhanced by the use of neutron multipliers. However, Flite blanket concepts have been reexamined anew in the current TPSS study because of their potential design advantages. Some of them are: (1) use of Flite as breeder and coolant as well (like liquid lithium); (2) almost no MHD effect (unlike liquid lithium); and (3) feasibility to operate at low pressure with high temperature and a high heat transfer coefficient.

The primary neutronics issues to be resolved in this section are: (1) to examine the feasibility of D-T fuel self-sufficiency in the Flite blanket designs being currently investigated, without enhanced neutron multiplication; (2) to identify the major difference from or improvement over earlier designs; and (3) to derive a feasible design window in terms of tritium breeding performance.

The Flite, Li_2BeF_4 , used in the present study has a melting point of 460°C and a density of 1.96 g/cc. The constituent atomic densities are:

$$\text{Li} = 2.388 \times 10^{-2} \text{ (atom/b-cm)}$$

$$\text{Be} = 1.194 \times 10^{-2}$$

$$\text{F} = 4.776 \times 10^{-2}$$

These densities are compared, for example, to lithium atom densities of 4.629×10^{-2} , 8.115×10^{-2} and 2.333×10^{-2} atom/b-cm in Li, Li_2O and LiAlO_2 , respectively. One notices that the lithium content in Flite is substantially lower than those in Li and Li_2O . Since the naturally abundant beryllium has an atomic density of $\sim 1.236 \times 10^{-1}$ atom/b-cm, the Flite in question neutronically resembles a mixture of a ternary-ceramic breeder such as LiAlO_2 and beryllium at about 10 vol-%. From the outset, therefore, it is expected that the attainable TBR in the Flite blanket is not likely to be too much different from that of a typical ternary-ceramic breeder design with some neutron multiplier.

Figure 4.5.6-1 shows a cross-sectional view of the computational model used for the analysis. This two-dimensional infinite cylinder model simulates

the BCSS limiter and its plenum configurations as illustrated. The limiter model which covers about 10% of the first wall area consists of three layers, viz., (1) 1 cm beryllium coating which faces the plasma region; (2) 0.65 m thick (38.5% V15Cr5Ti + 61.5% Flibe) coolant channel; and (3) 5 cm thick V15Cr5Ti heat sink layer.

The reference blanket is defined as follows:

First Wall	:	3 mm	:	100% V15Cr5Ti
Blanket	:	60 cm	:	8% V15Cr5Ti + 92% Flibe
Shield	:	30 cm	:	80% Fe1422 + 20% H ₂ O

All computations were carried out using the MCNP Monte Carlo transport technique. The associated nuclear data library is based on the ENDF/B-V nuclear file.

Figure 4.5.6-2 shows the TBR variation with ⁶Li enrichment for the reference V15Cr5Ti structure case and a case where the V15Cr5Ti structure is replaced by HT9 ferritic steel alloy. In the latter case, the limiter has been also replaced by the H₂O-cooled, Cu-2Be-heat sink, HT9 structure limiter of the type used in the BCSS study. One finds an optimal TBR at around 30% of ⁶Li enrichment, but the difference from the natural lithium (~7.4%) case is trivial. Therefore little incentive is found for employing enriched lithium in the Flibe blanket design, considering possible cost increase involved in the enrichment. However, another important design impact of ⁶Li enrichment issue is related to the required blanket thickness to attain a given TBR. Figure 4.5.6-3 examines the accumulation of TBR as a function of depth in the blanket for natural lithium and 30% ⁶Li enrichment. It is found that for a TBR goal of 1.0, the blanket thickness required differs only by ~5 cm, being increased to ~8 cm for 1.1 TBR and to ~10 cm for 1.15 TBR. Obviously, the difference becomes more significant for a high-TBR criterion as the TBR accumulation is nearly saturated. In fact, the natural lithium Flibe blanket requires a thicker outboard breeding zone by ~20 cm in order to obtain the same TBR of 1.17 that has been attained with a 60 cm inboard/outboard blanket based on 30% ⁶Li enrichment. The implication is that the ultimate choice of ⁶Li enrichment depends upon the breeding criterion to be met. As the Flibe blanket does not pose any difficulty with ⁶Li burnup, the required TBR is

Figure 4.5.6-1 A cross-sectional view of 2-D computational model for Flibe blanket analysis.

Figure 4.5.6-2 Effect on TBR of ^6Li enrichment in V15Cr5Ti-structure and HT-9-structure Flibe blanket designs (limiter based on BCSS model; MCNP/ENDF/B-V; 5,000 neutron histories).

Figure 4.5.6-3 Accumulation of tritium-bred in Flibe blanket (limiter based on BCSS model; MCNP/ENDF/B-V; 10,000 neutron histories).

perhaps the sole driving force to determine the ^6Li enrichment.

The TBR of ~ 1.15 obtained for the natural lithium Flibe with V15Cr5Ti structure appears adequate to warrant the fuel self-sufficiency for this blanket design. Evidently the reasonably high breeding ratio has been brought about by the use of V15Cr5Ti as the structural material. In fact, the breeding decreases when HT9 is used instead of V15Cr5Ti by ~ 0.03 for natural lithium and by ~ 0.05 for 30% ^6Li enrichment as shown in Fig. 4.5.6-2. This is due primarily to a larger neutron loss in HT9 than in V15Cr5Ti.

The systems examined here yield a neutron multiplication of ~ 1.25 ($\pm 1\%$). This multiplication is quite comparable to that for a compositionally similar blanket such as the helium-cooled (90% Li_2O + 10% Be) mixture design studied elsewhere in this report. However the TBR obtained for the Flibe design is substantially lower than that for such a mixture design. The underlying neutronic process is appreciable neutron loss and spectrum softening in the Flibe design due mainly to fluorine in Flibe, resulting in a significant decrease in the $^7\text{Li}(n, n'\alpha)t$ reaction rate.

The tritium breeding performance examined above is also nontrivially affected by the amount of structural material assumed. Figure 4.5.6-4 illustrates the TBR variation with structure volume fraction for the two alloy cases, via, V15Cr5Ti and HT9. The Flibe is assumed to have the naturally abundant lithium. The TBR varies from ~ 1.17 at 0% to ~ 1.08 at 20% of the V15Cr5Ti structure, by volume; approximately 8% TBR variation over 20% structure-volume variation.

The TBR variation shown does not seem overly sensitive to structure content in the neighborhood of the reference point (8% V15Cr5Ti). However the mechanical design needs to be carefully carried out in terms of determining structure fraction in the blanket because the breeding degradation is quite appreciable beyond a fraction of 10%. This is an important notion for the current Flibe blanket design in which the tritium breeding is somewhat marginal.

The impact on TBR of a large HT9 structure volume is undoubtedly more detrimental as clearly shown in Fig. 4.5.6-4. At 10% HT9, for instance, the TBR decreases ~ 1.1 , being further reduced to ~ 1.01 at 20% HT9. Note that the present design assumes a quite thin first wall; only three tenths of a centi-

Figure 4.5.6-4 Effect on TBR of structure volume fraction in V15Cr5Ti-structure and HT-9-structure Flibe blanket designs (limiter based on BCSS model; MCNP/ENDF/B-V; 5,000 neutron histories).

meter. When a thicker first wall is designed (as in many previous Flibe designs), the TBR may be decreased to below unity. It is for this reason that many of Flibe blanket designs have been ruled out of fusion application in the past, unless the breeding is substantially enhanced by multiplier assistance. Use of vanadium-base alloys as the structural material (at reasonably small volume fraction) is truly crucial to the feasibility of Flibe blankets in terms of assuring the fuel self sufficiency.

The neutronic analysis presented so far has assumed a Flibe-cooled, V15Cr5Ti-heat-sink limiter based on the BCSS (backup) limiter configuration. A few notes are in order for some alternative limiter concepts studied during the course of the present study. Table 4.5.6-1 summarizes four cases to identify the impact of limiter on TBR. These cases include: (1) no limiter; (2) the reference Flibe-cooled V15Cr5Ti limiter; (3) water-cooled Cu-2Be heat-sink limiter; and (4) self-pumped limiter. The third one above is the BCSS reference limiter design used for all the blanket designs that contain breeders/coolants other than liquid lithium. The fourth limiter design is based on the in-situ metal deposition concept. This concept eliminates all vacuum plena, vacuum pumps and pumping ports that are seen in the conventional poloidal pump-limiter. The major advantages of this concept is believed to be a substantial curtailment of the radiation shielding associated with the pumping system and reduced fuel recycle.

The TBRs in Table 4.5.6-1 calculated by MCNP reveal that all three different limiter concepts result in about the same TBR within a statistical fluctuation of $\sim \pm 1\%$. Comparing these TBRs with that of the no-limiter case, one finds no strong adverse effects on TBR, even when considering the relatively large limiter coverage ($\sim 10\%$) at the first wall that was assumed in these calculations.

As for the system nuclear heating, the reference blanket design results in a blanket energy deposition of 16.9 MeV, a shield energy deposition of 0.83 MeV and a system total of 17.7 MeV ($\pm 0.8\%$) per D-T neutron. The corresponding blanket and system energy multiplication factors (EMFs) are 1.20 and 1.26, respectively. These EMFs are quite comparable to those for the BCSS self-cooled, liquid-lithium blanket design. One notable feature of the Flibe blanket is a much steeper attenuation of local nuclear heating rate as illustrated in Fig. 4.5.6-5. This is due to the relatively soft neutron spectrum

in the Flibe blanket as mentioned earlier. Also shown in Fig. 4.5.6-5 is the local tritium production rate, which has a similar profile because the primary source of nuclear heating in Flibe lies in the ${}^6\text{Li}(n,\alpha)\text{t}$ reaction.

TABLE 4.5.6-1
IMPACT OF LIMITER DESIGNS ON TRITIUM BREEDING FOR SELF-COOLED
FLIBE BLANKET DESIGNS

	No Limiter	Flibe/V Limiter	H ₂ O/Cu-2Be Limiter	Self-Pumped Limiter
T ₆	1.05	1.02	1.04	1.05
T ₇	0.14	0.13	0.12	0.12
T ₆ + T ₇	1.19 (± 1.3%)	1.15	1.16	1.17

Figure 4.5.6-5 Spatial profiles of nuclear heating rate and tritium production rate in the reference self-cooled Flibe blanket (limiter based on BCSS model; MCNP/ENDF/B-V; 10,000 neutron histories).

4.5.7 Special Issues

High Temperature Operation

The high melting temperature of Flibe may cause special operating problems for the reactor system. A trace heating system, a coolant dump system, etc. will be required. As a rule, the coolant cannot be allowed to freeze in the blanket and piping system. Similar problems exist for Li and ^{17}Li -83Pb, however, the higher Flibe melting temperature makes this problem particularly important.

The blanket and the piping are all well insulated from the environment. The small but finite amount of after heat will keep the blanket temperature above the melting temperature of Flibe for at least 10 days. The blanket should be designed such that the coolant can be dumped readily to a dump tank when it is required. A dump system such as this is a standard requirement for any liquid system and does not represent extra cost. The Flibe can be allowed to freeze in the dump tank on which a preheating system is built in to melt the Flibe.

For a molten salt reactor system, the whole reactor is enclosed within an "oven" which keeps the reactor temperature above the melting temperature of the Flibe in case the molten salt cannot be dumped within a certain time. Similar types of facilities can be used in the fusion reactor. A set of heating elements can be placed in between the blanket and the shield. Since the blanket temperature has to be kept above 500°C all the time, a very modest heating rate of $.6 \text{ W/cm}^2$ has to be supplied. The total heating supplied is about 2 MW for the whole reactor.

For start up, the Flibe can be melted in the dump tank by a set of resistance heaters. At the same time, the blanket can be heated up using the built in heating element in between the blanket and shield, which serves the dual purpose of preheating the blanket and maintaining the temperature of the blanket. For a heating rate of 2 MW, the temperature of the blanket can be increased by 500°C in 5 hrs, which is a reasonable time for start up considerations.

Be Resources

The Flibe inventories in the blanket and primary loop are 3.2×10^5 and 2.8×10^5 kg, respectively. This represents total Be inventory of 5.5×10^4 kg, which corresponds to a 10-cm-thick equivalent of Be layer over the blanket. This inventory represents 0.075% of the estimated U.S. resource in each reactor which is quite significant. However, since Flibe can be re-used, there is no recycle problem. It is estimated that 4.5×10^4 GWe - Y of resource life is available based on U.S. resource estimates. If the world resource is considered, or a new resource is discovered, the power generation can be significantly increased.

4.5.8 Evaluation

The self cooled Flibe blanket is an attractive design due to its simplicity, low pressure operation, high allowable neutron wall loading for inherent safety, low activation, efficient thermal conversion, compact power cycle, and high mass utilization. It has the potential to become one of the most efficient blanket designs for D-T fusion, with only a reasonable extrapolation of state of art technology. However, many uncertainties exist which may impact the feasibility of the blanket, among which are:

1. Tritium containment: The thermodynamics and kinetics of interaction among lithium, Flibe, and tritium must be studied. The idea of using lithium to reduce the tritium and fluorine activities is interesting, but no experimental confirmation is available at this time. If lithium does not work as expected, Y may be dissolved in Flibe in ~ 100 ppm range.
2. Corrosion of V by Flibe: Addition of Be (or Li) to Flibe reduces the 316 ss corrosion by a factor of 100. Will similar phenomena occur to V-alloy? If it does, the chemical reaction problem between V and Flibe will be small, and the dissolution of V in Flibe may be important.
3. V/High Nickel Alloy bimetallic loop: For cost effectiveness, it has been assumed that the primary coolant piping and steam generation are made of high nickel alloy. The transport and deposition of the impurity to the V structure and its impact on the material properties has to be investigated.
4. High temperature system: The melting temperature of Flibe is 460°C. The operation problem associated with a high melting temperature material must be assessed.
5. Be resonance: The Flibe blanket uses an equivalent thickness of 10 cm Be. Its impact on the Be resource may be significant.

A reasonable size experimental program can be set to provide answers for many of the issues, the most critical of which are listed in 1 to 3.

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4.6 Special Issues

4.6.1 Safety/Afterheat

The neutronics issues to be addressed for the safety/decay afterheat analysis include:

- (1) impact of varied neutron wall loads;
- (2) difference of loss-of-flow and loss-of-coolant accidents;
- (3) impact of reflector designs, i.e., Fe1422-base design versus V15Cr5Ti-base design; and
- (4) comparative study of the three reference blanket designs, viz., the self-cooled, liquid-lithium blanket, the self-cooled Flibe blanket, and the helium-cooled, solid-Li₂O blanket.

Regarding Item (4) mentioned above, Fig. 4.6.1 describes the system dimensions and materials compositions used for the analysis. In all cases the neutron transport computation was performed by making use of a one-dimensional discrete-ordinates codes, ANISN⁽¹⁾, with the VITAMIN-C⁽²⁾ data library. The subsequent radioactivity analysis was carried out using the RACC⁽³⁾ code with the associated RACCXLIB and RACCDLIB⁽⁴⁾ data libraries.

4.6.1.1 Effect of Neutron Wall Load

It is not immediately known how neutron-induced radioactivity or decay afterheating as a response measure of the radioactivity varies with neutron wall load or integral neutron wall load. An attempt is made in this section to derive an analytical relationship between the two quantities, viz., radioactivity versus neutron wall load.

The time-dependent isotope density, N_i , during reactor operation is governed by the following equation:

$$\frac{dN_i}{dt} = \sum_j (\langle \sigma_{j \rightarrow i}^F \phi \rangle + \lambda_j b_{j \rightarrow i}) N_j - (\langle \sigma_i^D \phi \rangle + \lambda_i) N_i, \quad [4.6-1]$$

where

$$\langle \sigma \phi \rangle = \int \sigma(E) \phi(E) dE$$

(A) LI/LI/V15Cr5Ti

Plasma	Scrape Off	First Wall	Blanket	Reflector	Shield	
✓	✓	✓	40	30	30	Thickness (cm)

First Wall: 100% V15Cr5Ti
 Blanket: 5% V15Cr5Ti + 95% LI (@ natural LI)
 Reflector: 10% V15Cr5Ti + 10% LI (@ natural LI) + 80% Fe1422
 [Alternate Reflector: 90% V15Cr5Ti + 10% LI (@ natural LI)]
 Shield: 80% Fe1422 + 20% H₂O

(B) FLIBE/FLIBE/V15Cr5Ti

Plasma	Scrape Off	First Wall	Blanket	Shield	
✓	✓	✓	80	30	Thickness (cm)

First Wall: 100% V15Cr5Ti
 Blanket: 8% V15Cr5Ti + 92% FLIBE (@ natural LI)
 Shield: 80% Fe1422 + 20% H₂O

(C) Li₂O-Be/He/V15Cr5Ti

Plasma	Scrape Off	First Wall	Blanket-1	Blanket-2	Plenum	Shield	
✓	✓	✓	45	12	22	30	Thickness (cm)

First Wall: 12% V15Cr5Ti + 88% He/void
 Blanket-1: 8% V15Cr5Ti + 81% (7Li₂O-3Be) (@ 80% TD; natural LI) + 11% He
 Blanket-2: 29% V15Cr5Ti + 63% (7Li₂O-3Be) (@ 80% TD; natural LI) + 8% He
 Plenum: 20% V15Cr5Ti + 80% (7Li₂O-3Be) (@ 80% He/void
 Shield: 80% Fe1422 + 20% H₂O

Fig. 4.6-1 System dimensions and material composition of the three reference designs used for decay afterheat.

$\sigma_{j \rightarrow i}^F$: isotope-i formation neutron cross section by isotope j

σ_i^D : isotope-i destruction neutron cross section

$\phi(E)$: neutron flux

λ_i : decay constant of isotope i ($= \ln 2 / t_{1/2}$)

$b_{j \rightarrow i}$: isotope-j decay branching ratio for isotope-i production.

Since we are interested in short-half-life isotopes in terms of decay afterheating shortly after reactor shutdown, a relationship

$$\lambda_i \gg \left\langle \sigma_i^F \phi \right\rangle$$

can be considered to be a valid approximation. We also assume that isotope production by radioactive decay is much less than that by stable-isotope reactions, namely,

$$\sum_j \left\langle \sigma_{j \rightarrow i}^F \phi \right\rangle N_j^S \gg \sum_k \lambda_k b_{k \rightarrow i} N_k^R,$$

where N_j^S and N_k^R stand for stable and radioactive isotope densities, respectively. The above approximation seems valid in most of the practical cases because stable-isotope densities are generally substantially larger than radioactive-isotope densities.

The two approximations described above lead to a solution of Eq. 4.6-1:

$$N_i(t) \sim \sum_j \left\langle \sigma_{j \rightarrow i}^F \phi \right\rangle N_j^S \left[\frac{1 - e^{-\lambda_i t}}{\lambda_i} \right]. \quad [4.6-2]$$

Based on Eq. 4.6-2 one can make several important observations. They are:

- (1) The atomic number density, N_i of radioactive isotope i (or any of its responses such as radioactivity, decay afterheat, etc.), saturates with reactor operation time for $\lambda_i t \gg 1$;
- (2) The saturated radioactivity level varies linearly with the neutron flux, ϕ , or neutron wall load, i.e., $N_i(t) \propto \phi$;

- (3) Since the post-shutdown activation level is considered to be proportional to the activation level at shutdown, the post-shutdown activation level also varies linearly with neutron wall load during operation.

On the other hand, for $\lambda_1 t \lesssim 1$, that is, in the case of relatively long-lived isotopes, the solution of Eq. 4.6-1 can be approximated as follows:

$$N_1(t) \approx \frac{\phi}{1 - \exp[-(\lambda_1 + \sigma_1^D \phi)t]} .$$

The implication is that the long-lived isotope activation varies nonlinearly with neutron wall load and is to be higher than what is expected from the wall-load linearity.

Figures 4.6-2 through 4.6-4 illustrate the time-dependent behavior of decay afterheat in the first walls of the three reference blanket systems. The time-dependent variations shown appear to justify the observations made above. Namely, up to ~ 1000 s after shutdown the decay heat varies linearly with wall load. However, at ~ 1 h, for instance, the 5.0 MW/m^2 case yields a heating rate three times higher than the 2.5-MW/m^2 case due to the gradual contributions from long-lived isotopes. The difference becomes even larger by a factor of five or more beyond ~ 1 month after shutdown and thereafter.

4.6.1.2 LOFA and LOCA

The difference between LOFA and LOCA can be viewed as a compositional difference in constituting blanket materials in terms of the neutronic impact. In this section an attempt is made to derive generic information on decay afterheat based on the theory of interactions between decay gamma rays and blanket materials.

Figure 4.6-5 shows a typical spectrum of decay gamma emitted in the lithium-cooled blanket and reflector. One finds a series of gamma rays decaying from ^{24}Na , ^{51}Cr , ^{46}Sc , ^{47}Sc , ^{48}Sc , etc., produced in the V15Cr5Ti alloy structure. In addition to these, the gamma-ray peaks due to the ^{56}Mn decay are clearly observable in the reflector region where Fe-1422 is used. Typical energy of the decay gamma source ranges from several tens of keV to a couple of MeV. According to Ref. 5, the most dominant mode of gamma-ray interaction

Fig. 4.6-2. Effect of neutron wall load on decay heating in first wall; Li/Li/V15Cr5Ti design.

Fig. 4.6-3. Effect of neutron wall load on decay heating in first wall; Flibe/Flibe/V15Cr5Ti design.

Fig. 4.6-4. Effect of neutron wall load on decay heating in first wall; $\text{Li}_2\text{O}-\text{Be}/\text{He}/\text{V15Cr5Ti}$ design.

Fig. 4.6-5. Decay-gamma spectrum: $\text{Li}/\text{Li}/\text{V15Cr5Ti}$ with Fe-1422 reflector systems.

with materials which have atomic Z-numbers in the neighborhood of ~25 (corresponding to iron) is the Compton process. The effectiveness of this process is considered to be proportional to the atomic Z-number of interacting material. In actuality, the decay afterheating is, to some degree, contributed by interactions other than the Compton scattering, such as the photoelectric process and the pair-production process that have a substantially stronger Z-dependence. For example, in the case of the photoelectric effect, the interaction varies with the fifth power of the atomic Z-number. The implication is that the heating-rate estimates based on the Compton process provide the lower bound of the effectiveness of the structural material (which has the highest Z among the blanket materials under consideration.)

Table 4.6-1 has been derived based on this observation, to indicate the effectiveness of the Compton process for the candidate blanket materials. A mixture of 70% Li_2O + 30% beryllium (at its 80% theoretical density) is also included in this table which is to be used in the helium-cooled blanket design.

TABLE 4.6-1
EFFECTIVENESS OF THE COMPTON PROCESS

Material	Number Density @ 100% T.D. (atom/b-cm)	Effective Atomic Number	Effectiveness of the Compton Process
Lithium	0.0463	3.00	0.139
Flibe	0.0836	6.57	0.549
Li_2O	0.1217	4.67	0.568
Beryllium	0.1236	4.00	0.494
$[\text{7Li}_2\text{O}-3\text{Be}]^a$	0.0978	4.47	0.437
V15Cr5Ti	0.0710	23.11	1.64
Fel422	0.0859	25.85	2.22

^a@ 80% T.D.

According to Table 4.6-1, the specific afterheating (i.e., heating in unit material volume) of the V15Cr5Ti structure should be at least factors of ~10, ~3, and ~4 more than those in lithium, Flibe, and $7\text{Li}_2\text{O}-3\text{Be}$, respectively. The results of the actual heating analysis performed for the respective LOFA cases, which are presented in Tables 4.6-2 through 4.6-4, are very

TABLE 4.6-2
 [Li/V15Cr5Ti] LOFA/SPECIFIC DECAY AFTERHEAT (MW/m³)
 @ 2.5 MW-4 YR/m² WALL LOAD

REGION	0	1 MIN	10 MIN	1 HR	TIME AFTER SHUTDOWN 6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[V15CR5Ti]										
FW	4.45E-01	3.90E-01	1.67E-01	9.29E-02	7.85E-02	6.07E-02	2.05E-02	1.41E-02	8.64E-03	5.53E-03
BLK1	3.49E-01	3.04E-01	1.30E-01	7.26E-02	5.79E-02	4.36E-02	1.48E-02	1.02E-02	6.21E-03	3.96E-03
BLK2	2.73E-01	2.38E-01	1.00E-01	5.63E-02	4.02E-02	2.87E-02	9.93E-03	7.01E-03	4.28E-03	2.72E-03
BLK3	2.34E-01	2.04E-01	8.88E-02	5.11E-02	3.10E-02	2.01E-02	7.55E-03	5.53E-03	3.43E-03	2.18E-03
BLK4	2.18E-01	1.93E-01	9.13E-02	5.60E-02	2.74E-02	1.48E-02	6.61E-03	5.19E-03	3.30E-03	2.12E-03
REF1	2.21E-01	2.00E-01	1.19E-01	8.18E-02	2.78E-02	8.41E-03	6.46E-03	5.83E-03	3.88E-03	2.52E-03
REF2	1.30E-01	1.18E-01	7.31E-02	5.07E-02	1.47E-02	2.17E-03	1.90E-03	1.75E-03	1.17E-03	7.55E-04
REF3	6.83E-02	6.25E-02	3.87E-02	2.68E-02	7.31E-03	5.32E-04	4.45E-04	4.09E-04	2.65E-04	1.67E-04
[LI]										
BLK1	2.73E-02	1.05E-02	6.27E-03	4.87E-03	3.88E-03	2.93E-03	1.02E-03	7.24E-04	4.55E-04	2.93E-04
BLK2	1.77E-02	8.30E-03	4.95E-03	3.76E-03	2.67E-03	1.90E-03	6.70E-04	4.82E-04	3.02E-04	1.93E-04
BLK3	1.33E-02	7.36E-03	4.59E-03	3.46E-03	2.07E-03	1.32E-03	5.01E-04	3.73E-04	2.36E-04	1.51E-04
BLK4	1.14E-02	7.40E-03	5.14E-03	3.93E-03	1.87E-03	9.87E-04	4.45E-04	3.53E-04	2.28E-04	1.47E-04
REF1	1.10E-02	8.90E-03	7.82E-03	6.22E-03	2.08E-03	6.03E-04	4.71E-04	4.27E-04	2.85E-04	1.86E-04
REF2	6.31E-03	5.40E-03	4.85E-03	3.86E-03	1.11E-03	1.54E-04	1.36E-04	1.26E-04	8.45E-05	5.30E-05
REF3	3.40E-03	2.85E-03	2.59E-03	2.04E-03	5.55E-04	3.71E-05	3.11E-05	2.88E-05	1.89E-05	1.22E-05
[FE1422]										
REF1	2.52E-01	2.46E-01	2.21E-01	1.76E-01	5.48E-02	1.20E-02	9.32E-03	8.42E-03	5.56E-03	3.60E-03
REF2	1.46E-01	1.43E-01	1.32E-01	1.05E-01	2.94E-02	3.12E-03	2.72E-03	2.51E-03	1.67E-03	1.08E-03
REF3	7.53E-02	7.41E-02	6.84E-02	5.42E-02	1.46E-02	7.65E-04	6.21E-04	5.72E-04	3.73E-04	2.40E-04
NUCLEAR HEATING(MW/M**3) DURING OPERATION										
MAT.	FW	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3		
V*	1.26E+01	9.91E+00	7.31E+00	5.66E+00	4.59E+00	2.79E+00	1.15E+00	5.28E-01		
L*		8.18E+00	5.59E+00	4.06E+00	3.13E+00	2.29E+00	1.23E+00	7.76E-01		
FE						4.00E+00	1.62E+00	7.30E-01		

TABLE 4.6-3

[V15Cr5Ti/FLIBE] LOFA/SPECIFIC DECAY AFTERHEAT (MW/m³)
@ 2.5 MW-4 YR/m² WALL LOAD

REGION	0	1 MIN	10 MIN	1 HR	TIME AFTER SHUTDOWN 6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[V15Cr5Ti]										
FH	1.51E+00	7.79E-01	2.80E-01	1.22E-01	5.66E-02	3.68E-02	1.18E-02	7.80E-03	4.60E-03	2.88E-03
BLK1	1.39E+00	6.57E-01	2.41E-01	1.09E-01	3.91E-02	2.18E-02	6.94E-03	4.56E-03	2.63E-03	1.62E-03
BLK2	9.23E-01	4.22E-01	1.35E-01	5.32E-02	1.71E-02	8.72E-03	2.78E-03	1.83E-03	1.01E-03	6.02E-04
BLK3	5.34E-01	2.41E-01	6.78E-02	2.13E-02	6.87E-03	3.52E-03	1.17E-03	7.85E-04	4.15E-04	2.35E-04
BLK4	2.86E-01	1.26E-01	3.23E-02	8.14E-03	2.70E-03	1.42E-03	5.08E-04	3.52E-04	1.78E-04	9.57E-05
BLK5	1.45E-01	6.16E-02	1.48E-02	3.06E-03	1.05E-03	5.76E-04	2.23E-04	1.59E-04	7.78E-05	3.97E-05
BLK6	7.17E-02	2.85E-02	6.57E-03	1.17E-03	4.17E-04	2.32E-04	9.73E-05	7.15E-05	3.39E-05	1.66E-05
BLK7	3.48E-02	1.27E-02	2.96E-03	5.63E-04	1.95E-04	9.36E-05	4.28E-05	3.23E-05	1.52E-05	7.29E-06
BLK8	1.72E-02	5.96E-03	1.75E-03	6.31E-04	1.87E-04	4.17E-05	2.22E-05	1.77E-05	8.76E-06	4.51E-06
[FLIBE]										
BLK1	4.29E-01	6.56E-02	3.39E-02	2.30E-02	6.72E-03	3.13E-03	1.04E-03	7.25E-04	4.41E-04	2.79E-04
BLK2	2.21E-01	3.23E-02	1.50E-02	9.51E-03	2.73E-03	1.24E-03	4.00E-04	2.71E-04	1.59E-04	9.75E-05
BLK3	1.08E-01	1.59E-02	6.29E-03	3.59E-03	1.06E-03	4.96E-04	1.62E-04	1.10E-04	6.18E-05	3.66E-05
BLK4	5.16E-02	7.78E-03	2.66E-03	1.33E-03	4.05E-04	1.98E-04	6.81E-05	4.73E-05	2.55E-05	1.44E-05
BLK5	2.43E-02	3.72E-03	1.13E-03	4.89E-04	1.55E-04	7.91E-05	2.92E-05	2.08E-05	1.08E-05	5.82E-06
BLK6	1.14E-02	1.74E-03	4.86E-04	1.85E-04	6.08E-05	3.15E-05	1.26E-05	9.17E-06	4.60E-06	2.39E-06
BLK7	5.40E-03	8.07E-04	2.29E-04	8.79E-05	2.86E-05	1.27E-05	5.49E-06	4.13E-06	2.03E-06	1.03E-06
BLK8	2.62E-03	4.32E-04	1.80E-04	1.03E-04	2.97E-05	5.71E-06	2.93E-06	2.33E-06	1.21E-06	6.51E-07

NUCLEAR HEATING (MW/M**3) DURING OPERATION

MAT.	FH	BLK1	BLK2	BLK3	BLK4	BLK5	BLK6	BLK7	BLK8
V-	1.41E+01	1.08E+01	5.69E+00	2.83E+00	1.36E+00	6.39E-01	2.98E-01	1.42E-01	7.46E-02
FLIBE		1.86E+01	7.81E+00	3.31E+00	1.37E+00	5.59E-01	2.26E-01	9.11E-02	3.74E-02

TABLE 4.6-4

[Li/20/V/He/Be] LOCA/SPECIFIC DECAY AFTERHEAT (MW/m³)
@ 2.5 MW-4 YR/m² WALL LOAD

REGION	C	1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[VISCOSTI]										
FW	9.93E-01	7.48E-01	2.17E-01	7.29E-02	6.78E-02	5.45E-02	1.84E-02	1.26E-02	7.64E-03	4.85E-03
BLK1	8.19E-01	5.89E-01	1.56E-01	4.45E-02	4.13E-02	3.32E-02	1.11E-02	7.58E-03	4.51E-03	2.84E-03
BLK2	5.51E-01	3.92E-01	9.35E-02	2.01E-02	1.86E-02	1.49E-02	5.00E-03	3.43E-03	2.00E-03	1.24E-03
BLK3	3.34E-01	2.38E-01	5.39E-02	9.44E-03	8.73E-03	6.99E-03	2.40E-03	1.67E-03	9.67E-04	5.88E-04
BLK4	1.87E-01	1.32E-01	2.92E-02	6.48E-03	4.13E-03	3.31E-03	1.18E-03	8.41E-04	4.82E-04	2.88E-04
BLK5	1.01E-01	7.02E-02	1.52E-02	2.16E-03	1.97E-03	1.58E-03	5.91E-04	4.29E-04	2.43E-04	1.43E-04
BLK6	5.55E-02	3.79E-02	8.70E-03	1.05E-03	9.35E-04	7.47E-04	2.98E-04	2.21E-04	1.22E-04	6.96E-05
BLK7	3.21E-02	2.18E-02	4.69E-03	6.33E-04	5.03E-04	3.85E-04	1.56E-04	1.26E-04	6.65E-05	3.64E-05
PLWH	2.22E-02	1.61E-02	3.70E-03	6.91E-04	3.74E-04	2.27E-04	1.14E-04	8.87E-05	4.23E-05	2.06E-05
[7LI20-38E]										
BLK1	1.76E-01	3.50E-02	1.25E-02	6.93E-03	6.44E-03	5.16E-03	1.78E-03	1.27E-03	7.84E-04	5.00E-04
BLK2	8.43E-02	2.15E-02	6.68E-03	3.09E-03	2.85E-03	2.28E-03	7.70E-04	5.41E-04	3.29E-04	2.07E-04
BLK3	4.24E-02	1.35E-02	3.76E-03	1.44E-03	1.33E-03	1.06E-03	3.62E-04	2.57E-04	1.54E-04	9.57E-05
BLK4	2.18E-02	8.09E-03	2.11E-03	6.84E-04	6.25E-04	4.98E-04	1.76E-04	1.27E-04	7.54E-05	4.62E-05
BLK5	1.16E-02	4.89E-03	1.20E-03	3.31E-04	2.99E-04	2.39E-04	8.83E-05	6.46E-05	3.79E-05	2.29E-05
BLK6	6.87E-03	3.38E-03	7.85E-04	1.72E-04	1.51E-04	1.20E-04	4.72E-05	3.52E-05	2.00E-05	1.16E-05
BLK7	4.37E-03	2.44E-03	5.55E-04	1.06E-04	8.35E-05	6.35E-05	2.71E-05	2.06E-05	1.11E-05	6.20E-06

NUCLEAR HEATING(MW/M²*3) DURING OPERATION

HAT.

FW

BLK1

BLK2

BLK3

BLK4

BLK5

BLK6

BLK7

PLWH

7LI20-38E

V=

1.33E+01

9.73E+00

5.78E+00

3.44E+00

2.01E+00

1.23E+00

7.79E-01

5.24E-01

4.60E-01

3.81E-01

consistent with the observation made above. Note that the helium-cooled $7\text{Li}_2\text{O}-3\text{Be}$ blanket neutronically makes no difference between LOFA and LOCA. When the decay afterheat is caused predominantly by the Compton process, the volume-integrated heat generations in individual materials (for LOFA) should relatively compare as shown in Table 4.6-5. One can notice that the breeder materials are likely to be the major heat sink in the case of LOFA (and also LOCA for the helium-cooled blanket). This is due simply to the relatively large breeder volumes in the blankets. The volume-integrated heating rates actually calculated and presented in Tables 4.6-6 through 4.6-8 again appear to prove the quantitative heating trend characterized in Table 4.6-5.

TABLE 4.6-5
A COMPARISON OF RELATIVE DECAY HEATING RATES
OF BLANKET MATERIALS FOR LOFA

Blanket Materials	Relative Specific Heating	Density Factor in Reference Blanket	Relative Total Volume Heating in Blanket
Lithium	0.085	0.95	≤ 1.615 (62%)
V15Cr5Ti	1.0	0.05	1.0 (38%)
Fe1422	1.354	---	---
Fl1be	0.335	0.92	≤ 3.853 (79%)
V15Cr5Ti	1.0	0.08	1.0 (21%)
[$7\text{Li}_2\text{O}-3\text{Be}$]	0.266	0.81	≤ 2.698 (73%)
V15Cr5Ti	1.0	0.08	1.0 (27%)

There exists a substantial difference in the neutronic blanket performance between LOCA and LOFA environments. In the case of LOCA, the first-wall and blanket regions are likely to become quite transparent against the decay gammas once the coolant/breeder region has been voided. This is due to the fact that the structural material occupies only a small portion of the first-wall and blanket regions. In consequence, the overall blanket performance for LOCA is determined not only by the blanket itself but by components surrounding it such as the reflector and shield.

Table 4.6-6. [Li/V15Cr5Ti/Fel422] LOFA/Volume-Integrated Decay Afterheat (MW/m)
@ 2.5 MW-4 yr/m² Wall Load

REGION	0	TIME AFTER SHUTDOWN							1 MO	6 MO	1 YR
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK				
[V15CR5Ti]											
FH	6.00E-02	5.26E-02	2.25E-02	1.25E-02	1.06E-02	8.18E-03	2.76E-03	1.90E-03	1.16E-03	7.45E-04	
BLK1	2.41E-02	2.10E-02	8.98E-03	5.02E-03	4.00E-03	3.01E-03	1.02E-03	7.05E-04	4.29E-04	2.74E-04	
BLK2	1.97E-02	1.72E-02	7.23E-03	4.07E-03	2.90E-03	2.07E-03	7.21E-04	5.07E-04	3.09E-04	1.97E-04	
BLK3	1.76E-02	1.54E-02	6.70E-03	3.85E-03	2.34E-03	1.52E-03	5.69E-04	4.17E-04	2.59E-04	1.64E-04	
BLK4	1.71E-02	1.52E-02	7.17E-03	4.40E-03	2.15E-03	1.16E-03	5.19E-04	4.08E-04	2.59E-04	1.67E-04	
REF1	3.61E-02	3.27E-02	1.94E-02	1.34E-02	4.54E-03	1.37E-03	1.06E-03	9.52E-04	6.34E-04	4.12E-04	
REF2	2.21E-02	2.00E-02	1.24E-02	8.60E-03	2.49E-03	3.68E-04	3.22E-04	2.97E-04	1.98E-04	1.28E-04	
REF3	1.20E-02	1.10E-02	6.81E-03	4.71E-03	1.29E-03	9.36E-05	7.83E-05	7.20E-05	4.66E-05	2.97E-05	
[LI]											
BLK1	3.58E-02	1.38E-02	8.23E-03	6.40E-03	5.10E-03	3.85E-03	1.34E-03	9.51E-04	5.97E-04	3.85E-04	
BLK2	2.43E-02	1.14E-02	6.80E-03	5.16E-03	3.67E-03	2.61E-03	9.20E-04	6.62E-04	4.15E-04	2.65E-04	
BLK3	1.91E-02	1.05E-02	6.58E-03	4.96E-03	2.97E-03	1.89E-03	7.18E-04	5.34E-04	3.38E-04	2.16E-04	
BLK4	1.70E-02	1.10E-02	7.67E-03	5.86E-03	2.79E-03	1.47E-03	6.64E-04	5.27E-04	3.40E-04	2.19E-04	
REF1	1.80E-03	1.45E-03	1.28E-03	1.02E-03	3.40E-04	9.85E-05	7.69E-05	6.98E-05	4.66E-05	3.04E-05	
REF2	1.07E-03	9.16E-04	8.28E-04	6.55E-04	1.88E-04	2.61E-05	2.31E-05	2.14E-05	1.43E-05	9.33E-05	
REF3	5.98E-04	5.01E-04	4.56E-04	3.59E-04	9.76E-05	6.53E-06	5.47E-06	5.07E-06	3.33E-06	2.15E-06	
[FE1422]											
REF1	3.29E-01	3.21E-01	2.89E-01	2.30E-01	7.16E-02	1.57E-02	1.22E-02	1.10E-02	7.27E-03	4.70E-03	
REF2	1.98E-01	1.94E-01	1.79E-01	1.43E-01	3.99E-02	4.23E-03	3.69E-03	3.41E-03	2.27E-03	1.47E-03	
REF3	1.06E-01	1.04E-01	9.63E-02	7.63E-02	2.05E-02	1.08E-03	8.74E-04	8.05E-04	5.25E-04	3.38E-04	
NUCLEAR HEATING(MW/M) DURING OPERATION											
HAT.	FH	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3			
V*	1.70E+00	6.85E-01	5.23E-01	4.27E-01	3.60E-01	4.56E-01	1.97E-01	9.29E-02			
LI		1.07E+01	7.67E+00	5.82E+00	4.67E+00	3.74E-01	2.09E-01	1.37E-01			
FE						5.23E+00	2.20E+00	1.03E+00			

Table 4.6-7. [V15Cr5Ti/FLIBE] LOCA/Volume-Integrated Decay Afterheat (MW/m)
@ 2.5 MW-4 yr/m² Wall Load

REGION	0	1 MIN	10 MIN	1 HR	TIME AFTER SHUTDOWN				1 WEEK	1 MO	6 MO	1 YR
					6 HR	1 DAY	1 DAY	1 DAY				
[V15CR5Ti]												
FK	6.10E-02	3.14E-02	1.13E-02	4.92E-03	2.29E-03	1.48E-03	4.77E-04	3.15E-04	1.86E-04	1.16E-04		
BLK1	1.53E-01	7.24E-02	2.65E-02	1.20E-02	4.31E-03	2.40E-03	7.65E-04	5.03E-04	2.90E-04	1.79E-04		
BLK2	1.07E-01	4.86E-02	1.57E-02	6.13E-03	1.97E-03	1.01E-03	3.21E-04	2.11E-04	1.17E-04	6.94E-05		
BLK3	6.43E-02	2.90E-02	8.16E-03	2.56E-03	8.27E-04	4.24E-04	1.41E-04	9.45E-05	4.99E-05	2.83E-05		
BLK4	3.58E-02	1.58E-02	4.04E-03	1.02E-03	3.38E-04	1.79E-04	6.37E-05	4.41E-05	2.23E-05	1.20E-05		
BLK5	1.89E-02	8.03E-03	1.92E-03	3.98E-04	1.37E-04	7.50E-05	2.91E-05	2.08E-05	1.01E-05	5.17E-06		
BLK6	9.71E-03	3.86E-03	8.89E-04	1.59E-04	5.64E-05	3.13E-05	1.32E-05	9.67E-06	4.59E-06	2.25E-06		
BLK7	4.89E-03	1.79E-03	4.16E-04	7.90E-05	2.74E-05	1.31E-05	6.00E-06	4.53E-06	2.13E-06	1.02E-06		
BLK8	2.50E-03	8.67E-04	2.55E-04	9.18E-05	2.72E-05	6.06E-06	3.22E-06	2.57E-06	1.27E-06	6.55E-07		
[FLIBE]												
BLK1	5.44E-01	8.31E-02	4.29E-02	2.92E-02	8.52E-03	3.97E-03	1.32E-03	9.20E-04	5.59E-04	3.54E-04		
BLK2	2.93E-01	4.28E-02	1.98E-02	1.26E-02	3.62E-03	1.65E-03	5.30E-04	3.60E-04	2.10E-04	1.29E-04		
BLK3	1.49E-01	2.20E-02	8.70E-03	4.97E-03	1.46E-03	6.85E-04	2.24E-04	1.53E-04	8.56E-05	5.06E-05		
BLK4	7.43E-02	1.12E-02	3.83E-03	1.91E-03	5.84E-04	2.85E-04	9.82E-05	6.82E-05	3.67E-05	2.08E-05		
BLK5	3.65E-02	5.58E-03	1.69E-03	7.32E-04	2.33E-04	1.19E-04	4.37E-05	3.12E-05	1.61E-05	8.73E-06		
BLK6	1.78E-02	2.71E-03	7.56E-04	2.88E-04	9.47E-05	4.91E-05	1.95E-05	1.43E-05	7.15E-06	3.72E-06		
BLK7	8.73E-03	1.30E-03	3.69E-04	1.42E-04	4.61E-05	2.05E-05	8.87E-06	6.66E-06	3.28E-06	1.67E-06		
BLK8	4.37E-03	7.23E-04	3.01E-04	1.72E-04	4.96E-05	9.54E-06	4.90E-06	3.90E-06	2.02E-06	1.09E-06		

NUCLEAR HEATING(MW/M) DURING OPERATION

MAT.	FK	BLK1	BLK2	BLK3	BLK4	BLK5	BLK6	BLK7	BLK8
V*	5.68E-01	1.19E+00	6.56E-01	3.41E-01	1.70E-01	8.33E-02	4.04E-02	1.99E-02	1.08E-02
FLIBE		2.35E+01	1.03E+01	4.58E+00	1.98E+00	8.39E-01	3.52E-01	1.47E-01	6.25E-02

Table 4.6-8. [Li20V/He/Be] LOCA/Volume-Integrated Decay Afterheat (MW/m)
@ 2.5 MW-4 yr/m² Wall Load

REGION	0	TIME AFTER SHUTDOWN							1 YR	
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO		6 MO
[V15CR5TI]										
FH	9.74E-02	7.35E-02	2.13E-02	7.16E-03	6.65E-03	5.35E-03	1.81E-03	1.24E-03	7.50E-04	4.76E-04
BLK1	8.32E-02	5.99E-02	1.59E-02	4.52E-03	4.20E-03	3.37E-03	1.13E-03	7.70E-04	4.58E-04	2.88E-04
BLK2	5.82E-02	4.14E-02	9.88E-03	2.12E-03	1.97E-03	1.57E-03	5.28E-04	3.62E-04	2.12E-04	1.31E-04
BLK3	3.66E-02	2.61E-02	5.91E-03	1.04E-03	9.58E-04	7.67E-04	2.64E-04	1.89E-04	1.06E-04	6.45E-05
BLK4	2.13E-02	1.51E-02	3.32E-03	5.10E-04	4.70E-04	3.77E-04	1.35E-04	9.57E-05	5.48E-05	3.28E-05
BLK5	1.19E-02	8.27E-03	1.79E-03	2.54E-04	2.32E-04	1.86E-04	6.96E-05	5.05E-05	2.86E-05	1.69E-05
BLK6	1.62E-02	1.11E-02	2.37E-03	3.09E-04	2.74E-04	2.19E-04	8.72E-05	6.48E-05	3.57E-05	2.04E-05
BLK7	9.61E-03	6.52E-03	1.40E-03	1.90E-04	1.51E-04	1.15E-04	4.98E-05	3.77E-05	1.99E-05	1.09E-05
PLNH	1.77E-02	1.28E-02	2.95E-03	5.50E-04	2.98E-04	1.81E-04	9.07E-05	7.06E-05	3.37E-05	1.64E-05
[7Li20-38E]										
BLK1	1.81E-01	3.60E-02	1.28E-02	7.12E-03	6.62E-03	5.31E-03	1.83E-03	1.30E-03	8.06E-04	5.15E-04
BLK2	9.02E-02	2.30E-02	7.14E-03	3.30E-03	3.05E-03	2.43E-03	8.24E-04	5.79E-04	3.52E-04	2.22E-04
BLK3	4.71E-02	1.50E-02	4.18E-03	1.60E-03	1.47E-03	1.17E-03	4.02E-04	2.85E-04	1.71E-04	1.06E-04
BLK4	2.51E-02	9.32E-03	2.43E-03	7.88E-04	7.20E-04	5.74E-04	2.03E-04	1.46E-04	8.69E-05	5.33E-05
BLK5	1.38E-02	5.83E-03	1.43E-03	3.95E-04	3.57E-04	2.85E-04	1.05E-04	7.70E-05	4.52E-05	2.73E-05
BLK6	4.37E-03	2.15E-03	5.00E-04	1.10E-04	9.62E-05	7.63E-05	3.01E-05	2.24E-05	1.27E-05	7.41E-06
BLK7	2.84E-03	1.59E-03	3.61E-04	6.93E-05	5.43E-05	4.13E-05	1.76E-05	1.34E-05	7.24E-06	4.03E-06
NUCLEAR HEATING(MW/H) DURING OPERATION										
HAT.	FH	BLK1	BLK2	BLK3	BLK4	BLK5	BLK6	BLK7	PLNH	
V#	1.30E+00	9.88E-01	6.10E-01	3.77E-01	2.32E-01	1.45E-01	2.28E-01	1.57E-01	3.03E-01	
7Li20-38E		2.03E+01	1.09E+01	5.88E+00	3.12E+00	1.64E+00	4.83E-01	2.99E-01		

As an example, Fig. 4.6-6 shows a comparison of LOFA and LOCA for the decay-heating rates in the first 10 cm of the lithium-cooled blanket as a function of post-shutdown time. There exists only a marginal difference between the two cases, indicating that the presence of liquid lithium is not likely to affect the specific heating rate in the V15Cr5Ti structure.

By comparing Tables 4.6-2 and 4.6-8 with Tables 4.6-9 and 4.6-10, respectively, one finds that most of the decay gammas coming through the first-wall and blanket regions are being absorbed in Fe1422 in the reflector region. The implication is that the reflector region, particularly Fe-1422, becomes the major heat sink for this design under LOCA circumstances. In fact, the effectiveness of the Compton process in the reflector region amounts to more than 20 times that in the blanket region.

In the case of LOCA for the self-cooled Flibe-blanket design, the decay gammas most populated at the first wall and in its vicinity tend to travel further into deep blanket regions. A large fraction of the gamma rays appear to be absorbed in the blanket before they reach the shield region due to the large blanket thickness (80 cm) of this design. This trend is clearly shown in Fig. 4.6-7. One can also compare Tables 4.6-11 and 4.6-12 with Tables 4.6-3 and 4.6-7, respectively. Unlike the liquid-lithium blanket design discussed earlier, the decay afterheat for LOCA in the Flibe design is more uniformly smeared out over the V15Cr5Ti structure in the entire blanket region.

4.6.1.3 Impact of Reflector Designs

As discussed in the previous section, the major decay-afterheat sink in the liquid-lithium blanket is the Fe1422-base reflector in the case of LOCA. In addition, the Fe1422 alloy itself is the decay gamma source as already shown in Fig. 4.6-5.

The objective of this section is to study the impact of an alternative reflector design which consists of 90% V15Cr5Ti + 10% lithium, on the overall blanket performance in terms of the decay afterheating.

Figure 4.6-8 compares the decay gamma spectra for these two different reflector designs. One notices that the three major gammas at energies, ~2.1 MeV, ~1.8 MeV, and ~0.85 MeV that are emitted from ^{56}Mn have been eliminated in the alternative reflector design. Note that the 0.85-MeV peak is slightly

Fig. 4.6-6. Comparison of decay heating for LOFA/LOCA cases; Li/Li/V15CrTi design (BLK1 stands for the first 10 cm region of the blanket.)

Fig. 4.6-7. Comparison of decay heating for LOFA/LOCA cases; Flibe/Flibe/V15Cr5Ti design (BLK1 stands for the first 10 cm of the blanket and BLK8 stands for the last 10 cm that is 70 cm behind BLK1.)

Table 4.6-9. [Li/V15Cr5Ti/Fe1422] LOCA/Specific Decay Afterheat (MW/m³)
@ 2.5 MW-4 yr/m² Wall Load

REGION	TIME AFTER SHUTDOWN									
	0	1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[V15CR5Y]										
FH	4.89E-01	4.31E-01	1.97E-01	1.16E-01	8.71E-02	6.40E-02	2.22E-02	1.56E-02	9.58E-03	6.13E-03
BLK1	4.09E-01	3.59E-01	1.55E-01	1.02E-01	7.27E-02	5.19E-02	1.83E-02	1.29E-02	7.97E-03	5.09E-03
BLK2	3.55E-01	3.03E-01	1.44E-01	8.97E-02	5.98E-02	4.10E-02	1.49E-02	1.07E-02	6.61E-03	4.22E-03
BLK3	3.05E-01	2.69E-01	1.31E-01	8.33E-02	5.08E-02	3.30E-02	1.25E-02	9.21E-03	5.74E-03	3.67E-03
BLK4	2.80E-01	2.48E-01	1.25E-01	8.18E-02	4.50E-02	2.71E-02	1.10E-02	8.40E-03	5.30E-03	3.40E-03
REF1	2.34E-01	2.17E-01	1.26E-01	8.73E-02	3.18E-02	1.13E-02	7.49E-03	6.58E-03	4.35E-03	2.82E-03
REF2	1.31E-01	1.19E-01	7.37E-02	5.12E-02	1.49E-02	2.26E-03	1.94E-03	1.79E-03	1.19E-03	7.71E-04
REF3	6.85E-02	6.27E-02	3.89E-02	2.70E-02	7.36E-03	5.39E-04	4.49E-04	4.12E-04	2.68E-04	1.71E-04
[FE1422]										
REF1	2.71E-01	2.62E-01	2.31E-01	1.84E-01	6.05E-02	1.61E-02	1.08E-02	9.47E-03	6.22E-03	4.02E-03
REF2	1.47E-01	1.44E-01	1.33E-01	1.06E-01	2.97E-02	3.26E-03	2.78E-03	2.56E-03	1.70E-03	1.10E-03
REF3	7.56E-02	7.44E-02	6.87E-02	5.44E-02	1.46E-02	7.75E-04	6.27E-04	5.77E-04	3.77E-04	2.43E-04
NUCLEAR HEATING(MW/M**3) DURING OPERATION										
HAT.	FH	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3		
V*	1.28E+01	9.91E+00	7.31E+00	5.66E+00	4.59E+00	2.79E+00	1.16E+00	5.28E-01		
LI		8.18E+00	5.59E+00	4.06E+00	3.13E+00	2.29E+00	1.23E+00	7.76E-01		
FE						4.00E+00	1.62E+00	7.30E-01		

Table 4.6-10. [Li/V15Cr5Ti/Fe1422] LOCA Volume-Integrated Decay Afterheat (MW/m)
(MW/m³) @ 2.5 MW-4 yr/m²

REGION	0	TIME AFTER SHUTDOWN							1 YR	
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO		6 MO
[V15CR5TI]										
FW	6.59E-02	5.81E-02	2.66E-02	1.56E-02	1.17E-02	8.63E-03	2.99E-03	2.10E-03	1.29E-03	8.28E-04
BLK1	2.83E-02	2.48E-02	1.07E-02	7.05E-03	5.02E-03	3.59E-03	1.26E-03	8.92E-04	5.51E-04	3.52E-04
BLK2	2.49E-02	2.19E-02	1.04E-02	6.50E-03	4.32E-03	2.96E-03	1.08E-03	7.73E-04	4.78E-04	3.05E-04
BLK3	2.30E-02	2.03E-02	9.88E-03	6.28E-03	3.83E-03	2.49E-03	9.42E-04	6.94E-04	4.33E-04	2.77E-04
BLK4	2.20E-02	1.95E-02	9.82E-03	6.42E-03	3.53E-03	2.13E-03	8.64E-04	6.60E-04	4.16E-04	2.67E-04
REF1	3.82E-02	3.54E-02	2.06E-02	1.43E-02	5.19E-03	1.85E-03	1.22E-03	1.07E-03	7.11E-04	4.61E-04
REF2	2.22E-02	2.02E-02	1.23E-02	8.69E-03	2.53E-03	3.83E-04	3.29E-04	3.04E-04	2.02E-04	1.31E-04
REF3	1.21E-02	1.10E-02	6.84E-03	4.75E-03	1.29E-03	9.48E-05	7.90E-05	7.25E-05	4.71E-05	3.01E-05
[FE1422]										
REF1	3.54E-01	3.42E-01	3.02E-01	2.40E-01	7.91E-02	2.10E-02	1.41E-02	1.24E-02	8.13E-03	5.25E-03
REF2	2.00E-01	1.95E-01	1.81E-01	1.44E-01	4.03E-02	4.42E-03	3.77E-03	3.47E-03	2.31E-03	1.49E-03
REF3	1.06E-01	1.05E-01	9.67E-02	7.66E-02	2.05E-02	1.09E-03	8.82E-04	8.12E-04	5.31E-04	3.42E-04
NUCLEAR HEATING(MW/M) DURING OPERATION										
MAT.	FW	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3		
V*	1.70E+00	6.85E-01	5.23E-01	4.27E-01	3.60E-01	4.56E-01	1.97E-01	9.29E-02		
LI		1.07E+01	7.67E+00	5.82E+00	4.67E+00	3.74E-01	2.09E-01	1.37E-01		
FE						5.23E+00	2.20E+00	1.03E+00		

Table 4.6-11. [V15Cr5Ti/FLIBE] LCCA/Specific Decay Afterheat (MW/m³)
@ 2.5 MW-4 yr/m²

REGION	0	TIME AFTER SHUTDOWN								
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO	1 YR	
[V15CR5II]										
FH	9.17E-01	7.76E-01	2.11E-01	5.98E-02	5.53E-02	4.43E-02	1.41E-02	9.27E-03	5.38E-03	3.34E-03
BLK1	7.99E-01	6.74E-01	1.75E-01	4.78E-02	4.44E-02	3.56E-02	1.14E-02	7.61E-03	4.39E-03	2.71E-03
BLK2	6.00E-01	5.05E-01	1.25E-01	3.20E-02	2.97E-02	2.38E-02	7.77E-03	5.22E-03	2.99E-03	1.83E-03
BLK3	4.12E-01	3.46E-01	8.59E-02	2.17E-02	2.01E-02	1.61E-02	5.32E-03	3.62E-03	2.07E-03	1.25E-03
BLK4	2.70E-01	2.26E-01	5.62E-02	1.53E-02	1.41E-02	1.13E-02	3.77E-03	2.59E-03	1.49E-03	9.03E-04
BLK5	1.75E-01	1.47E-01	3.74E-02	1.12E-02	1.02E-02	8.16E-03	2.74E-03	1.89E-03	1.10E-03	6.70E-04
BLK6	1.15E-01	9.71E-02	2.56E-02	8.40E-03	7.58E-03	6.01E-03	2.02E-03	1.41E-03	8.28E-04	5.07E-04
BLK7	7.90E-02	6.66E-02	1.83E-02	6.50E-03	5.72E-03	4.50E-03	1.52E-03	1.06E-03	6.32E-04	3.89E-04
BLK8	5.64E-02	4.76E-02	1.35E-02	5.24E-03	4.41E-03	3.40E-03	1.15E-03	8.13E-04	4.88E-04	3.02E-04

NUCLEAR HEATING(MW/M ² *3) DURING OPERATION									
HAT.	FH	BLK1	BLK2	BLK3	BLK4	BLK5	BLK6	BLK7	BLK8
V*	1.41E+01	1.08E+01	5.69E+00	2.83E+00	1.36E+00	6.39E-01	2.98E-01	1.42E-01	7.46E-02
FLIBE	1.86E+01	1.86E+01	7.81E+00	3.31E+00	1.37E+00	5.59E-01	2.26E-01	9.11E-02	3.74E-02

Table 4.6-12. [V15Cr5Ti/FLIBE] LOCA Volume-Integrated Afterheat (MW/m)
(MW/m³) @ 2.5 MW-4 yr/m²

REGION	0	TIME AFTER SHUTDOWN							1 YR	
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO		6 MO
[V15CR5Ti]										
FW	3.70E-02	3.13E-02	8.52E-03	2.41E-03	2.23E-03	1.79E-03	5.70E-04	3.74E-04	2.17E-04	1.35E-04
BLK1	8.80E-02	7.43E-02	1.93E-02	5.27E-03	4.89E-03	3.92E-03	1.26E-03	8.39E-04	4.84E-04	2.98E-04
BLK2	6.92E-02	5.82E-02	1.44E-02	3.69E-03	3.43E-03	2.75E-03	8.95E-04	6.02E-04	3.44E-04	2.10E-04
BLK3	4.96E-02	4.17E-02	1.02E-02	2.61E-03	2.42E-03	1.94E-03	6.40E-04	4.35E-04	2.49E-04	1.51E-04
BLK4	3.38E-02	2.84E-02	7.05E-03	1.92E-03	1.77E-03	1.42E-03	4.72E-04	3.24E-04	1.86E-04	1.13E-04
BLK5	2.28E-02	1.91E-02	4.83E-03	1.46E-03	1.33E-03	1.06E-03	3.57E-04	2.47E-04	1.43E-04	8.73E-05
BLK6	1.56E-02	1.31E-02	3.46E-03	1.14E-03	1.03E-03	8.14E-04	2.74E-04	1.91E-04	1.12E-04	6.86E-05
BLK7	1.11E-02	9.35E-03	2.55E-03	9.13E-04	8.04E-04	6.31E-04	2.13E-04	1.49E-04	8.88E-05	5.46E-05
BLK8	8.20E-03	6.92E-03	1.97E-03	7.61E-04	6.41E-04	4.94E-04	1.68E-04	1.18E-04	7.10E-05	4.39E-05

NUCLEAR HEATING(MW/M) DURING OPERATION										
MAT.	FW	BLK1	BLK2	BLK3	BLK4	BLK5	BLK6	BLK7	BLK8	
V*	5.58E-01	1.19E+00	6.56E-01	3.41E-01	1.70E-01	8.33E-02	4.04E-02	1.99E-02	1.08E-02	
FLIBE		2.35E+01	1.03E+01	4.58E+00	1.98E+00	8.39E-01	3.52E-01	1.47E-01	6.25E-02

Fig. 4.6-8. Effect of reflector designs on decay-gamma spectrum system: Li/Li/V15Cr5Ti.

shifted to a lower-energy side due to the multigroup energy structure used in the calculation.

Based on these gamma spectra, a comparison was made for the specific decay heating rates in three cases: V15Cr5Ti in the blanket (Fig. 4.6-9), lithium in the blanket (Fig. 4.6-10), and V15Cr5Ti in the reflector (Fig. 4.6-11). These figures indicate that the reflector performance in terms of the decay afterheat can be substantially improved by the V15Cr5Ti-base reflector design without much affecting the blanket performance. Although the lithium heating rate in the blanket is almost doubled by the use of the V15Cr5Ti-base reflector, the absolute magnitude is substantially lower than that of the V15Cr5Ti structure in the blanket. In fact this increase does not appear to pose any difficulty in terms of the thermal hydraulics as shown elsewhere in this report.

As a final note, the impact of the use of the alternative reflector design is not limited to the decay afterheat. The overall blanket performance on tritium breeding ratio (TBR) and system energy multiplication factor (EMF) can be compared as follows:

Fig. 4.6-9. Effect of reflector designs on decay-gamma heating in blanket; material: V15Cr5Ti (system: Li/Li/V15Cr5Ti).

Fig. 4.6-10. Effect of reflector designs on decay heating in blanket; material: lithium (system: Li/Li/V15Cr5Ti).

Fig. 4.6-11. Effect of reflector designs on decay heating in reflector; material V15Cr5Ti (system: Li/Li/V15Cr5Ti).

	<u>TBR</u>	<u>EMF</u>
Fe1422-base reflector	1.26	1.27
V15Cr5Ti-base reflector	1.35	1.22

Namely, the alternative reflector design enhances tritium production due to the less parasitic neutron loss in V15Cr5Ti than in Fe1422, while the energy multiplication is sizably lowered in return.

For the sake of complete comparison with the Fe1422-base reflector design, four tables, Tables 4.6-13 through 4.6-16 for the alternative reflector design are included here. These tables should be compared respectively to Tables 4.6-2, 4.6-6, 4.6-9, and 4.6-10.

Table 4.6-13. [V15Cr5Ti/Li] LOFA/Specific Decay Afterheat (MW/m³)
@ 2.5 MW-4 yr/m²

REGION	0	TIME AFTER SHUTDOWN							1 YR	
		1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO		
[V15CR5Ti]										
FH	4.48E-01	3.89E-01	1.54E-01	8.08E-02	7.53E-02	6.05E-02	1.99E-02	1.35E-02	8.22E-03	5.25E-03
BLK1	3.51E-01	3.03E-01	1.15E-01	5.79E-02	5.40E-02	4.33E-02	1.40E-02	9.45E-03	5.69E-03	3.61E-03
BLK2	2.76E-01	2.37E-01	8.17E-02	3.78E-02	3.52E-02	2.82E-02	8.99E-03	6.01E-03	3.58E-03	2.25E-03
BLK3	2.40E-01	2.04E-01	6.35E-02	2.59E-02	2.42E-02	1.94E-02	6.11E-03	4.08E-03	2.40E-03	1.49E-03
BLK4	2.30E-01	1.94E-01	5.41E-02	1.84E-02	1.71E-02	1.37E-02	4.34E-03	2.90E-03	1.68E-03	1.03E-03
REF1	2.49E-01	2.08E-01	4.76E-02	8.30E-03	8.20E-03	6.56E-03	2.11E-03	1.40E-03	7.42E-04	4.19E-04
REF2	2.13E-01	1.78E-01	3.66E-02	3.13E-03	2.68E-03	2.12E-03	8.40E-04	5.97E-04	2.65E-04	1.21E-04
REF3	1.49E-01	1.25E-01	3.01E-02	6.46E-03	2.37E-03	8.43E-04	4.65E-04	3.64E-04	1.53E-04	6.27E-05
[LI]										
BLK1	4.43E-02	2.52E-02	1.27E-02	9.32E-03	8.73E-03	7.02E-03	2.35E-03	1.64E-03	1.02E-03	6.54E-04
BLK2	3.13E-02	1.97E-02	8.83E-03	6.02E-03	5.64E-03	4.52E-03	1.47E-03	1.01E-03	6.23E-04	3.96E-04
BLK3	2.55E-02	1.74E-02	6.78E-03	4.12E-03	3.86E-03	3.09E-03	9.91E-04	6.78E-04	4.12E-04	2.59E-04
BLK4	2.39E-02	1.73E-02	5.77E-03	2.95E-03	2.76E-03	2.21E-03	7.05E-04	4.81E-04	2.87E-04	1.79E-04
REF1	2.70E-02	2.07E-02	5.19E-03	1.50E-03	1.40E-03	1.12E-03	3.56E-04	2.39E-04	1.30E-04	7.46E-05
REF2	2.32E-02	1.81E-02	3.89E-03	5.33E-04	4.55E-04	3.57E-04	1.37E-04	9.67E-05	4.42E-05	2.07E-05
REF3	1.64E-02	1.30E-02	3.57E-03	1.14E-03	4.11E-04	1.37E-04	7.28E-05	5.68E-05	2.43E-05	1.02E-05
NUCLEAR HEATING(MW/M**3) DURING OPERATION										
MAT.	FH	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3		
V*	1.24E+01	9.74E+00	7.11E+00	5.43E+00	4.31E+00	2.58E+00	1.21E+00	7.12E-01		
LI		2.12E+01	1.41E+01	9.79E+00	7.04E+00	4.16E+00	1.85E+00	7.70E-01		

Table 4.6-14. [V15Cr5Ti/Li] LOFA/Volume-Integrated Decay Afterheat (MW/m)
@ 2.5 MW-4 yr/m²

REGION	0	TIME AFTER SHUTDOWN						1 WEEK	1 MO	6 MO	1 YR
		1 MIN	10 MIN	1 HR	6 HR	1 DAY					
[V15CR5TI]											
FH	6.04E-02	5.24E-02	2.08E-02	1.09E-02	1.01E-02	8.15E-03	2.68E-03	1.82E-03	1.11E-03	7.07E-04	
BLK1	2.43E-02	2.10E-02	7.94E-03	4.00E-03	3.73E-03	2.99E-03	9.70E-04	6.53E-04	3.93E-04	2.49E-04	
BLK2	2.00E-02	1.71E-02	5.90E-03	2.73E-03	2.55E-03	2.04E-03	6.50E-04	4.35E-04	2.58E-04	1.62E-04	
BLK3	1.81E-02	1.54E-02	4.79E-03	1.95E-03	1.82E-03	1.46E-03	4.61E-04	3.07E-04	1.81E-04	1.13E-04	
BLK4	1.80E-02	1.53E-02	4.25E-03	1.64E-03	1.35E-03	1.08E-03	3.41E-04	2.28E-04	1.32E-04	8.13E-05	
REF1	3.66E-01	3.08E-01	6.99E-02	1.29E-02	1.20E-02	9.65E-03	3.10E-03	2.06E-03	1.09E-03	6.16E-04	
REF2	3.26E-01	2.72E-01	5.59E-02	4.79E-03	4.10E-03	3.24E-03	1.28E-03	9.12E-04	4.05E-04	1.85E-04	
REF3	2.36E-01	1.98E-01	4.76E-02	1.02E-02	3.76E-03	1.34E-03	7.36E-04	5.76E-04	2.43E-04	9.92E-05	
[LI]											
BLK1	5.82E-02	3.31E-02	1.66E-02	1.22E-02	1.15E-02	9.22E-03	3.08E-03	2.15E-03	1.34E-03	8.59E-04	
BLK2	4.29E-02	2.71E-02	1.21E-02	8.27E-03	7.74E-03	6.21E-03	2.02E-03	1.39E-03	8.56E-04	5.44E-04	
BLK3	3.65E-02	2.49E-02	9.72E-03	5.91E-03	5.53E-03	4.43E-03	1.42E-03	9.71E-04	5.90E-04	3.71E-04	
BLK4	3.56E-02	2.59E-02	8.62E-03	4.40E-03	4.11E-03	3.30E-03	1.05E-03	7.17E-04	4.29E-04	2.66E-04	
REF1	4.41E-03	3.38E-03	8.48E-04	2.45E-04	2.29E-04	1.83E-04	5.82E-05	3.90E-05	2.12E-05	1.22E-05	
REF2	3.93E-03	3.07E-03	6.59E-04	9.09E-05	7.72E-05	6.08E-05	2.32E-05	1.64E-05	7.49E-05	3.51E-06	
REF3	2.89E-03	2.28E-03	6.29E-04	2.01E-04	7.22E-05	2.42E-05	1.28E-05	9.99E-06	4.28E-06	1.79E-06	
NUCLEAR HEATING(MW/M) DURING OPERATION											
FH		BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3			
HAT.											
V*	1.67E+00	6.73E-01	5.14E-01	4.09E-01	3.38E-01	3.80E+00	1.85E+00	1.13E+00			
LI		2.78E+01	1.93E+01	1.40E+01	1.05E+01	6.80E-01	3.15E-01	1.35E-01			

Table 4.6-15. [V15Cr5Ti/Li] LOCA/Specific Decay Afterheat (MW/m³)
@ 2.5 MW-4 yr/m²

REGION	0	1 MIN	10 MIN	1 HR	TIME AFTER SHUTDOWN 6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[V15CR5TI]										
FH	4.95E-01	4.29E-01	1.65E-01	8.46E-02	7.88E-02	6.33E-02	2.06E-02	1.39E-02	8.39E-03	5.33E-03
BLK1	4.14E-01	3.57E-01	1.31E-01	6.83E-02	6.37E-02	5.11E-02	1.65E-02	1.11E-02	6.66E-03	4.21E-03
BLK2	3.51E-01	3.02E-01	1.07E-01	5.36E-02	5.00E-02	4.01E-02	1.28E-02	8.65E-03	5.16E-03	3.25E-03
BLK3	3.13E-01	2.68E-01	8.93E-02	4.26E-02	3.98E-02	3.19E-02	1.02E-02	6.87E-03	4.03E-03	2.56E-03
BLK4	2.94E-01	2.50E-01	7.84E-02	3.45E-02	3.23E-02	2.59E-02	8.29E-03	5.60E-03	3.32E-03	2.07E-03
REF1	2.66E-01	2.24E-01	5.47E-02	1.38E-02	1.29E-02	1.03E-02	3.36E-03	2.28E-03	1.29E-03	7.69E-04
REF2	2.15E-01	1.80E-01	3.72E-02	3.49E-03	3.01E-03	2.38E-03	9.36E-04	6.69E-04	3.12E-04	1.51E-04
REF3	1.50E-01	1.26E-01	3.03E-02	6.53E-03	2.42E-03	8.69E-04	4.76E-04	3.73E-04	1.58E-04	6.59E-05

NUCLEAR HEATING(MW/M**3) DURING OPERATION										
MAT.	FH	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3		
V*	1.24E+01	9.74E+00	7.11E+00	5.43E+00	4.31E+00	2.58E+00	1.21E+00	7.12E-01		
LI		2.12E+01	1.41E+01	9.79E+00	7.04E+00	4.16E+00	1.85E+00	7.70E-01		

Table 4.6-16. [V15Cr5Ti/Li] LOCA/Volume-Integrated Decay Afterheat (MW/m)
@ 2.5 MW-4 yr/m²

REGION	0	1 MIN	10 MIN	1 HR	6 HR	1 DAY	1 WEEK	1 MO	6 MO	1 YR
[V15CR5TI]										
FW	6.67E-02	5.78E-02	2.23E-02	1.14E-02	1.06E-02	8.53E-03	2.77E-03	1.87E-03	1.13E-03	7.18E-04
BLK1	2.86E-02	2.47E-02	9.25E-03	4.72E-03	4.40E-03	3.53E-03	1.14E-03	7.66E-04	4.61E-04	2.91E-04
BLK2	2.54E-02	2.18E-02	7.76E-03	3.87E-03	3.61E-03	2.90E-03	9.28E-04	6.25E-04	3.73E-04	2.35E-04
BLK3	2.36E-02	2.02E-02	6.77E-03	3.21E-03	3.00E-03	2.40E-03	7.69E-04	5.18E-04	3.08E-04	1.93E-04
BLK4	2.31E-02	1.96E-02	6.15E-03	2.71E-03	2.53E-03	2.03E-03	6.51E-04	4.40E-04	2.61E-04	1.63E-04
REF1	3.92E-01	3.29E-01	8.04E-02	2.03E-02	1.89E-02	1.52E-02	4.94E-03	3.35E-03	1.90E-03	1.13E-03
REF2	3.29E-01	2.74E-01	5.68E-02	5.33E-03	4.60E-03	3.64E-03	1.43E-03	1.02E-03	4.76E-04	2.30E-04
REF3	2.37E-01	1.99E-01	4.79E-02	1.03E-02	3.82E-03	1.38E-03	7.54E-04	5.90E-04	2.51E-04	1.04E-04

NUCLEAR HEATING(MW/M) DURING OPERATION.

MAT.	FW	BLK1	BLK2	BLK3	BLK4	REF1	REF2	REF3
V*	1.67E+00	6.73E-01	5.14E-01	4.09E-01	3.30E-01	3.80E+00	1.85E+00	1.13E+00
LI		2.78E+01	1.93E+01	1.40E+01	1.05E+01	6.80E-01	3.15E-01	1.35E-01

4.6.1.4 Transient Temperature Calculations

With the afterheat calculated as a function of time after reactor shutdown and location, the transient behavior of the blanket can be calculated for various accident scenarios. The main uncertainty is how to define the boundary conditions. For being conservative, it is assumed here that the blanket, reflector shield and ambient are completely separated. Thermal radiation is the only heat transfer mode between these regions. The emissivity used is 0.3. This conservative assumption can be relaxed as more detailed system configuration becomes available in the future.

A finite difference computer code has been set up to do this calculation. The iterative method is based on energy balance for each zone during one time step. This equation is in the form of

$$T_i' = T_i + \sum_j \text{COND}_{ij} (T_j - T_i) + V_i Q_i''' - \frac{\Delta\theta}{(\rho C_p V)_i},$$

in which

T_i' is temperature after time step $\Delta\theta$

T_i is temperature before time step $\Delta\theta$

COND is the conductivity

$$\left[\equiv \frac{kA}{x} + \sigma \epsilon (T_j^2 + T_i^2)(T_j + T_i) \right]$$

V is volume

Q_i''' is heat generation rate

ρC_p is volumetric thermal inertia

and

$\Delta\theta$ is the time step which is defined by

$$\Delta\theta \leq \frac{(\rho C_p V)_i}{\sum_j \text{COND}_{ij}} \text{ min.}$$

By knowing the initial condition, the entire transient of the blanket can be calculated by each time step Δt iteratively. The time step is defined by the convergence criteria of the iteration process.

Three types of accident conditions are evaluated for two blankets. The blankets evaluated are self-cooled V/Li blanket and self-cooled V/Flibe blanket. The accident conditions evaluated are:

- (1) Loss of flow accident: At time = 0, the coolant flow stops for both blanket, reflector and shield.
- (2) Loss of coolant accident: At time = 0, the coolant is drained from the blanket, reflector and shield.
- (3) Partial loss of coolant accident: At time = 0, one blanket module loses coolant, while the other blanket modules remain in normal operation.

The plasma is assumed to be quenched at time = 0 for all the cases.

The allowable temperature limitations for the V-blanket structure with no pressure load is not clear. It is believed that if temperature exceeds 120°C , the blanket configuration may change so that normal maintenance scheme may not be usable. Therefore, 1200°C is picked as the allowable upper temperature limit.

The temperature responses of the V/Li blanket for LOFA and LOCA are shown in Figs. 4.6-12 and 4.6-13, respectively, with different neutron wall loadings. The LOCA is the more serious accident because the thermal inertia of the blanket becomes much reduced. The maximum allowable neutron wall loading is about 2.7 MW/m^2 for the maximum allowable temperature of 1200°C . As discussed before, the reflector material is Fe1422 and the afterheat there is very high, as can be seen of Fig. 4.6-11. This high afterheat causes the relatively low allowable wall loading for inherent safety if V reflector is used. The allowable loading will be higher, but the blanket energy multiplication will be reduced. The detailed trade-off between safety and economics has to be carried out.

The temperature responses of the V/Flibe blanket for LOFA, LOCA, and partial LOCA are shown on Figs. 4.6-14, 15, 16 respectively. Again, the LOCA is the most serious case due to the same reason. However, a very respectable 5 MW/m^2 neutron wall loading is acceptable for this case. The main reason is

that there is no reflector for this design. The afterheat due to the V-alloy in blanket is low. For the other accident cases, a wall loading of $\sim 10 \text{ MW/m}^2$ is allowable.

From this calculation, it appears that inherent safety can be achieved for the blanket with a reasonable neutron wall loading of ~ 2.5 to 5 MW/m^2 , under a set of very conservative assumptions. By relaxing the assumptions and by careful design modifications, an inherently safe blanket with a neutron wall loading of 5 to 8 MW/m^2 should be feasible.

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4.6.2 Organic Coolants and Their Applications To Fusion Reactors

The search for simple, efficient fluids for cooling fission reactors led to consideration of organic coolants in the 1950's. Research on these fluids led to the use of terphenyl coolants in the US PIQUA and OMRE reactors. These reactors experienced coolant/moderator coking problems, and organic coolants were dropped with the successful emergence of the water-cooled reactor.⁽¹⁾

Research continued in Canada, however, and developments included separation of moderator and coolant, modifications to the base coolant, and methods for fouling control. These led to the WR-1 organic-cooled, heavy-water-moderated reactor at the Whiteshell Nuclear Research Establishment (WNRE) at Pinawa, Manitoba. This 50 MWth reactor operated successfully for 20 years, until it was shut down in 1985 for replacement by a new research reactor.⁽²⁾

In addition, an organic-cooled loop was operated in the NRU reactor at Chalk River to explore fuel and coolant performance for an organic-cooled CANDU power reactor (CANDU-OCR). Design studies in the mid-1970's indicated that CANDU-OCR's were cost-competitive with heavy-water CANDU's, and were preferred for advanced converter fuel cycles based on thorium and U-233.⁽³⁾

The search for fusion reactor coolants has also led to consideration of many fluids. Tables 4.6.2-1 and 4.6.2-2 summarize characteristics of reactor-grade terphenyls, along with other coolants. The organic is a moderate density and heat capacity fluid with a low thermal conductivity. The operating range extends from room temperature up to about 400°C at fairly low pressures (1-2 MPa). It is non-electrically conducting. Scoping experiments indicate only minor reactions with liquid lithium and lithium-lead. These properties lead to the possibility for unique design opportunities in fusion reactors.

Here, the characteristics of radiation-resistant organic coolants are described, particularly thermal-hydraulics, decomposition and fouling, compatibility and safety. Then the major potential applications of organics as blanket/first wall coolant, high heat flux component coolant, and power cycle working fluid are considered. More details may be found in Ref. (4).

TABLE 4.6.2-1
MAJOR ADVANTAGES AND DISADVANTAGES OF ORGANIC COOLANTS

Advantages

Low pressure coolant with good heat transfer, leading to simpler, more reliable mechanical designs with improved tritium breeding rate

High-temperature coolant, leading to higher thermal efficiency

Low corrosion with a wide variety of structural alloys

Low activity of the primary heat transport system due to the reduced corrosion and the low mass transport of corrosion products in the non-polar organic coolant.

Possibly compatible with lithium and lithium-lead

Simplified maintenance since leaks are self-revealing, the area around a leak becomes coated with a black material

Improved heat transfer relative to helium

Disadvantages

Flammable in air, which requires careful design and operation

Decomposes by radiolysis and pyrolysis, which requires coolant replacement and fast flow to avoid coking

Fouling of heat transfer surfaces, which requires coolant chemistry control

Reduced heat transfer relative to water

TABLE 4.6.2-2
COMPARISON OF CANDIDATE FUSION COOLANTS

Parameter	Organic Coolant ^a	Helium	Water	Draw Salt	Lithium	⁷ Li- ⁸³ Pb
<u>Typical Properties:</u>						
Density (kg/m ³)	850	3.5	800	1760	490	9330
Viscosity ($\times 10^{-4}$ kg/m-s)	6	0.3	0.8	11	3	20
Thermal conductivity (W/m-K)	0.11	0.18	0.6	0.57	46	22
Specific Heat (J/kg-K)	2500	5200	6000	1550	4200	180
Min. Temperature (C)	70 ^b	< 0	~ 0	140	810	235
Max. Temperature (C)	400 ^b	> 1000	340	~ 480	1350	--
<u>Typical Operating Parameters:</u>						
Temperature (C)	350	400	300	350	400	400
Temperature Rise (C)	100	200	40	80	250	150
Flow Speed (m/s)	10-20	20-100	3-10	1-5	0.1-1	0.1-1
Pressure (MPa)	3	5	15.2	0.4	3	2.4
Pressure Drop (MPa)	2-3	0.1	0.2	0.3	~ 3	2.2
<u>Typical Cost:</u>						
(\$/kg)	5 ^c	<< 1	<< 1	1.5	40 natural	6.2 30% ⁶ Li

^a 40% high boiler OS-84

^b Minimum practical temperature limited by increasing viscosity.

Maximum temperature limited by thermal decomposition.

^c 1985 price for small amounts (WR-1)

4.5.2.1 Coolant Characteristics

Coolant Material

Coolant screening tests indicated that terphenyl isomers had relative radiation and thermal stability, soluble decomposition products, and ready availability at low cost. The basic materials were classed under the generic name Santowax, a proprietary name of the Monsanto Company. A particular form, OS-84 (originally designated HB-40, a commercial partially-hydrogenated terphenyl mixture) was adopted as the feed material for the Canadian program.

The reasons for the radiation resistance are still uncertain. Early research identified the benzene ring structure as fairly tolerant of incident nuclear particles, presumably the energy could be rapidly shared and diluted around the ring. Under irradiation, reaction products generally are soluble benzene-ring-type compounds. Further damage tends to occur at the broken ends rather than the remaining ring structure. As a result, an initial terphenyl fluid develops into a range of molecules including gases, phenyls (e.g., benzene), biphenyls, terphenyls, quaterphenyls and so on. Once developed, this mixture is less sensitive to further radiation than the initial material, presumably some degree of equilibration is achieved between radiation and thermally-induced decomposition and recombination among the various compounds.

The steady-state composition of these organics is a function of the feed fluid and system design and operating conditions. The WR-1 fluid contains gases such as H_2 and CH_4 , phenyls (3%), biphenyls (16%), terphenyls (44%), quaterphenyls (7%) and high boilers (30%).⁽¹⁾ For fusion blankets, a 40 wt % high boiler (HB) content terphenyl-based mixture is a reasonable reference coolant, with a C/H atom ratio of 1.23 and a molecular weight of 291 g/mole.

Thermal-hydraulics

For multicomponent fluids, phase changes occur over a temperature range. For the U-3 loop fluid, the vapor pressure is 1.2 MPa at 400°C. Since the operating fluids are jet black, liquidus points cannot be easily determined. However, the proposed organics are liquid at room temperature.

The heat carrying capacity per unit channel cross-sectional area, $\dot{Q}/A$, is compared with other coolants in Table 4.6.2-3 for typical conditions. The organic is normally a high velocity coolant in order to have good heat

transfer, to minimize the "exposed" coolant volume, and to minimize fouling. Under these conditions, the heat-carrying capacity is better than even water.

The heat transfer correlation for the organics is $Nu = 0.224 Re^{0.83} Pr^{0.4}$, which is slightly better than the standard form. The heat transfer coefficient is about a factor of five less than water but twenty times greater than helium at the same velocity. Experiments for the WR-1 coolant indicate that the critical heat flux q''_{CHF} is a function of the sub-cooling, ΔT_{sub} , and the fluid velocity, v :⁽¹⁾

$$q''_{CHF} [MW/m^2] = 3.00 + 0.0075 (\Delta T_{sub} [C])^{0.8} (v [m/s])^{1.2} \quad [4.6.2-1]$$

Measured values exceed $10 MW/m^2$ at 10 m/s and 150 K subcooling.

Decomposition and Fouling

At high temperatures and in radiation fields, organic coolants can decompose, leading to high replacement costs, surface fouling or channel plugging ("coking"). These were significant problems in early organic fission reactor experiments, but were successfully resolved and demonstrated in the operation of the WR-1 reactor and the U-3 loop at the NRU reactor. The key factors were reducing the coolant in-core volume, maintaining adequate flow and strict coolant chemistry control.

TABLE 4.6.2-3
COMPARISON OF HEAT CARRYING CAPACITY OF VARIOUS COOLANTS

	ρ (kg/m ³)	v (m/s)	c_p (J/kg-K)	T_{in} (C)	T_{exit} (C)	ΔT (C)	$\rho v c_p \Delta T = \dot{Q}/A$ (MW/m ²)
Helium	3.5	25	5200	275	510	235	100
¹⁷ Li- ⁸³ Pb	9330	0.5	150	300	450	150	100
Lithium	500	0.5	4200	300	550	250	260
Water	800	5	6000	280	320	40	960
Draw salt	1760	5	1550	300	380	80	1100
Organic	855	15	2570	300	400	100	3400

The total decomposition rate is the sum of the radiolytic and pyrolytic decomposition rates. For most fusion reactor in-vessel applications, radiolytic decomposition dominates. The radiolytic rate, proportional to $(100 - \text{HB})^2$, is about 0.0096 kg/hr/kW-absorbed for a 40% HB coolant based on the WR-1 neutron spectrum. Pyrolytic decomposition is most important for bulk temperatures at or above 400 C. The pyrolytic rate, proportional to $(100 - \text{HB})$, is $1.28 \times 10^{11} e^{-180.98/RT}$ kg/h/kg-coolant, RT in kJ/mole, for 40% HB material.

Whether the radiolysis yields for fission reactors can be applied to fusion systems must be established. Since nearly 70% of the coolant dose in WR-1 is attributed to fast neutrons,⁽⁵⁾ the WR-1 coolant measured yield has been established in a radiation environment with a strong fast neutron component, and presumably with a consequent high LET level. The yield per energy absorbed can increase only if there is a significant increase in the LET effect, such that there are local high concentrations of reacting species. So the question is whether there is a higher LET effect in a fusion reactor environment than in the fission reactor coolant. As shown in Ref. (6) for water, the LET for protons, electrons, and carbon nuclei all appear to be decreasing or nearly level with particle energies increasing from 2 MeV to 20 MeV. It is reasonable to assume that the LET conditions are similar for the hydrogen-bearing organic coolant as for water. Therefore, it is plausible that the radiolysis yields for organics measured in fission reactors can be applied. However, detailed calculations of the LET effects and measurements of radiolytic decomposition would be needed to confirm this.

For low decomposition rates, a fraction of the coolant can be simply withdrawn and replaced with fresh feed material. With a vacuum distillation column, it is possible to remove the high boilers and recover the 60 wt % of the coolant that is still good. A high boiler hydrocracking process could recover up to 90% of the high boiler material for coolant outlet temperatures < 350°C, and 75% at 400°C. In this process, hydrogen is added to the high boiler material at high temperature and pressure with appropriate catalysts, and the material is converted back into usable coolant.^(1,4,7)

Since the reprocessing system is likely to be fully contained with hydrogen isotope recovery, it may be easy to convert the coolant to a deuterated form by using deuterium instead of hydrogen as feed to the hydrocracker. With a deuterated coolant, lower decomposition rates are expected

because of potential reductions in the absorbed dose (a factor of 3); the pyrolysis rates (possibly a factor of 2 at 400°C); and the radiolytic yield.

Decomposition can result in "coking" or fouling. Coking is the formation of carbonaceous deposits in low flow regions due to precipitation of decomposition products. Fouling is the formation of films on high temperature heat transfer surfaces, normally dependent on the coolant impurities. Coking is prevented by reducing the decomposition rate and maintaining high flow rates to remove decomposition products in solution before they exceed the solubility limit. Fouling is avoided through coolant chemistry control.^(1,4) From the various irradiation experiments, a summary of typical performance is presented in Table 4.6.2-4. Fouling was not observed under the heat transfer conditions and flow velocities of interest to fusion applications.

Materials Compatibility

Measured corrosion rates for various materials in organic coolants are summarized in Table 4.6.2-5. There is a substantial long-term data base at relevant operating conditions for various steel and zirconium alloys since these were the primary construction materials for pressure tubes, piping and fuel sheaths. Typical corrosion rates for the A106 carbon steel used in WR-1 and other ferritic steels are indicated. With ferritic steels, corrosion decreased with increasing alloy content. For HT-9, the penetration rate is estimated as $< 0.8 \mu\text{m/yr.}$ ^(1,4)

Coolant channels fabricated from Type 347 austenitic stainless steel were installed for WR-1 start-up and operated satisfactorily. Also, 304 L stainless steel sheathed fuel elements were irradiated with fuel sheath temperatures up to 470°C with no corrosive attack or carburization observed. Austenitic steels such as PCA or the low activation variant, Fe-15Mn-15Cr, are expected to be very corrosion resistant with penetration rates $< 0.5 \mu\text{m/yr.}$

Zirconium and its alloys are compatible with organic coolants if certain impurities, particularly chlorine, are controlled and some water concentration is maintained to repair a protective oxide film. Zr-2.5Nb pressure tubes and fuel sheaths were irradiated with surface temperatures up to 500°C with a measured corrosion rate of $6 \mu\text{y.}$ Zr-2.5% Nb is the reference material for organic-cooled fission reactor fuel sheaths and pressure tubes.

TABLE 4.6.2-4
FISSION REACTOR FOULING EXPERIENCE

Fuel type	Operating time (hrs)	T _{outlet} (C)	v (m/s)	D (mm)	Re (x 10 ⁴)	q" (MW/m ²)	ΔT _{film} (C)	Fouling failure
OCR UC fuel	6,500	400	14	6.6	16	1.5	79	None
WR-1 UC fast neutron rod	10,000	325	12	5.3	8.3	1.7	110	None
	19,000					1.0		None
WR-1 driver	8,000	400	7.5	5.3	7.5	1.7	131	None
WR-1 low flow expts.	7,940	400	4.1	4.02	3.3	0.35	41	None
	3,670	330	4.1	4.02	2.2	0.66	100	Failed

TABLE 4.6.2-5
COMPATIBILITY OF STEELS AND OTHER MATERIALS WITH ORGANIC COOLANT

Material	Organic coolant	Test temperature (C)	Test time (hrs)	Water content (μg/g)	Penetration rate (μm/yr)
<u>Ferritic steels:</u>					
A106 Carbon steel	OS-84	350-400	> 10 ⁴	700-1000	0.8
SAE-4130	Santowax	425	-	1500	1.3
Type 410 SS	Santowax	425	-	1500	0.32
<u>Austenitic steels:</u>					
Type 304 SS	Santowax	425	-	1500	0.32
Type 304L SS	Santowax	470	~ 1000	200	Clean
Type 347 SS	OS-84	350-400	20,000	700-1000	Carbon film
<u>Zirconium and others:</u>					
Zr-2.5Nb	OS-84	400	35,000	700-1000	6.0
Copper	Santowax	400	-	0-2000	12-18
1100 Aluminum	Santowax	400	-	0-2000	1.8-1100
Beryllium	Santowax	400	-	0-1000	0.6-84

There is no organic corrosion data for the proposed V-Cr-Ti alloy. However, by analogy with zirconium alloys, a Cr_2O_3 film on this V-alloy is likely to be effective in controlling corrosion or oxidation. This film may also prevent metal hydriding. The hydrogen that does penetrate the oxide film is quite soluble so that embrittlement by hydride precipitation should be less a problem for this vanadium alloy than for zirconium alloys. It is further speculated that the Cr_2O_3 film will not dissolve quickly in the matrix metal (as does oxygen in zirconium) so that less or no dissolved water will be required to maintain the protective film.

Limited data for copper, aluminum and beryllium is available. Careful tests with proper water and impurity control could indicate that copper and even aluminum might be acceptable. Aluminum gaskets operated in high temperature but stagnant regions in WR-1 without observable corrosion attack.

Safety

An assessment of the WR-1 coolant⁽⁸⁾ has established maximum permissible concentrations in air and water of 4.4 mg/m^3 and 8 mg/L respectively. Studies with rats found that the unirradiated OS-84 is non-carcinogenic and the irradiated material is slightly carcinogenic but much less so than standard industrial tar. The WNRE staff surveillance program shows no significant difference between exposed and control populations.⁽⁹⁾

The flammability of organic fluids is potentially their most serious disadvantage. However, the nineteen years of operation of the WR-1 reactor has proven that this hazard is easily managed. There are four basic processes.^(1,2,4) The first is external ignition of organic liquid. The combustion is slow but generates black smoke. The second mechanism, auto-ignition, can occur without a source of ignition. The auto-ignition temperature of the fluid is maintained higher than the bulk coolant temperature by $20\text{--}25^\circ\text{C}$. The auto-oxidation mechanism occurs where an organic soaked region, especially piping insulation, can slowly oxidize if sufficient oxygen is available and, if the heat of combustion is retained, can reach temperatures of $600\text{--}700^\circ\text{C}$. The fourth mechanism is deflagration of a vapor cloud. These are controlled by standard petroleum industry precautions including good housekeeping (detect and fix leaks promptly, remove organic-soaked insulation), maintaining components and air cool, use of flame arrest zones and

spark-free equipment, and use of CO₂ or halons for fire suppression. An inert atmosphere would virtually eliminate coolant flammability concerns.

A major advantage for organic coolants is their relative compatibility with lithium. This has led to the choice of an organic heat exchanger for cooling the FMIT lithium primary loop.⁽¹⁰⁾ Scoping tests where several grams of organic and liquid metals were mixed together (Table 4.6.2-6) indicate that the reactions were mild with little heat release.^(11,12)

The major coolant activity with the OS-84 was from impurities, particularly the As impurity in the feed coolant, carbon steel corrosion products, and ⁴¹Ar from dissolved air. The overall activity levels were low enough that hands-on maintenance of the primary system was possible during operation. There is little activity transport because the corrosion products are likely to form under a tightly-bound carbonaceous film. The corrosion products, if released, would be in carbon-coated particles with little or no activity dissolved in the fluid. These particles can be controlled by clay columns.

TABLE 4.6.2-6
EXPERIMENTAL RESULTS ON ORGANIC/LIQUID METAL REACTIVITY

First material added to	Second material	Result
2 g burning Li @ 540 C	13 g OS-84 @ 230 C	Li extinguished; oil ignited
1 g OS-84 @ 230 C	3 g Li @ 290 C	Mild reaction; carbon film on Li
1 g VP-1	3 g Li @ 290 C	Mild reaction; carbon film on Li
1 g OS-84 @ 230 C	3 g Li @ 430 C	Pressure surge; 1 mm carbon film
5 g Li @ 290 C	21 g OS-84 @ 230 C	Oil vapor, no fire.
20 g Li @ 230 C	21 g OS-84 @ 230 C	Oil vapor, no fire.
OS-84	Li @ 430 C	Mild reaction
17Li-83Pb @ 400 C	excess Therminol-66	Mild reaction
Therminol-66	excess 17Li-83Pb @ 400 C	Mild reaction
17Li-83Pb @ 400 C	excess Dowtherm-A	Mild reaction
Dowtherm-A	excess 17Li-83Pb @ 400 C	Mild reaction
Pb @ 400 C	excess Therminol-66	Almost no reaction
Pb @ 400 C	excess Dowtherm-A	Almost no reaction

4.6.2.2 First Wall and Blanket Applications

First wall and blankets are moderate heat flux environments with high neutron fluxes and large overall volume. The advantages of organic coolants (OC) for these applications is the lower pressure and higher temperature than water, better heat transfer than helium, and lack of MHD effects. The only previously reported OC blanket designs used a dual coolant system⁽¹³⁾. Single coolant blankets are considered here, based on 40% HB OS-84.

Thermal-Hydraulics and Thermal-Mechanics

The coolant flow characteristics are chosen, consistent with temperature and pumping limits, so as to maximize the heat carrying capacity of the fluid in the blanket. Increasing heat carrying capacity, and so reducing the coolant volume per input power ($\propto A/\dot{Q}$), requires increasing the flow velocity. The problems with high velocity water (over 10-20 m/s) include corrosion (or erosion), large pressure drop leading to lower boiling point and higher pumping power, and flow vibrations. However organics are less corrosive, have a high boiling point even at low pressures, and prefer velocities over 10 m/s to prevent fouling. Increased pumping power may be balanced by increased thermal efficiency from the higher temperature organic coolant, and flow vibrations may be controlled by proper stiffening. The CANDU-OCR was designed for 12-14 m/s velocities in the core,⁽³⁾ and WR-1 operated with driver fuel around 8-12 m/s.⁽²⁾

A lobed first wall/blanket module is assumed, with small coolant channels passing azimuthally through the 15 cm radius first wall as in the Blanket Comparison and Selection Study (BCSS)⁽¹⁴⁾. The assumed heating rates are 1 MW/m² peak plasma heat flux and 5 MW/m² neutron wall load. A 3 mm erodable layer allows a 3 year life at 1 mm/yr net erosion rate.

Organic coolant first wall designs are summarized in Tables 4.6.2-7 and 4.6.2-8. The thermal plus pressure stresses were less than 250 MPa at beginning-of-life for the 1.5 mm ID channel design with 2 MPa internal pressure. The lower stresses and temperatures are possible because the low coolant pressure allows thinner first walls. No first wall grooving is required since the surface temperature is below 550 C and the stresses are within ASME code limits.

TABLE 4.6.2-7
COMPARISON OF $\text{LiAlO}_2/\text{Be}/\text{FS}$ BLANKETS

Coolant	Organic		Water ⁽¹⁴⁾	Draw salt ⁽¹⁴⁾	Helium ⁽¹⁴⁾
Width (m)	0.3		0.3	0.3	0.3
Depth to shield (m)	0.6	0.7	0.7	0.5	0.7
Length (m)	2		3	3	3
Multiplier region: Be/ LiAlO_2 (%)	0.2 m 80/20		0.2 m 90/10	- 73/27	0.2 m Be rods
Peak heating:					
Surface (MW/m^2)	1		1	1	1
Bulk (MW/m^3)	50		70	50	64
1-D TBR	1.57	1.65	1.21	1.3	1.21
Energy multiplication	1.46	1.46	1.4	1.3	1.21
Volume fractions:					
Coolant (%)	3	2	-	-	-
Structure (%)	9	8	-	-	-
Breeder (%)	54	62	-	-	-
Multiplier (%)	34	28	-	-	-
Energy deposition in coolant (%)	4.8	2.7	-	-	-
Pump power/thermal (%)	3	1.4	<1	0.2	2-4
Gross thermal eff. (%)	-	-	35.7	37.5	39.2
First wall:					
Thickness (mm)	7	6.5	11.9	17	10.8
Channel dim. (mm x mm)	2x2	1.5x1.5	3.8x3.8	10x10	1x7
Flow speed (m/s)	10	10	6	1-5	60
Pressure drop (MPa)	0.25	0.4	0.2	0.3	-
Max structure temp. (C)	534	534	465	-	646
Coolant inlet temp. (C)	300	300	280	-	275
Breeder:					
Geometry	BOT	BOT	BOT	BOT	plate
Tube ID (mm)	3	2	8-10	10	-
Clad thickness (mm)	0.8	0.8	0.75-1	-	-
Minimum pitch (cm)	1.1	0.9	1.6	2.5	-
Nbr tubes/module	402	664	250	-	-
Pressure (MPa)	2.5	3	15.2	0.4	5
Flow speed (m/s)	20	15	3	1-5	23
Pressure drop (MPa)	2.1	2.1	-	-	0.13
Coolant inlet temp. (C)	320	300	280	330	300
Coolant exit temp. (C)	380	400	320	405	510
Max Be/brdr temp. (C)	700/1000		650/1000	-	430/1000
Min breeder temp. (C)	400	381	350	-	520

TABLE 4.6.2-8
COMPARISON OF LITHIUM BREEDER BLANKETS

Blanket	Li/OC/FS FW only	Li/OC/FS	Li/OC/V	Li/Li/V ⁽¹⁴⁾	Li/He/FS ⁽¹⁴⁾
Width (m)	0.3	0.3	0.3	7.8	0.3
Depth to shield (m)	0.81	0.81	0.81	0.765	1.2
Length (m)	3	2	2	3	3
Peak heating:					
Surface (MW/m ²)	1	1	1	1	1
Bulk (MW/m ³)	25	25	25	25	23
1-D TBR	1.42	1.44	1.48	1.31	1.16
Energy multiplication	1.27	1.32	1.27	1.3	1.23
Volume fractions:					
Coolant (%)	0.7	2	2	-	23
Structure (%)	8	6	6	8	7
Breeder (%)	91	92	92	92	70
Energy deposition in coolant (%)	1.9	3.2	3.0	~92	~0
Pump power/thermal (%)	-	-	-	-	-
Gross thermal eff. (%)	-	-	-	42.3	39.2
<u>First Wall:</u>					
Thickness (mm)	7.0	6.5	6.5	65	10.8
Channel dim. (mm x mm)	2 x 2	2 x 2	2 x 2	24 x 45	1 x 7
Flow speed (m/s)	10	10	10	1	60
Pressure drop (MPa)	0.25	0.25	0.25	1	-
Max structure temp. (C)	534	534	534	-	649
Coolant inlet temp. (C)	300	300	300	~300	275
<u>Breeder:</u>					
Geometry	pool	BOT	BOT	self-cooled	BIT
Tube ID (mm)	NA	8	8	NA	45
Clad thickness (mm)	NA	1	1	NA	1.1
Minimum pitch (cm)	NA	5.3	5.3	NA	5.1
Nbr tubes/module	0	32	32	0	-
Pressure (MPa)	1-2	1.5	1.5	3	5
Flow speed (m/s)	-	20	20	0.1-0.3	-
Pressure drop (MPa)	~ 1	1.0	1.0	~2	0.14
Coolant inlet temp. (C)	-	260	310	300	330
Coolant exit temp. (C)	-	350	400	550	510
Max breeder temp. (C)	-	900	950	550	560
Max structure temp. (C)	-	550	600	690	515
NA - Not applicable					

Tritium recovery is assumed similar to that in the Li/He/FS BCSS blanket, where the lithium is circulated slowly. The breeder-inside-tube Li/He/FS configuration analyzed in BCSS had a 1 MPa lithium pressure drop (tokamak) with a 0.3 kg blanket tritium inventory.

Table 4.6.2-8 summarizes possible Li/OC blankets. The maximum breeder temperature is 900°C, and is limited by the large heat flux per channel leading to large film temperature rises and consequently high structure and/or low coolant inlet temperatures. With ferritic steel, the design limit is the maximum temperature for mechanical strength and lithium compatibility (about 550°C). If the organic is compatible with vanadium at temperatures up to 600°C, then the larger allowable structure temperature (750°C for mechanical integrity, lithium compatibility) allows a 400°C coolant outlet temperature.

Neutronics and Tritium Breeding

One-dimensional ANISN calculations were made to determine heating profile, energy multiplication, tritium breeding and coolant energy deposition. A 14 cm reflector is used to shield the large volume of plenum coolant.

The OC designs presented here have good 1-D tritium breeding ratios (TBR) ranging from 1.4-1.6. Changing from ferritic steel to vanadium increased the TBR by 0.08, but changing the coolant (not its volume) to water or deuterated organic changed the TBR by less than 0.01. In the solid breeder blankets, decreasing the ^6Li enrichment from 90% to 50% also did not affect the net TBR. These TBR's are aided by the decreased structural volume that is a consequence of minimizing the coolant volume and the low coolant pressure.

The direct deposition of radiation energy into the coolant ranged from 1.9% of the incident neutron energy (0.3 MeV/n) for an organic-cooled first wall (including rear plenum absorption) on a Li/OC/FS blanket; to 3.5% (0.5 MeV/n) for a Li/OC/FS blanket actively cooled by the organic; to 4.8% (0.7 MeV/n) for a $\text{LiAlO}_2/\text{OC}/\text{FS}/\text{Be}$ solid breeder blanket. Typically about 0.1 MeV/n is deposited in the first wall region where the neutron flux is highest. However, the largest single contribution (0.1-0.2 MeV/n) is in the rear plenum where a large volume of slower fluid is needed to supply all the blankets without an unreasonably large pressure drop.

The $\text{LiAlO}_2/\text{H}_2\text{O}/\text{FS}/\text{Be}$ blanket was a highly rated BCSS solid breeder concept. The key questions relate to tritium recovery from the breeder, reliability of multiple high-pressure coolant tubes, tritium control in the coolant, and maintaining the breeder temperature within operational limits. Replacing water with an organic could yield a more attractive design since an organic-cooled version could operate at lower pressure and higher temperature.

Representative OC designs are summarized in Table 4.6.2-7. Achieving under 3% direct energy deposition in the coolant requires many small tubes with high speed flow. At 2-3 MPa, OC blankets are still much lower pressure than water-cooled systems. This alleviates the coolant tube reliability concerns somewhat, although it is offset by the increased number of tubes. The higher minimum breeder temperature without gap resistance ($10,000 \text{ W/m}^2\text{-K}$ assumed) leads to a much lower tritium inventory.

Self-cooled liquid metal blankets are attractive because of their design simplicity. The main concerns are removing high surface heat fluxes in a magnetic field, and materials compatibility. An alternative is to keep the liquid metal as a quasi-static breeding material and rely on another fluid for heat transport. Again, it is possible that organic coolants would yield a more attractive blanket since organics would provide good heat transfer, keep structural temperatures below liquid metal compatibility limits, and have limited reaction with the liquid metal in the event of breeder/coolant mixing.

Lithium is used the present calculations because of its good breeding, structural compatibility, density (reduced static pressures) and tritium recovery. The BOT and lobed first wall configuration is used as with the solid breeder blanket. Minimizing the organic volume requires allowing high breeder temperatures. The practical limit depends on whether the hot breeder can come into contact with structure. In the lobed module concept, the first wall and back surfaces are directly cooled, and the module end plates are indirectly cooled by the high density of cooling tubes as they turn from their axial position towards the rear manifolds. There are three possible circulation mechanisms - forced circulation for tritium recovery, natural circulation and toroidal magnetic field transients. However, the first is subject to design control, natural convection will be suppressed since the ratio of buoyancy force to magnetic force is $\text{Gr}^{0.5}/\text{Ha}^2 \sim 10^{-5}$, and the field transient concern should be limited by the high lithium thermal conductivity.

These energy deposition rates are much lower than the 2.2 MeV/n estimates in Ref. (13) and indicate the importance of optimizing the design. Some further reductions could be obtained if the coolant can be pushed through the blanket at higher speeds or in smaller tubes, but the most useful design effect would be achieved through careful design of the reflector and plenum regions. A larger reduction in energy deposition by about a factor of three could be obtained if a deuterated organic is used. The nitrate draw salt considered in BCSS has a similar energy deposition rate to the hydrogenated organic.

Decomposition and Fouling

Based on the neutronics calculations, and since the first wall neutron load is about 60% of the reactor power (energy multiplication of 1.3), then the radiolytic decomposition rate is 0.12-0.30 kg/h/MWth or 600-1500 kg/h for a 5000 MWth reactor. This decomposition rate per unit coolant volume per pass is not large ($< 1\%$) as compared with the 40% high boiler content of the coolant. The pyrolytic decomposition rate ranges from < 10 kg/h for the first wall only, to ~ 100 kg/h for blankets with 400 C exit temperatures.

At these decomposition rates, it would be worth adding a hydrocracker to recover as much of the coolant as possible. Assuming 90% recovery efficiency, the net decomposition rate is about 60-160 kg/h for these designs. If deuterium gas feedstock is used in the hydrocracker, then the coolant will be converted into a deuterated organic within a short period from startup and the gross decomposition rate could decrease by a further factor of three.

Tritium Recovery and Control

Tritium recovery will be through purging the solid breeder with helium or by circulating liquid lithium to an external extractor as in the BCSS LiAlO_2 and Li/He blankets, respectively. The major differences are that the higher solid breeder temperature (380-400°C minimum) reduces the inventory from 2.3 kg to 0.3-0.6 kg, while the lithium blanket may have slower circulation velocity and reduced pressure drop due to the larger lithium volume.

For an organic coolant with an oxide layer on the channel surfaces, the estimated tritium permeation rate is 0.1-1 g/d in a 5 GWth reactor.⁽¹⁴⁾ Since tritium is likely to equilibrate with the coolant hydrogen, the simplest

tritium removal system would be based on the coolant processing system. If the tritium can be removed with the decomposed coolant, then the permeation rate is 1-100 Ci/d, depending on the tritium input and coolant removal rates (Fig. 4.6.2-1), and the coolant tritium inventory is similarly 3-300 g. Such a coolant reprocessing/tritium recovery/waste treatment system appears to combine standard chemical engineering and tritium processing technology, but no detailed design was attempted here.

Safety

The general concerns relating to toxicity and flammability have been discussed previously. In the 19 years of operating experience with organics at WR-1 and organic loops at NRU, these have not proven to be problems. The use of inert atmosphere, remote handling, and tritium tight hardware may be necessary for other reasons, and would substantially reduce remaining concerns. The high tritium retention is advantageous in that the coolant would tend to hold onto any loose tritium, but a disadvantage for waste treatment systems. The low pressure, low chemical reactivity and low corrosion product transport are attractive features of organic systems.

Economics

For the blanket designs presented, net decomposition rates of 20-400 kg/h are expected in a 5 GWth reactor. Cost estimates for the hydrocracker system are about 3 M\$.⁽⁴⁾ Including tritium and general coolant chemistry control, then overall blanket coolant processing, purification and replacement costs are similar to those estimated in BCSS - 10 M\$ for coolant purification and 10-40 M\$ for tritium recovery and purification. For a 5 GWth reactor, the annual costs including coolant replacement and operation of the hydrocracker are estimated as 3 M\$/yr, or 0.3 mil/kWh at 34% net electrical efficiency.⁽⁴⁾

A complete assessment of the reactor costs was not attempted. However, in the BCSS cost of electricity (COE) estimates, the variation among the top-ranked blankets was less than 20% in spite of a wide diversity of design concepts, and probably within the uncertain in the estimate. On the basis of the information presented here, it is probable that an organic-cooled blanket could be designed with a similar overall cost of electricity.

Figure 4.6.2-1 Tritium permeation from organic coolant across steam generator.

4.6.2.3 High Heat Flux Applications

Organic fluids such as the hydrogenated terphenyls offer reasonable heat transfer capabilities and compatibility with liquid metals. The heat transfer coefficient is lower than water, but much larger than helium. The Nusselt number is slightly better than the standard Dittus-Boelter correlation predicts. There is substantial operating experience at $1\text{--}2\text{ MW/m}^2$ with the WR-1 reactor driver fuel bundles at L/D of 100-500 and 8-12 m/s.⁽²⁾ The critical heat flux (WR-1 type coolant, OS-84 with 30% high boiler) is over 10 MW/m^2 at 100°C subcooling and 15 m/s velocity. The onset of subcooled boiling is about 30°C above the saturation temperature, and the vapor pressure at 400°C is about 1 MPa. The operating temperature range is about $100\text{--}400^\circ\text{C}$.

In high heat flux (HHF) applications, cold inlet conditions are desirable to maximize the allowed temperature rise. Although reactor-grade organics do not solidify at room temperature, the viscosity of 40% high boiler (HB) OS-84 becomes large at low temperatures, and 5% HB is assumed here. This implies some penalty in decomposition rate, but it is anticipated that the amount of coolant exposure to neutrons is limited in HHF components. Decomposition, tritium control and safety have been described in Sections 2 and 3.

In order to compare the HHF performance of organics with other coolants, an analysis of the maximum heated channel is provided here under comparable design conditions. The common design constraints are: 1) 5 MPa inlet pressure; 2) uniform heat flux; 3) 550°C maximum temperature at the coolant/-structure interface; and 4) 1 cm ID channels. These constraints are more a function of the application than of the coolant.

Helium, water and the organics performance are calculated with full property variations and compressible flow effects. Only single phase coolants are considered. Water is assumed limited to 15 m/s, consistent with general industrial practice. Organics are allowed up to 20 m/s based on their reduced corrosion characteristics relative to water and on operating experience up to 14 m/s. Helium velocities are ultimately limited by choking at high Mach numbers. A practical limit is 1/3 sonic speed, or 300 m/s at 5 MPa. Velocity limits for lithium are usually dominated by MHD forces.

Two illustrative lithium cases are considered. In the first case, operating with a transverse magnetic field of 5 T leads to coolant velocities under 1 m/s with a 5 MPa pressure drop in 1 m (0.07 wall conductivity ratio, as in a 1 mm steel wall). The heat transfer estimate includes entry length effects and a 50% increase in Nu based on the effects of the magnetic field on the velocity profile at fully-developed flow conditions.⁽⁴⁾ The second case with flow parallel to the magnetic field allows higher velocity in the HHF channel, but raises other concerns. For example, the pressure drop would be limited by the flow outside the HHF channel itself. Heat transfer would be dependent on the details of the geometry due to the possibility of wall jets, non-uniform velocity profiles and long entry lengths. As a rough estimate, the second case assumes 10 m/s velocity and Nu = 25.

Figure 4.6.2-2 and Table 4.6.2-9 shows the results of the calculations. Organics are seen to handle higher heat fluxes than lithium or helium coolant, and are comparable to water below about 8 MW/m² uniform heat flux. Above 8 MW/m², under the assumptions here, the film temperature rise becomes too large for the organic (and for helium). Flow augmentation mechanisms would not clearly give a relative advantage to any coolant. Such methods also increase pressure drop so might cause a reduction in the maximum heated length if the pressure drop leads to choked flow or boiling sooner than the increased heat transfer improves the film temperature drop.

TABLE 4.6.2-9
HIGH HEAT FLUX COMPARISON AT 5 MW/m² and 1 m^{2a}

Coolant	Velocity (m/s)	Channel pump power (kW)	Pressure drop (MPa)	Channel length (m)	Total pump power (MW)	Number channels
H ₂ O	15	1.1	0.56	6.2	0.036	16
He	250	128	2.1	1.5	8.45	66
He	--	10	0.4	0.4	2.50	250
Organic	20	4.7	1.9	5.1	0.093	20
Li (⊥ B)	3	1.2	5.0	0.3	0.39	330
Li (B)	10	--	--	2.9	--	34

^a 100 m cooled length (1 m² area), 5 MPa inlet pressure, 1 cm ID tubes

Figure 4.6.2-2 Comparison of heat removal capabilities of various coolants in uniformly heated 1 cm ID tubes at 5 MPa inlet pressure and 550°C maximum surface temperature.

4.6.2.4 Power Cycle Applications

Intermediate heat exchanger

An intermediate heat exchanger (IHX) loop between the primary blanket coolant and a steam power cycle may be necessary to reduce chemical reactivity and tritium leakage. An organic IHX offers several advantages. First, it appears reasonably compatible with all major primary coolants, particularly liquid metals on the blanket side and water on the steam side. This compatibility led to the choice of an organic IHX for FMIT⁽¹⁰⁾ and TASKA.⁽¹⁵⁾

Secondly, an organic coolant offers good tritium control. With a liquid metal primary loop, there will only be one oxide barrier (assuming single-walled heat exchangers) between the primary and the steam cycle if there is a sodium IHX or no IHX. An organic would allow three oxide barriers. In addition, tritium would be expected to remain in the organic due to the high solubility of H_2 and H_2O , and by isotopic exchange with the hydrogen in the organic. Finally, because of its high boiling point at low pressures (as with sodium), the organic IHX would be at lower pressure than the steam cycle so leaks would tend to be into the organic.

The primary disadvantage of organics relative to sodium is thermal decomposition. For pure OS-84, the boiling point is 400°C at 0.2 MPa and 500°C at 1 MPa. However, decomposition prevents using the higher temperatures. At 500°C, the decomposition rate is roughly 3 kg/hr/kg coolant for OS-84, while it is reduced to 0.1 kg/hr-kg at 400°C. OS-84 is a partially saturated compound which is desirable for a radiation environment, but unsaturated compounds exhibit more thermal stability. For example, saturated terphenyls have a thermal decomposition activation energy of 230 kJ/mole as compared with 180 kJ/mole for OS-84.⁽¹⁾ Consequently, this disadvantage may be reduced by proper choice of fluid and by minimizing the hot coolant volume.

In the OS-84 cooled IHX design for TASKA, the thermal decomposition was calculated as 1.4 kg/hr, and the equilibrium tritium inventory at 11 Ci/kg or 15 g total. Several power cycle arrangements were considered for this 45 MWth device. For a lithium-lead temperature ranging from 350-450°C and the OS-84 from 321-421°C, a power cycle was defined with a net electrical conversion efficiency of 39%. In the FMIT organic IHX, 3.5 MW is removed from the

lithium through a conventional baffled, multi-pass, tube-and-shell heat exchanger. The lithium was cooled from 270°C to 220°C.

High Temperature Power Cycles

Organics can also be used directly as the working fluid in thermodynamic cycles such as Rankine power cycles (as in the standard water/steam power generation system). The advantages of organics include those mentioned above plus others such as a higher boiling point. For example, the compatibility of organics with liquid metals and their tritium retention characteristics may make it possible to go directly from a liquid metal primary coolant to a power generation system with no IHX to reduce efficiency. And the higher boiling point in principle allows heat to be transferred more efficiently at a constant higher temperature. The disadvantages of organics are again their reduced thermal stability at very high temperatures and their reduced heat capacity relative to water which increases the required mass flow rate.

One particular cycle which has received considerable attention is a supercritical toluene cycle at 370°C and 4.7 MPa. Versions of this cycle for high-temperature industrial waste heat recovery are currently available. For example, Sunstrand Corp. has four 0.6 MWe Organic Rankine Cycle (ORC) engines in use.⁽¹⁶⁾ There is also interest in ORC engines for space nuclear systems. A general characteristic of these applications is the single-pass high-speed turbines used to minimize equipment complexity. These particular engines may not be the appropriate route for electric utility primary power generation because of the high turbine speeds and lower efficiency.

Low Temperature Power Cycles

The third power cycle application utilizes organics to recover low-grade heat. This could be important in fusion reactors where high heat flux components may require separate coolant loops with relatively low temperatures, or where there is generally several sources of low-grade heat.

Since some organics have low boiling points, they could be used in simple power cycles to recovery some of this energy. For example, Freon-114 boils at 95°C. ORC engines with 5-6% net electrical efficiencies are being developed to provide electricity cogeneration with AECL SLOWPOKE heating reactors.⁽¹⁷⁾

4.6.2-5 Summary

Organic coolants offer a unique set of characteristics for fusion applications. The advantages include high temperature (400°C) but low pressure (2 MPa) operation, limited reactivity with lithium and lithium-lead, reduced corrosion and activation, good heat transfer capabilities, no MHD effects, and an operating temperature range that extends to room temperature. The major disadvantages are decomposition and flammability. However, organics have been extensively studied in Canada, including nineteen years with an operating 60 MWth organic-cooled reactor. Proper attention to design and coolant chemistry controlled these potential problems to acceptable levels. This experience provides an extensive data base for design under fusion conditions.

The decomposition rates anticipated by simple extrapolation are very large due to the high fraction of primary energy transported by neutrons in fusion reactors. Even if the cost of replacing the decomposed coolant were tolerable, serious concerns over fouling and waste disposal would remain. However, simple estimates do not properly reflect the unique features of organics and their possible applications to fusion. It has been the intent of this report to carefully consider organic coolant characteristics and optimize both the applications and the designs to minimize decomposition.

For blanket coolant applications, designs are presented that are based on attractive BCSS blanket concepts, yet have reduced gross decomposition relative to simple coolant substitution estimates by factors of 3-10 through choice of concept and careful design, have reduced net decomposition by a further factor of up to 10 through hydrocracking technology, and could reduce gross decomposition by a further factor of 3 through deuterium gas addition. The result is blanket and first wall designs that take advantage of organic features, yet have decomposition-related costs that are a small fraction of the overall cost of electricity. For example, organic/lithium/ferritic steel blankets are possible in high-field tokamaks with high surface heat fluxes.

The analysis presented here was most detailed for blankets, but the conclusions with respect to thermal-hydraulics, thermal-mechanics and decomposition are relevant to other applications. In particular, organics are probably best used in high heat flux applications such as first walls or limiters (e.g., 5 MW/m²) and in intermediate heat exchanger loops.

APPENDIX A: Coolant property correlations

The properties of reactor organic coolants are a function of the feed material, operating conditions, and coolant processing system. The following correlations are for HB-40 fluid operating in a reactor system such as WR-1 at a typical volatiles content of 3-5 wt %.⁽¹⁾ The units are T in C, HB (high boiler) in wt %, ρ in kg/m³, μ in N-s/m², c_p in J/kg-K, and k in W/m-K.

Density: 25-400°C, 0-43 wt % HB

$$\rho = 1018.1 - 0.73891 T + 2.2343 HB + 0.0016670 T HB$$

Viscosity: 150-400°C, 0-45 wt % HB

$$\ln \mu = -11.626 + 0.026269 HB - 0.00053368 HB^2 + \frac{2185.6}{T+273} - \frac{11.56 HB}{T + 273} + \frac{0.54814 HB^2}{T + 273}$$

Specific heat: 100-300°C, 0-45 wt % HB

$$c_p = 1504.9 + 3.5476 T + 0.50183 HB - 0.019283 T HB$$

Thermal conductivity: 125-400°C, 0-40 wt % HB

$$k = 0.12677 - 1.0864 \times 10^{-4} T + 4.8620 \times 10^{-4} HB + 7.8045 \times 10^{-8} T HB$$

In addition, the latent heat of vaporization is about 200 J/g around 300°C and for 1-50% vaporization; the heat of combustion is around 40 kJ/g; and the dielectric constant is about 2.1 at 300°C, 0 wt % HB (HB-40), and 10²-10⁴ Hz. The solubility of various materials in the organic are available^(1,4). The solubility of H₂ and CH₄ are particularly high. Water is also quite soluble at high temperature, which allows fairly high dissolved concentrations without bubble formation on heated surfaces.

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4.6.3 Beryllium Assessment

Beryllium metal has often been suggested for use in fusion reactor blankets as a means of increasing a reactor's performance by increasing the tritium breeding ratio. At the same time, the extra neutrons are being absorbed in the lithium, there is the potential to make use of the extra neutrons to increase the energy output of the blanket, thus achieving a double benefit. It may be possible to boost the energy output for a given blanket configuration by more than 40% by including large thicknesses of beryllium as an energy multiplier. If the costs of adding large amounts of beryllium to the blanket are less than the benefits obtained, then there is the possibility that the overall cost of electricity for a reactor system utilizing beryllium energy multiplication would be less than that for a more traditional blanket design without beryllium.

The disadvantages of using beryllium in fusion reactor blankets have been discussed previously.⁽¹⁻³⁾ Specifically, these studies have mentioned six primary concerns in the avoidance of using large quantities of beryllium in blanket designs as listed in Table 4.6.3-1. This section will suggest possible solutions to each of the major issues, list the potential benefits of utilizing beryllium metal as an energy multiplier, and arrive at a qualified justification for using large amounts of beryllium in a reactor blanket for neutron and energy multiplication enhancement.

TABLE 4.6.3-1
ISSUES IN THE USE OF BERYLLIUM FOR FUSION REACTOR BLANKET APPLICATIONS

Major Issues	Other Concerns
1. Resource Limitations	1. Tritium Release
2. Irradiation Induced Swelling	2. Salt Compatibility
	3. Toxicity
	4. Efficient Recycling

4.6.3.1 Resource Limitations

The resource issue is probably the most restrictive constraint on the use of beryllium in fusion reactor blankets and was the primary reason for the rejection of its use in large applications in the past. Recently, McCarville et al.⁽³⁾ have reassessed this issue and conclude that, even by considering only the known reserves of, "it appears reasonable to consider beryllium multipliers for the first and subsequent generations of fusion breeder reactor service, but close attention to beryllium exploration, beryllium lifetime, and recycle losses will be required". Since the mass of beryllium which they consider for the reference fusion breeder (almost 900 MT) is approximately the same as the mass of beryllium in a 0.5-m thick beryllium zone (90% theoretical density, 90% volume fraction) surrounding a 0.6-m first wall with a length of 150 m, the conclusions for a pure fusion reactor should not change. However, liberalization of the amount of reserves greatly reduce the concerns for the availability of sufficient beryllium beyond the 60-200-year fusion economy duration assumed by McCarville et al.

One of the problems in estimating the reserves and possibly recoverable resources of beryllium is that there is very little pressure to search for more of the metal because domestic and imports of beryllium are sufficient to cover the small demand. If a fusion reactor economy were to begin, beryllium prospecting would increase and undoubtedly increase the reserve estimates significantly. Erickson⁽⁴⁾ provides a quantitative evaluation to substantiate this claim. He reports that the potential recoverable resource for most elements in the earth's crust should approach

$$R = 2.45 A \times 10^6$$

where A is the abundance of material in the earth's crust expressed in grams per metric ton or parts per million and R is the resource expressed in metric tons. He also finds that the reserves of metals which most closely approach this potential recoverable resource are the metals which have been sought after for the longest time, namely lead, copper, zinc, silver, gold, and molybdenum. This can be seen in Table 4.6.3-2.

It has been traditional to base future (i.e., 50 years hence) use of beryllium and the amount of reserves available upon the U.S. Bureau of Mines

TABLE 4.6.3-2.
ABUNDANCE-RESERVES-RESOURCES RELATIONSHIP IN U.S. AND EARTH CRUST⁽⁴⁾

Element	UNITED STATES			WORLD		
	Reserve ^a (MT × 10 ⁶)	Recoverable Resource Potential ^c (MT × 10 ⁶)	Ratio of Potential to Reserve	Reserve ^b	Recoverable Resource Potential ^c	Ratio of Potential to Reserve
Lead	31.8	31.8	1.	0.54	550.	1000
Copper	77.8	122.	1.6	200.	2120.	10
Zinc	31.6	198.	6.3	81.	3400.	42
Silver	0.05	0.16	3.2	0.16	2.75	18
Gold	0.002	0.0086	4.1	0.011	0.15	14
Molybdenum	2.83	2.7	1.	2.	46.6	23
Beryllium	0.073	3.7	50.	0.016	64.	4000
Beryllium	0.025 ^d	3.7	148.	0.38 ^d	64.	170

^aU.S. Bureau of Mines (1970).

^bU.S. Bureau of Mines (1970), does not include U.S. reserve.

^cRecoverable resource potential = $2.45 A \times 10^6$ (abundance A expressed in g/mt).

^dU.S. Bureau of Mines (1982)⁽³⁾

reserve figures. However, after looking at Table 4.6.3-2 it is reasonable to assume that if prospecting and searching for beryllium were to increase to a large extent and lower grade ores of beryl and bertrandite are used (for example, at present the beryl ore is hand picked and only modules greater than about 1 in. in diameter are processed and the remaining beryl is ignored) the then USBM reserve estimates could be increased by a factor of five or more. This would reduce the ratio of resource potential to reserves for beryllium for the most conservative estimates (USBM-1982) to about 30. This is still a factor of 5 to 10 greater than the ratio which is seen for the more sought after lead, copper, gold, silver, etc. Therefore, since Ref. 4 concludes that, with adequate recycling of the beryllium, there is sufficient reserves for the first few generations of fusion reactors, then increasing the reserves by simply searching for more beryllium would increase the number of fusion reactors which could use beryllium five fold.

4.6.3.2 Irradiation Swelling

The second critical issue involved with the use of beryllium as an energy multiplier is the amount of radiation induced growth due primarily to helium bubble swelling.⁽³⁾ The formation of helium gas bubbles arises from precisely the same nuclear reaction which allows for increased tritium breeding and energy multiplication;

A model has been described by Wolfer and McCarville⁽⁵⁾ which accounts for the experimentally observed helium bubble swelling of beryllium exposed in fission reactor environment. The result of their analysis shows that the swelling, $\Delta V/V_0$, is equal to the largest of

$$\frac{\Delta V}{V_0} = [0.58 \pm 0.25] C_{\text{He}} \quad [4.6.3-1]$$

or

$$\frac{\Delta V}{V_0} = 100 \left(\frac{3}{4\pi N} \right)^{1/2} \left(\frac{C k_B T}{2\gamma} \right)^{3/2}, \quad [4.6.3-2]$$

where C_{He} is the helium concentration in the beryllium in atom %, C is the number of atoms of He per cm^3 of beryllium, k_B is Boltzmann's constant, T is the absolute temperature, γ is the helium bubble surface energy, and N is the helium bubble density found empirically to be

$$N = 1.4 \times 10^{14} \exp(0.41 \text{ eV}/k_B T) \quad [\text{cm}^{-3}] . \quad [4.5.3-3]$$

It is found that the best fit to the data is found when a value of 2 J/m^2 is used for the surface energy, γ , as seen in Fig. 4.6.3-1. The upper limits for swelling in beryllium is reached when the driving force for swelling, the formation of helium bubbles, is lost after the helium gas is vented. This is likely to occur in one of two ways depending upon the helium migration in beryllium which in turn depends on the temperature. At relatively high temperatures ($>650^\circ\text{C}$) helium bubbles will migrate most easily along grain boundaries and eventually form interconnected grain-edge tunnels to vent the helium. In this case it is estimated that intergranular grain-boundary bubble behavior will be important and limit the maximum swelling to less than 5-12%, $\Delta V/V_0^2$.

For irradiation conditions less than 650°C the helium atom diffusion is much slower so that bubbles will remain within grains and will be small, isolated, and generally dispersed. In this case, a rapid heatup of the helium containing beryllium could cause a significant amount of swelling, up to the geometrical limit of 33% $\Delta V/V_0$. At this point the bubbles within the grains will grow to touch each other to form interconnected porosities and the helium will be vented and the swelling will cease.⁽²⁾ However, one additional concern should be stated here. That is, if a blanket is operating at low temperature ($<650^\circ\text{C}$) for an extended period of time (long enough to generate sufficient helium) then any power and temperature excursion in the blanket due to plasma overpower, loss of coolant, or loss of flow could conceivably cause the contained beryllium to be rapidly exposed thereby causing potential damage to the blanket. This damage could be caused by the disintegrating and crumbling of the beryllium metal, thus allowing possible open, unshielded, or hot spot sectors to form, or by the formation of stresses on any cooling or structural components within the blanket causing deformation or kinking of coolant tubes which could lead to potential hot spots and melting. Additionally, since beryllium swelling is anisotropic great care must be taken to allow for preferential swelling in the case of extruded beryllium components.

Figure 4.6.3-1 Swelling model results of Wolder and McCarville.⁽⁵⁾

If one looks at beryllium blankets operating over a temperature range from 300 to 700°C, exposed to first wall neutron fluences of around 5 to 1 MW-y/m² (this corresponds to about 20,000 to 32,000 appm of helium generation) then it can be seen from Fig. 4.6.3-1 that Eq. [4.6.3-2] above can be used to describe the amount of swelling and the dependence of the swelling on the helium concentration goes as C^{3/2} or the swelling can be written as

$$\frac{\Delta V}{V_o} = a C^{3/2} \quad [4.6.3-4]$$

for a constant temperature. The constant a contains the temperature dependence of the swelling. It can be observed that the spatial dependence of the helium production rate will fall exponentially through a thick beryllium zone so that the helium concentration throughout the blanket can be expressed as

$$C = C_o e^{-bx}, \quad [4.6.3-5]$$

where C is the helium atom concentration (cm³) at a distance x (cm) into the blanket, C_o is the helium atom concentration at the blanket surface facing the plasma, and b is the rate of decay or decay constant for the falling of the helium concentration (cm⁻³). If these expressions, [4.6.3-4] and [4.6.3-5] are combined, then we find

$$\frac{\Delta V}{V_o} = a C_o^{3/2} e^{-3/2bx} \quad [4.6.3-6]$$

or if we normalize the swelling throughout the blanket to the swelling at the first wall we find

$$\frac{\Delta V}{V_o} = S_o e^{-3/2bx} \quad [4.6.3-7]$$

where S_o = ΔV/V_o at the first wall or surface of the beryllium breeding blanket. Figure 4.6.3-2 shows the dependence of the local swelling, normalized to a first wall swelling, S_o, equal to one for three different values of the decay constant, b. One can easily see that for large values of the decay constant that the local swelling falls off very rapidly to less than 1% of the first wall swelling in only a few centimeters. Also, for very thick regions of beryllium it can be seen that the swelling at the back of the blanket will

Figure 4.6.3-2 Normalized local swelling. (Normalized to the value of the swelling at the surface closest to the plasma.)

be at least an order of magnitude less than that at the front surface, even for very low decay constants. Typical values found for thick zones of beryllium are in the range 0.08 cm^{-1} to 0.12 cm^{-1} so that the middle curve for $b = 0.1 \text{ cm}^{-1}$ will be representative of the swelling in many thick, high volume fraction beryllium blankets. One also should note that the swelling falls off faster than even the helium concentration profile. This can be seen in Fig. 4.6.3-2 and also from Eq. [4.6.3-7]. The extra $3/2$ factor in the exponential arises from the model of Wolfer and McCarville. This result shows that even if there will be a large amount of swelling in the first few centimeters of a thick, high volume fraction beryllium blanket there will be very little, or negligible, swelling deeper into the blanket.

Another way to look at this is to consider the average amount of beryllium swelling across the blanket zone. This can be accomplished by taking the volume average of Eq. [4.6.3-7] across the blanket (assuming a homogeneous blanket). For simplicity we will assume the blanket to be a cylindrical shell of inside radius r_i , outer radius r_o , thickness t , and length l .

For this case, then the volume element is $dV = 2\pi r dr$, but we will change variables to integrate over the distance in the blanket x to get

$$\left(\frac{\Delta V}{V_0}\right) = \frac{2 S_0}{2r_1 t + t^2} \left\{ \frac{2}{3b} \left[r_1 (1 - e^{-3/2 bt}) - t e^{-3/2 bt} \right] + \frac{4}{9b^2} (1 - e^{-3/2 bt}) \right\}. \quad [4.6.3-8]$$

Equation 4.6.3-8 is plotted in Fig. 4.6.3-3 as a function of the depth into the beryllium breeding zone, t , for the three different decay constants; 0.05 cm^{-1} , 0.10 cm^{-1} , 0.50 cm^{-1} . As one would expect, the faster that the helium concentration falls off, the smaller the overall amount of swelling that takes place. Again these curves are normalized to a first surface swelling of 1.

Figure 4.6.3-3. Volume-averaged swelling. (Normalized to the value of the swelling at the surface closest to the plasma.)

As an example, let us assume that the first surface swelling, closest to the plasma, is the maximum allowable or 33% at the end of the blanket's useful

service lifetime. This means that somewhere in the blanket there must be an allowance for this additional space. This is often done by assuming a reduction in the beryllium volume fraction, or number density by 33%. If we have a thin beryllium zone, say 6 cm, in front of a tritium breeding zone, then we must leave a void volume fraction of approximately 21% at the beginning of life to accommodate swelling for a decay constant of 0.1 cm^{-1} . However, if our beryllium zone is 50 cm thick (we could utilize a lithium bearing coolant within the beryllium zone and breed sufficient tritium at the same time and eliminate the need for a separate breeding zone) we would need only to allow for a 3-4% void fraction within the beryllium. This, however, assumes that the beryllium will swell to fill the 3-4% void without damaging or unnecessarily stressing any structure or cooling tubes which run through the blanket.

The conclusion from this analysis and discussion is that even if the local swelling reaches $30\% \Delta V/V_0$ at the front of a thick beryllium zone used for energy multiplication at the end of life, the average amount of swelling will be fairly small and only a small amount, on the order of 3-5%, of void space needs to be included at the beginning of blanket lifetime. This should allow thinner blankets to be designed.

4.6.3-3. Other Concerns

Four other problems arise in the consideration of large amounts of beryllium as an energy multiplier: concerns about the release of tritium, compatibility with molten salts, beryllium toxicity during fabrication operations, and the design of efficient recycling systems. Each of these could be of significant concern to a blanket designer; however, it is not felt that any one of them, by itself, would be sufficient to eliminate the use of beryllium as an energy multiplier. In a few of the cases it is the unavailability of data which causes the concern. If data becomes available it is possible that these concerns would vanish.

Beryllium, when bombarded by energetic neutrons, will produce small quantities of tritium. The amount produced is small compared to the amount needed to fuel the reactor, and is small when compared to the amount produced in the rest of the blanket, however the amount produced is large enough to be a safety concern. The primary questions which need further research are: How does the tritium behave after it is produced in the beryllium? Does it diffuse out of the beryllium or is it tied up inside the blanket? How is it

released during an accident? These questions have received little attention and need to be answered to determine the complete feasibility of beryllium as an energy multiplier. Especially important will be the exposure of beryllium to high fluences of energetic ($E > 1$ MeV) neutrons. It is possible that the tritium which is formed will be released along with the helium as it diffuses out of the beryllium, or as it escapes as the beryllium swells. This tritium can then be handled by the blanket tritium processing system along with the already large quantities routinely generated.

Another concern which has not received too much study is the salt compatibility issue. Some blanket designs have been suggested which require this issue to be settled in a timely fashion. Further experiments must be performed to determine whether such a materials combination is reasonable.

The last two concerns, toxicity and efficient recycling, will be somewhat reduced by the fact that in the potential scenarios for beryllium use recycling is considered to be an absolute necessity. Once the blanket has been irradiated it will contain a significant amount of radioactivity which will require remote handling to be reprocessed. Therefore, the processing plant for material after the initial loading will be closely monitored for airborne particulates and therefore the toxicity problem will be minimized. Since the most hazardous exposure to beryllium is due to the milling and use of beryllium,⁽⁶⁾ which will need to be done remotely due to the radioactivity of the exposed metal, and not the mining and handling of beryl ore, an increase in the exploration and mining of beryllium should not pose much risk to the public. Recent evaluations have also shown the cancer risk from beryllium to be lower than previously determined, thus leading the way to a possible reduction in current standards.⁽⁷⁾ This would also lessen the concerns about beryllium toxicity.

Efficient recycling will be necessary for three reasons. The cost of beryllium and the limited reserves will dictate the first order requirements on the reprocessing system. Since the reserves are low this will force producers to utilize less concentrated ores which undoubtedly increase the costs, but if an efficient recycling system, with losses of less than 1% can be designed, then obviously the materials costs can be minimized. The second reason why an efficient recycle system is necessary is to reduce the volume of radioactive waste which must be disposed of, and the third reason is to minimize releases of beryllium to the environment.

4.6.3-4. Beryllium Benefits

The basic driving force behind considering beryllium as an energy multiplier is to increase the amount of thermal power, and therefore increase the amount of gross electrical power from the blanket therefore decreasing the cost of electricity as long as any increased costs do not accumulate at the same rate. To get an estimate of how much effect increasing the energy multiplication has on the cost of electricity we will look at the MARS⁽⁸⁾ reactor cost estimates and then consider the effects of increasing the blanket energy multiplication by 30% on the system costs and the cost of electricity.

The first step in this analysis is to evaluate the effect on the power flow of the system by increasing by 32% the amount of power which comes from the blanket and reflector. This represents an increase in the blanket energy multiplication from the listed value for MARS of 1.362 to 1.8. Figure 4.6.3-4 is the appropriately modified power flow diagram. In addition to the extra power output it has been assumed that the auxiliary power requirement has been increased by 20% to accommodate the increase in thermal power. This has not been scaled by 32% because the definition of the auxiliary power includes other components than just those related to the thermal power. Also note that the power output for the direct converter is not affected by increasing the energy multiplication. For this reason, then the overall increase in net electrical power is about 29%, not 32%.

The next step in the comparison was to assess the costs of building and operating the modified plant. As with the auxiliary power above, all of the components will not scale directly with the thermal power and thus will not be increased by the full 32%. Table 4.6.3-3 lists the accounts which have been adjusted and their adjustment factors. This data is then tabulated in Table 4.6.3-4 for the predominant cost accounts for MARS. The beryllium costs have been separated and left as a variable for the sake of this analysis.

The first case, and most optimistic, would be to allow the cost of the beryllium to have been included in the changes already made in the other accounts. The effect of this assumption is to set $X = 0$ so that the total direct cost and the total capital cost are \$2.45 B and \$3.02 B. This latter

TABLE 4.6.3-3.
ADJUSTMENTS TO COSTS

Account Number	Title	Adjustment Factor
21.02	Reactor Building	$(1.32)^{0.6}$
21.03	Turbine Building	"
21.04	Cooling System Structure	"
21.05	Power Supply & Energy Storage	"
21.02.01.01	Pumps	"
22.02.01.02	Piping	0.8^a
22.02.01.03	Heat Exchanges	$(1.32)^{0.6}$
22.02.01.04	Tanks	"
22.02.02	Reflector & Direct Converter H ₂ O	$(1.23)^{0.6^b}$
23	Turbine Generator Plant	$(1.32)^{0.6}$
24	Electric Plant Equipment	"
26.01	Reactor Coolant	0.75^c

^aThe MARS piping needed to handle the very heavy LiPb coolant. The reduction in cost is due to an expected reduction in mass to be handled.

^bNot as much energy multiplication in the reflector/direct converter H₂O system.

^cReduction in mass of coolant by changing from LiPb to other coolant.

TABLE 4.6.3-4.
COST ACCOUNTING FOR THE DENSE BERYLLIUM BLANKET

Account Number	Title	Cost (1983 K\$)	
20	Load & Land Rights	5,000	
21	Structures & Site Facilities	277,950	
21.01	Site Improvements		10,650
21.02	Reactor Building		102,650*
21.03	Turbine Building		42,500*
21.04	Cooling System Structures		6,500*
21.05	Power Supply & Energy Storage		6,080*
21.06	Miscellaneous Buildings		68,410
21.07	Ventilation Stack		2,900
21.98	Spare Parts		2,010
21.99	Contingency		36,250
22	Reactor Plant Equipment	1,532,240	
22.01	Subtotal		892,920
22.01.01	Blanket & First Wall		71,190 ^a
22.01.02	Shield		74,970
22.01.03	Magnets		493,020
22.01.04	Supplemental Heating		101,300
22.01.05	Primary Structure & Support		54,550
22.01.06	Reactor Vacuum System		6,970
22.01.07	Power Supply Switching		63,420
22.01.08	Drift Pump Coils		4,710
22.01.09	Direct Convertor		22,790
22.02	Main Heat Transfer		244,660
22.02.01	Primary System		153,570*
22.02.01.01	Pumps		20,390*
22.02.01.02	Piping		64,770*
22.02.01.03	Heat Exchanger		60,480*
22.02.01.04	Tanks		2,720*
22.02.01.05	Insulation		5,210*
22.02.02	Reflector & Direct Converter H ₂ O		91,090*

TABLE 4.6.3-4.
COST ACCOUNTING FOR THE DENSE BERYLLIUM BLANKET
(continued)

Account Number	Title	Cost (1983 K\$)
22.03	Auxiliary Cooling System	50,160
22.04	Radioactive Waste Treatment	11,430
22.05	Fuel Handling & Storage	45,880
22.06	Other Reactor Plant Equipment	30,480
22.07	Instrumentation & Control	24,800
22.08	Spare Parts	32,000
22.09	Contingency	199,860
23	Turbine Generator Plant	324,820*
24	Electric Plant Equipment	189,000*
25	Miscellaneous Plant Equipment	33,160
26	Special Materials	93,850+X
26.01	Reactor Coolant	93,150*
26.04	Other Materials	700
26.05	Beryllium	X
<u>Total Direct Cost</u>		2,456,020 +X
91	Construction Facilities (0.1% DC)	245,600 (+ .1X)
92	Engineering Services (0.08% DC)	196,500 (+ .08X)
93	Other (.05% DC)	122,800 (+ .05X)
<u>Total Capital Cost</u>		<u>3,020,920 + 1.23X</u>

*Changes made from base case.

^a Beryllium is treated as a special material.

value reflects an overall cost increase of 3.7% over the costs listed in the MARS report. Since the interest during construction (IDC) and the escalation during construction (EDC) represent constants in the analysis, we find that the variation of the total capital investment will be altered by the ratio of the increased cost factor (1.037) to the increased power output factor (1.292), or that the \$/kWe will be reduced by $(1.037/1.292) = 0.803$ or a 19.7% reduction.

If we add the cost of beryllium to these estimates we find that, obviously, this cost benefit will be reduced. If the same cost for the beryllium is allowed as in the BCSS then each kilogram of beryllium will cost \$440. If the blanket is to attain a blanket energy multiplication approaching 1.8, then a very high density of beryllium must be used. Thus, a 0.5 m thick zone of 90% theoretical density beryllium (first wall radius 0.6 m, 85% volume fraction) contains 495,000 kg of beryllium or a value of \$217.8 M. When this is added to the costs of the reactor system and the additional indirect costs are included then the total direct and total capital costs are \$2.674 B and \$3.289 B respectively, and the ratio for the total capital costs of the beryllium MARS and the original MARS is 1.13, or 13% more costly. Therefore, for such a substantial beryllium zone, the savings on the unit capital costs (\$/kW_e) would be $1.13/1.29 = 0.874$ or around 12%.

The effects of adding a beryllium energy multiplying blanket can also be seen in Table 4.6.3-5. As the costs go up by the requirement to handle more power and to supply the costly beryllium, the overall effect pushes the unit capital costs ratio to be greater than 1. However, when the extra thermal energy received is accounted for, then the ratio drops below 1 and even though some of the advantage is lost due to extra power handling and not being able to increase the energy output from the direct converter, there remains approximately a 12% savings in the unit capital costs.

Since the annual operation and maintenance costs, scheduled replacement, and fuel costs for MARS are small compared to the annual fixed charge, we will assume that they are a constant. Also, assume that the overall availability of the system will not deviate from 73% by the change in blanket systems. With these assumptions, we can consider the effect that changing blankets will

TABLE 4.6.3-5.
EFFECTS OF ADDING A BERYLLIUM ENERGY MULTIPLIER ON THE UNIT CAPITAL COSTS

	Base Case (M = 1.36)	Beryllium Case (M = 1.8)	Change
Total Capital Costs (Base = 1000) without Beryllium	1000	1037	1.037 (+3.7%)
Total Capital Costs with Beryllium Added	1000	1129	1.129 (+9.2%)
Blanket Neutron Energy (MWt)	2820	3724	0.855 (-27.4%)
Net Electrical Energy (MWe)	1200	1550	0.874 (-1.9%)

Total reduction in plant capital cost per MWe = 12.6%.

TABLE 4.6.3-6
FACTORS USED IN EVALUATING THE EFFECTS OF A BERYLLIUM ENERGY
MULTIPLICATION BLANKET ON THE COST OF ELECTRICITY

	Base Case	Beryllium Case with Constant AOM
Annual Fixed Charge	AFC	0.874 AFC
Annual O&M, SCR, Fuel	AOM	AOM
Total Cost of Electricity	(AFC + AOM)	(0.874 AFC + AOM)

$$\text{COE RATIO} = \frac{0.874 \text{ AFC} + \text{AOM}}{\text{AFC} + \text{AOM}}$$

have on the cost of electricity. Table 4.6.3-6 lists the factors which are considered in this analysis and Fig. 4.6.3-5 shows the cost of electricity ratio as a function of the ratio of the annual operating costs to the annual fixed charge. Since typical values are on the order of 0.05 to 0.10, we expect an 11 to 12% savings on the cost of electricity to be realized from the change of blankets. Also, if the operating costs are allowed to be a function of the amount of energy produced, then it is conceivable that even greater savings can be obtained.

Fig. 4.6.3-5. Cost of electricity ratio as a function of the ratio of the annual operating costs to the annual fixed charge.

It is unfortunate that the various diluting effects, i.e., increased pumping requirement, increased cost for other components, the additional cost

of beryllium, do not allow a more significant effect on the cost of electricity; however, a 10 to 12% savings is not insignificant. If additional savings can be made on the power cycle, i.e., reducing the need for a nuclear grade balance of plant, or perhaps easing the requirements on plasma performance for examples, then this 10 to 12% could be compounded with any additional savings to yield a considerable reduction in the cost of electricity for fusion. Therefore, the conclusion here is that while beryllium used as an energy multiplier will not affect the cost of electricity in a great way, the savings are not insignificant, especially when compounded with any other reactor improvements which could reduce costs.

4.6.3-5. Conclusions

While it has been shown above that beryllium metal utilized for energy multiplication does not provide great savings in the cost of electricity, it has also been demonstrated that the potential problems with its use need not be considered as fatal flaws. Even a 10% savings on the cost of electricity, especially when compounded with additional design changes aimed at reducing costs, can be used to increase the attractiveness of fusion power plants. The problems of resources and irradiation induced swelling must be adequately addressed, but do not appear to be insurmountable. The resource issue is hamstrung by the fact that very little prospecting has been done for the metal and that future reserves can be extended so that a healthy fusion economy could rely on beryllium for the first few generations of reactors and well over 200 reactors could be built. The swelling issue is important at the first wall, however due to the nature of the production of helium in the reactor blanket, the average amount of swelling in a thick beryllium zone will be on the order of 3 to 5%.

The chief benefit of increasing the energy multiplication of the blanket is the additional amount of power which is obtained. This can then be utilized to reduce the cost of electricity; however, the savings are not as spectacular as could be hoped. Increasing the blanket energy multiplication for the MARS reactor, for example, from 1.36 to 1.8 allows a 12% decrease in the cost of electricity. This is due to the increased costs of the reactor system from both adding the cost of beryllium and increasing the amount of power which must be handled. Therefore, while the benefits may not be quite as large as expected, the problems need not be considered as quite as harmful.

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4.6.4 Coolant Manifold Sizes

The inside diameters of the coolant inlet and outlet manifolds were calculated for water, lithium, lead-lithium, and helium for reactor thermal power varying from 1000 MW to 3000 MW as shown in Table 4.6.4-1. For the range of operating parameters used in the calculation, the diameters of the coolant manifolds were found to vary from 14 cm for lithium to ~85 cm for helium. From this data, the approximate manifold sizes can be calculated for other operating conditions.

4.6.4.1 Assumptions

- A. Thermal power: 1000, 2000, 3000 MW_t.
- B. 12 equal sectors.
- C. Coolant, coolant temperatures, and coolant velocity are given in Table 4.6.4-2.

TABLE 4.6.4-1
MANIFOLD DIAMETER SUMMARY

Water	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>u = 6 m/s</u>	<u>u = 10 m/s</u>
	1000		33.4 (13.2)	25.9 (10.2)
	2000		46.5 (18.6)	36.6 (14.4)
	3000		57.9 (22.8)	44.8 (17.7)
Lithium-Inlet Manifold	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 200,$</u>	<u>$\Delta T = 250$</u>
	1000		15.8 (6.2)	14.1 (5.6)
	2000		22.3 (8.8)	20.0 (7.9)
	3000		27.4 (10.8)	24.5 (9.6)
Lithium-Outlet Manifold	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 200,$</u>	<u>$\Delta T = 250$</u>
	1000		16.1 (6.4)	14.7 (5.8)
	2000		22.8 (9.0)	20.7 (8.2)
	3000		27.9 (11.0)	25.4 (10.0)
Lithium-Lead Manifold	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 150,$</u>	<u>$\Delta T = 200$</u>
	1000		32.0 (12.6)	27.7 (10.9)
	2000		45.3 (17.8)	39.2 (15.4)
	3000		55.4 (21.8)	48.0 (18.9)
Helium Inlet Manifold	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 300,$</u>	<u>$\Delta T = 350$</u>
	1000		32.6 (12.8)	30.2 (11.9)
	2000		46.1 (18.1)	42.6 (16.8)
	3000		56.4 (22.2)	52.2 (20.6)
Helium Outlet Manifold	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 300,$</u>	<u>$\Delta T = 350$</u>
	1000		40.2 (15.8)	38.3 (15.1)
	2000		56.8 (22.4)	54.1 (21.3)
	3000		69.6 (27.4)	66.3 (26.1)
	<u>Thermal Power, MW</u>		<u>Diameter, cm (in.)</u>	
			<u>$\Delta T = 300,$</u>	<u>$\Delta T = 350$</u>
	1000		49.2 (19.4)	46.8 (18.4)
	2000		69.6 (27.4)	66.3 (26.1)
	3000		85.3 (33.6)	81.2 (32.0)

TABLE 4.6.4-2
COOLANT, COOLANT TEMPERATURES, AND COOLANT VELOCITY

	Water	Lithium	Lead-Lithium	Helium*
Inlet Temp., °C	280	300	300	300
Outlet Temp., °C	320	500, 550	450, 500	600, 650
Temp. Rise (ΔT), °C	40	200, 250	150, 200	300, 350
Velocity, (u) m/s	6, 10	10	5	100

*Helium inlet pressure, $P = 50, 75$ atm.

4.6.4.2 Discussion of Results

The coolant velocities assumed for the above calculations are somewhat arbitrary. As the manifold diameter is inversely proportional to the square root of the coolant velocity, the manifold diameter can be calculated for other velocities. Also, the same proportionality is valid for coolant ΔT . This data may be used to calculate the manifold sizes for any other coolant temperature differences for H_2O , Li, and LiPb. For helium, the effect of temperature on coolant density needs to be taken into account. The calculations for helium assume no pressure drop in the system. The effect of pressure drop can be accounted for since the manifold diameter is inversely proportional to the square root of the absolute pressure. For example, if there is a 5% drop in pressure across the blanket, the outlet coolant manifold sizes for helium would be increased by $1/\sqrt{0.95}$. Since the exact dependence of the physical properties on temperature and pressure has not been taken into account, the estimated manifold sizes may be considered as approximate.

4.6.4.3 Pressure Losses and Pumping Power Requirements

Since the size of the coolant manifolds are listed in the previous section, it is useful to calculate the relative pressure drop and pumping power requirements for the four coolants. Note that the pressure drop does not include MHD-related losses for lithium and lead-lithium as the manifolds are outside the magnet region.

The relative pressure loss calculations for helium coolant compared with water, lithium, and lead-lithium can be carried out using conventional empirical equations. For turbulent flow of fluids the pressure loss, ΔP , can be expressed as:

$$\Delta P = \frac{2 f L_e}{g_c D_e} + \frac{\Sigma K_i}{g_c} u_p^2 \quad [4.6.4-1]$$

where

f = Fanning friction factor

L_e = Equivalent length of flow channel

D_e = Equivalent diameter

K_i = Empirical loss coefficient expressed as number of velocity heads

u = Velocity
 ρ = Density
 g_c = Conversion factor

The loss coefficient K_i represents the pressure losses in component i , which may be valves, fittings, bends, entrance and exit effects, etc. The value of K_i varies from less than 1.0 to several hundred depending on the complexity of the flow path. The relative magnitude of pressure losses can be found from the value of $u^2\rho/D_e$ as listed in Table 4.6.4-3.

TABLE 4.6.4-3
 CHARACTERISTIC DATA FROM COOLANT MANIFOLD

Coolant	Manifold Diameter, cm	Velocity m/s	$\frac{u^2\rho^*}{D_e}$
Water	57.8	6	9.7
Lithium	27.4	10	36.8
Lead-Lithium	55.4	5	85.0
Helium (50 atm)	85.3	100	10.2

*Relative values - no units.

A comparison of $u^2\rho/D_e$ as listed above shows that lead-lithium has the highest pressure loss. However, from the operational point of view, the pumping power requirement, and not the pressure loss, is the critical factor. The relative pumping power requirement (which is proportional to $\Delta p \times$ volumetric flow rate, v) in terms of $u^2\rho/D_e \cdot v$ is listed in Table 4.6.4-4.

TABLE 4.6.4-4
RELATIVE PUMPING POWER REQUIREMENTS

	$\frac{u^2 \rho \cdot v}{D_e}$
Water	547
Lithium	1062
Lead-Lithium	1381
Helium (50 atm)	13724

Thus, it can be seen that for an identical geometry, the pumping power requirements for helium is the highest. The pumping power for helium is approximately 25 times higher than that for water. The relative values of $u^2 \rho / D_e$ and $u^2 \rho / D_e \cdot v$ as listed above are approximate since each coolant system is expected to have its own unique flow geometry. If one compares the total pressure loss, including the loss coefficient factors, the above conclusions would still be valid.

It should be noted that the relative pumping power requirements as listed above are for the manifold sections only. The pressure losses across the coolant manifolds are expected to be only a small part of the total pressure loss. Hence, when the total pressure losses, including the MHD pressure drop, are taken into account, the above conclusions may not be valid. Since the total pressure loss is design-dependent, no firm conclusions can be made from these preliminary manifold size calculations.

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