MASTER

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**SYNFUELS FROM FUSION - USING THE TANDEM MIRROR REACTOR
AND A THERMOCHEMICAL CYCLE TO PRODUCE HYDROGEN**

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February 9, 1982

✓
Lawrence
Livermore
Laboratory

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FOREWARD AND ACKNOWLEDGEMENTS

This report represents the second year's effort of a scoping and conceptual design study being conducted for the express purpose of evaluating the engineering potential of producing hydrogen by thermochemical cycles using a tandem mirror fusion driver. The hydrogen thus produced may then be used as a feedstock to produce fuels such as methane, methanol, or gasoline. The main objective of this second year's study has been to obtain some approximate cost figures for hydrogen production through a conceptual design study.

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SECTION 1

EXECUTIVE SUMMARY

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PART A - STUDY HIGHLIGHTS

1.1 Introduction

This report presents substantial details of how to produce hydrogen using a fusion reactor as the energy source and a thermochemical cycle as the water decomposition process.

The fusion reactor is the tandem mirror (TMR) of the LLNL. This machine, with its linear topology and output of both thermal energy and high voltage dc electrical energy, has advantages for this application. This energy source uses the D-T reaction producing an energetic neutron and an alpha particle.

An artist's view of a TMR is shown in Fig. 1.1-1

Fig. 1.1-1 The Tandem Mirror Reactor

Fig. 1.1-1

The thermochemical process uses General Atomic's sulfur-iodine cycle. The net result of this cycle, as Fig. 1.1-2 illustrates, is the thermochemical decomposition of water to make hydrogen and oxygen using only water as the feedstock fluid. The chemical reagents, sulfur and iodine, are continuously recycled and reused with essentially no loss of material. This cycle is one of three that have been demonstrated at the laboratory level.

1.2 Motivation for the Study

The hydrogen produced is to be used as a portable fuel or as a feedstock for other portable fuels of more complex, molecular structure; methane, methanol, gasoline, for instance. A reminder of the extreme importance to the U.S. economy of synthesizing these portable fuels is suggested by Fig. 1.2-1, the familiar spaghetti chart showing our U.S. energy flow for 1979 and how it is distributed in the consumption process; roughly 73% used as fuels and chemicals, and 27% used for electricity generation. As these non-renewable

THE GENERAL ATOMIC SULFUR-IODINE CYCLE

72-255-1

Fig. 1.1-2 The General Atomic Sulfur-Iodine Cycle

(NET PRIMARY RESOURCE CONSUMPTION 77.8 QUADS)

72-256-0

Fig. 1.2-1 U.S. Energy Flow - 1979

energy supplies of oil, coal and gas predictably diminish in the early decades of the 21st century and are replaced by the renewables--fusion being one of these--it seems clear that not only must the renewable energy sources produce electricity but they also must produce fuels and chemical feedstock in even greater quantity. Beyond postulating a method for producing fuel there is a logical case for studying synthetic fuel production in conjunction with the fusion reactor. At least three objectives important to fusion are served by these fusion/synfuel studies:

- (1) The base for fusion's applicability to our future national overall energy needs is broadened.
- (2) Fusion reactor studies for the production of electricity gain measurably by the reactor designs carried out for synfuels. Different scientific disciplines become involved. Different industrial partners become aware of fusion.

(3) The timing of fusion's availability and the need for synfuels coincide. About the time when fusion energy sources are on the threshold of commercial reality, synthetic fuels will be needed to bolster the waning supplies of the exhaustables. Figure 1.2-2 illustrates the rise and fall of our various exhaustable energy sources starting with wood from 1850 up through the 2000's. Somewhere around 2010 or perhaps a decade thereafter there is a high probability of an energy shortfall and it is then that fusion must begin to be commercialized using the inexhaustibles.

1.3 Major Conclusions and Observations

Our studies on synfuels, carried out over the last two years, have been very productive, particularly in FY81. We now have a plot plan of a representative fusion/synfuel plant. We have a new process flow sheet tailored to the fusion driver and the plant process energy demands. We have incorporated new codes in our

Fig. 1.2-2 Contribution of Various Energy Sources

studies, such as the PROCESS code, that permits better understanding and improvement of the chemical processes. The need for very high temperature blankets in the reactor has been avoided by our introduction of a new concept for SO_3 decomposition. We call this concept Joule Boosting. We have a good materials base. The linear topology of the tandem mirror reactor, its steady-state operation, and its production of energy in both thermal and electrical form have been fully utilized. We believe fusion/synfuels to be a sound concept.

Here are the highlights of our study. Some extension of these highlights may be found in Part B of this summary. The reader interested in engineering or scientific detail is referred to the main report.

- Costs of Producing the Hydrogen

The production of hydrogen using the TMR energy source and the GA thermochemical cycle has good economic promise. Our preliminary cost estimate indicates a price as low as ~\$10-12/GJ for H_2 . In comparison, a barrel of oil priced at \$50 at the wellhead and valued at \$75/bbl after refining has a cost of \$12.30/GJ.

- Plant Efficiency

Our synfuel plant efficiency ranges from about 49-51% based on two study approaches.

- A 800-900 K fusion reactor heat pipe blanket used with the Joule-Boosted SO_3 decomposer.
- A two-temperature zone blanket (850 K and 1160 K) used with a fluidized bed decomposer.

- Meeting the High Temperature Requirements of the Process

Roughly 25% of the energy demand of the thermochemical plant is at high temperature (> 1050 K) and is required to decompose SO_3 to SO_2 . The remaining 75% of the energy requirement is at much lower temperature (see Fig. 1.1-2).

Our introduction of the Joule-Boosted decomposer concept is an important system improvement in fusion/synfuels compatibility

allowing very high SO_3 decomposition temperatures ($\sim 1250 \text{ K}$) to be provided electrically but significantly relaxing temperature demands on the reactor blanket to approximately 975 K . This Joule Boosting works by using the unique high voltage direct current output from the TMR plus some electricity that is blanket derived to generate high temperatures using electrical heating elements right at the decomposer itself rather than delivering high temperature process heat.

A fluidized bed decomposer which we also introduced (in our FY80 study) may produce a higher plant efficiency but also requires the use of higher temperature (1160 K) blankets. The fluidized bed nevertheless is also a significant system improvement in reducing blanket temperatures compared to its pebble bed predecessor that started our study. Figure 1.3-1 illustrates very vividly how we have been able to reduce blanket temperatures while concurrently increasing the SO_3 decomposition temperature. The result is that the blanket design is thermally much less difficult and the decomposer is more efficient.

Fig. 1.3-1 Progress in Lowering Blanket Temperature and Raising Decomposer Temperature

Reactor Blanket Design

The heat pipe blanket module that we coupled to the Joule-Boosted decomposer concept was an extension of our Cauldron blanket design we studied in FY80. In the Cauldron idea we found the vapor bubble rise velocity in the liquid (~ 0.2 m/s) limited our heat transfer. To overcome this we decoupled the vapor from the liquid by introducing heat pipes into the Cauldron. Vapor velocities within heat pipes can be hundreds of meters per second. Detailed analysis of the heat pipe as a single unit or as an array of heat pipes disclosed that overall this is a complicated design. We would not presently recommend its use although during the course of our study we developed a flattened heat pipe with an axial heat flux capability almost 20 times higher than a conventional heat pipe for use in the presence of a magnetic field. It may have applicability in future work.

Our two-temperature zone blanket, studied in parallel with the heat pipe blanket, delivers process heat at both high temperature (1160 K) and a lesser temperature (850 K) and allows greater use of thermal energy from the TMR in the chemical process. The thermal efficiency is higher. The lower temperature zone, if operated slightly hotter (~ 100 K), could also be used independently as an alternate to the heat pipe blanket for the Joule-Boosted concept. Some difficulty was experienced in delivering the required energy fraction ($\sim 25\%$) at the high temperature while still maintaining adequate tritium breeding in the lower temperature zone. Further design refinements in the blanket should resolve the high temperature/lesser temperature energy division problem.

Our blanket choice of the two studied would be the Li_2O lower temperature zone part of the two-zone blanket coupled to the Joule-Boosted decomposer.

- Plant Size

We have developed a preliminary Synfuel Plant plot plan. The area required for this plant which produces ~ 4900 gm mols/s of H_2 is comparable in area to that required for a nuclear power plant of the same relative output, about 0.36 square miles.

- Design Analysis

As we indicated in last year's report, the fusion/synfuel complex is substantially more difficult to analyze than a fusion reactor/electric complex. However, we have developed an improved process flow sheet tailored to our fusion source. Because of the necessary study involved in getting to this development, we are able to identify and place in order of priority the units or unit processes that we think can be substantially improved or reduced in cost. This is a highly iterative process but we now have the highly sophisticated simulation code (PROCESS) that aids immeasurably.

- Coupling the Reactor to the Process

A cost effective interface has been developed between the TMR and the chemical plant using helium as a reactor coolant. The chemical plant uses helium and steam internally. Even with the temperature and pumping power penalties associated with gas coolants, the safety considerations dictate the use of a gas coolant.

- Chemical Plant Operational Dependability

With an appropriate degree of process unit redundancy that we have included, we can predict a high, combined TMR/chemical plant availability of $\sim 74\%$, and this figure is included in our cost figures for the hydrogen produced.

- Materials

We have sufficient materials options in the reactor and the chemical plant so that we feel we have a credible materials

base. However, we are hampered by a lack of long-term creep behavior, experimental property data on lithium oxide, and experimental corrosion data of H_2SO_4 in both liquid and gaseous states.

As fusion comes of age it is critically important that we verify our analytical studies with a strong experimental backup. This should begin now because a materials development program can be very lengthy.

Problem Areas

Blanket design remains an area of principal concern. Even with Joule boosting or fluidized bed decomposers relaxing the temperature demands of the blanket, the design is difficult because of the myriad demands the blanket must satisfy. Much more attention is needed here. This concern is true for all blankets--for synfuels, for electricity, for co-generation, for hybrids. Analyzing the pluses and minuses of our heat pipe blanket and our two-zone blanket designs, as well as those of other studies, we believe that the most promising design options are now clearer. As a result we feel that we can now converge on a highly effective design approach which uses the best of all the parts that we can garner from our study. This design will be a non-flowing lithium oxide blanket module using helium as a coolant. The module will be functional over a range of temperatures and useful for synfuels, for electrical energy, or both.

In the chemical plant we wish to make substantial improvements in the HI separation and purification. This process requires a large amount of power and has high capital costs ($\sim 1/3$ of the chemical plant). A new technique is envisioned.

1.4

Recommendations for Further Study

Our analytical studies have been very fruitful and produced innovative ideas and technical contributions that help to lend good credibility to the fusion/synfuel tie. We recommend that the

conceptual design for the reactor and chemical plant components continue to be refined and that an experimental program to complement or verify the conceptual design efforts be initiated. There are numerous process elements in the chemical plant that we have identified for technical or economic improvement and these should be studied. We should increase our understanding of the overall process and the system integration that must be done.

Specifically we would recommend the following for the near term.

- Continue the purely analytical studies in greater depth and couple them to experiments that the analytical studies suggest. We particularly cite the following:
 - Improve the chemical plant design analysis and initiate parallel experiments to reduce or solve materials problems, improve heat balances, and lower equipment costs. The decomposition of H_2SO_4 and recovery of HI are particular areas where significant improvements are achievable and desirable.
 - Refine the helium-cooled Li_2O blanket design.
 - Fabricate and test laboratory-scale components of a Li_2O blanket module.
 - Obtain experimental property data on Li_2O .

It should be noted that these last three would be of value to the entire fusion technology community.

PART B

ADDITIONAL INFORMATION ON THE FUSION SYNFUEL PLANT

1.5. A Plot Plan for the Hydrogen Plant

To create a total fusion/synfuel plant there are four major plant components that must be designed, interfaced and iterated. As shown in Fig. 1.5-1 these are:

- (1) The Tandem Mirror Reactor (TMR) with its associated physics.
- (2) The reactor blanket and direct convertor, sources of the process energy.
- (3) The energy transport and conversion system.
- (4) The synfuel plant, producer of the hydrogen fuel.

Each of these components will be discussed subsequently but first it will be of interest to the reader to see our first plot plan of

Fig. 1.5-1 Four Major Plant Components for a Fusion-Based Hydrogen Production System

[illegible]

72-104-0

Table 1.5-1. Reactor and Synfuel Plant Energetics

Reactor

a)	Thermal energy due to neutrons	2150 Mw
b)	Exothermic blanket energy mult.	<u>430</u>
c)	Total blanket thermal energy	2580
d)	Energy produced by alpha particle	717
e)	Alpha energy converted to high voltage dc @ 60% efficiency	430
f)	Alpha energy available thermally (by difference)	287
g)	High voltage used to drive the reactor	315
h)	Auxiliary energy to reactor as thermal energy	52
i)	Net energy to synfuel plant	
o	As thermal energy (c + f - h)	2815
o	As high voltage dc (e - g)	115

Synfuel Plant

j)	Gram mole of H ₂ produced/s	4900
k)	Heating value per gram mol	286 kJ
m)	Equivalent output in MW	1445

1.6

The Tandem Mirror Reactor and Its Physics

The Tandem Mirror Reactor (TMR) is a linear device in which ions in a long center cell are confined by ambipolar potentials developed in mirror "plugs" at each end of the machine. In this report, the TMR is operated with A-cell thermal barriers.

A-cells are auxiliary mirror cells added beyond the yin-yang plug coils of a standard tandem mirror magnet set. Figure 1.6-1 shows the TMR with the A-cell tandem mirror magnet configuration which was taken as the basis for this report. Also shown are the locations of the neutral beam injection and the electron-cyclotron microwave heating (ECRH). In the yin-yang cells, the neutral beams produce hot ions which provide the beta necessary for MHD stability as well as the electrostatic end plugging.

Neutral beams in the A-cells pump out the cold ions to maintain the thermal barriers. These barriers are depressions in the ion potential which insulate colder center-cell and plug electrons from the heated electrons in the outer potential peak at the ends of the A-cells. The electrons at the A-cell ends are also microwave heated (ECRH) to increase the potential peak which confines the center-cell ions. This outer potential peak is the result of neutral beams injected off the midplane in the A-cell in such a manner that ions "slosh" axially between the A-cell magnetic field maxima. ECRH is also used at the thermal barrier minimum to heat the fraction of cold center-cell electrons that pass into the A-cell and thus confine them magnetically. A reduction of the cold-electron density required for charge neutrality further depresses the thermal barrier potential minimum.

The physics model which describes the plasma is presented in detail in Section 3.0. The code used to solve the equations of the physics model requires as input all the magnetic field strengths,

Fig. 1.6-1 Tandem Mirror Reactor with A-cell Thermal Barriers

the plasma betas, the fusion power, the center-cell length, the injection energies, and the center-cell electron and ion temperatures.

Because of thermochemical and neutronics considerations, our reactor parameters were subject to certain constraints, namely the neutron wall loading $\Gamma = 2 \text{ MW/m}^2$, the first wall radius $r_w = 1.5 \text{ m}$, as well as the fusion power of 2301 Mw for coupling to a synfuel plant with a two-zone decomposer, or 2634 Mw for coupling to a synfuel plant with a Joule-Boosted decomposer.

Reactor parameters were obtained in both cases for Q values of 15 with a center-cell beta of 0.4, and for Q values of 25 with a center-cell beta of 0.8. The length of the center cell was 98 m appropriate for coupling to the synfuel plant with a two-zone decomposer, and 112 m appropriate for coupling to the synfuel plant with a Joule-Boosted decomposer.

1.7

The Reactor Blankets - Principal Source of the Process Energy

In this year's study two different blanket designs were completed to match two fundamentally different ways to satisfy the energy demands of the General Atomic thermochemical process. This process, as we indicated in Fig. 2 in the introduction, has thermal energy requirements that can be split into two temperature ranges. The process requires approximately 25% of its energy at high temperature ($> 1050 \text{ K}$) to cause SO_3 to be decomposed to SO_2 . The temperature/enthalpy diagram of Fig. 9 illustrates this H_2SO_4 boiling and decomposition step. The remainder of the process plant needs can be supplied at a lesser temperature which is design dependent. Our heat pipe blanket is used in conjunction with the Joule-Boosted SO_3 decomposer. In this concept electricity obtained directly as high voltage direct current output

Fig. 1.7-1 Temperature/Enthalpy Diagram for SO_3 Decomposition

from the Tandem Mirror Reactor's direct convertor, supplemented by some electricity that is blanket heat derived, is used to satisfy in situ the high temperature (~ 1250 K) process demands of decomposing SO_3 to SO_2 within this chemical reactor. Generating high temperatures using electrical heating elements at the SO_3 decomposer itself rather than delivering high temperature process heat eliminates the need for a very high temperature blanket and energy transport system. Figure 1.7-1 shows how the Joule boosting has decreased the high temperature demands placed on the blanket, while at the same time permitting higher decomposition temperatures. As can be seen from the figure, approximately 100 kJ/gm mol of H_2 are supplied by providing the energy electrically in the high temperature range up to 1250 K. This very effectively decomposes the SO_3 to SO_2 with approximately 84% conversion efficiency. To keep the SO_2 in the decomposed state, it is

quickly quenched to ~ 1050 K by adding a stream of cold liquids and gases. A heat exchanger is then used to recover sensible heat from the decomposed and partially cooled gases, and to preheat the incoming $\text{SO}_3/\text{H}_2\text{O}$ mixture from about 900 K to the 1050 K level. Effectively, then, the blanket need only provide heat to the chemical plant at about the 900 K level.

We consider the Joule-Boosted SO_3 decomposer to be a very important step forward for fusion/synfuels. The blanket in this case can operate at manageable temperature levels; somewhat above 900 K.

It is interesting to note that the SO_3 decomposer can actually run hotter and thus chemically more effectively using silicon carbide electrical heating elements than it could if the processor were one using a fluidized bed, as is the case of the processor for our alternate blanket design. The increased decomposition effectiveness (84% vs 55%) roughly balances out the penalty paid for the conversion of some blanket thermal energy to electrical energy.

Our alternate blanket design used with a fluidized bed SO_3 decomposer uses $\sim 25\%$ of the total blanket energy to provide high temperature thermal energy for the SO_3 decomposition instead of electricity. The resulting blanket concept has two radial temperature zones and each zone supplies its required energy fraction to the process; a hot zone at 1050 K and a cooler zone at 850 K.

1.7.1 The Heat Pipe Blanket and Heat Exchangers

In our first year of synfuel blanket design work we used the idea of a boiling Li-Na liquid pool or Cauldron, which resulted in minimal structural materials exposed to neutron activation or damage and fundamentally simple blanket module shapes. We found it necessary to modify this attractive concept when we discovered that the void fractions near the first wall were predicted to be 60% or greater at 1200 K. This original blanket concept, with a brief description of how it works, is shown in Fig. 1.7-2.

The two liquids in this Cauldron module act as both the neutron moderator and heat transfer fluid, absorbing energy in direct proportion to the energy input and transferring it by latent heat of vaporization of the sodium to a heat exchanger in the dome of the vessel. The lithium performs the function of tritium breeding.

The sodium vaporizes, leaving behind the lithium in the liquid state to do its neutron-moderating, tritium-producing function.

The sodium condenses on the heat exchanger tubes in the dome, yields energy and returns as liquid droplets in the pool, thus completing the cycle.

In the dome the condensing vapor heat exchanger (CVHX) transfers the thermal energy out of the module to various chemical processors located some distance from the reactor.

Fig. 1.7-2 Original Cauldron Blanket Concept

To resolve this void fraction problem we could have increased the Cauldron temperature to ~ 1400 K, thereby increasing the vapor density fourfold and decreasing the void to $\sim 15\%$. However, we were striving for lower blanket temperatures rather than higher and the Joule-Boosted decomposer made this possible. Consequently, the modification we introduced consisted of using heat pipes in a modified Cauldron volume. The heat pipes permit vapor velocities approaching sonic velocity, thereby overcoming the vapor velocity limitation of the boiling Cauldron and permitting operation of the lithium pool at temperatures more in keeping with the new and lesser demand of 850-950 K due to Joule Boosting. Figure 11 shows the resultant heat pipe blanket module.

The parameters for the reference heat pipe design for one typical blanket module are summarized in Table 2.

72-282-3

Fig. 1.7-3 Heat Pipe Blanket Module

Table 1.7-1. Flat-Heat-Pipe-Cooled Module Parameters*

Neutron wall loading	2 MW/m ²
Central cell magnetic field strength	2 Tesla
Thermal power/module	22,600 kW
No. of sheet heat pipes/module	88
No. of CI heat pipes/sheet	18
No. of CI heat pipes/module	1584
Normal heat pipe operating temperature	1000 K
Average no. of CI heat pipes/m ² at the first wall	153
Average void fraction near the first wall for Li (Li-Pb)	31% (36%)
Average structural fraction near the first wall for Li (Li-Pb)	4.5% (9.5%)
Design-point heat transport/CI heat pipe = 3 CI heat pipes (with $SF_Q = 1.5$, $\phi = 20^\circ$)	70 kW
Design-point heat transport/CI heat pipe (with $SF_Q = 1.5$, $\phi = 20^\circ$)	23.3 kW
Design-point axial heat flux based on condenser total cross sectional area (with $SF_Q = 1.5$, $\phi = 20^\circ$)	5.0 kW/cm ²
Helium coolant inlet bulk temperature	490 K
Helium coolant exit bulk temperature	875 K

* For definition of symbols and parameters, the reader is referred to Section 5 of the main text.

The heat pipe blanket concept has many attractive features when used with a liquid lithium pool. These include:

- (a) An average void fraction near the first wall of only about 30%, which is half the value of the 60% predicted for the boiling Cauldron.
- (b) An average structural fraction near the first wall of only about 4.5%, which is less than most helium-cooled and flowing lithium-lead-cooled blanket designs.
- (c) A flattened heat pipe design, which is predicted to be able to transport about 5 kW/cm^2 axially (along the heat pipe) in a 2-T magnetic field. This is over 17 times the heat transport capability of a conventional channel wick heat pipe design operating at the same conditions.
- (d) The design includes substantial redundancy in the event of the failure of individual heat pipes. The failure mode is "soft," so the reactor would not have to be shut down unless several adjacent heat pipes failed, which is an unlikely occurrence.
- (e) The design retains many of the advantages of the liquid pool blanket in that the amount of material subject to neutron damage is still relatively small.

However, when we look at all the design considerations with objectivity, we are forced to conclude that, in its present form, the heat-pipe-cooled liquid pool blanket concept is not as attractive as we had initially hoped. Our detailed analysis disclosed that collectively the design is too complicated.

1.7.2 Two-Temperature Zone Blanket and Heat Exchangers

As a parallel to the heat-pipe-cooled blanket concept coupled to the Joule-Boosted decomposer, it is possible to devise a high temperature blanket concept to replace the joule heating of the SO_3 decomposer with a thermally driven decomposer. The concept is one using a two-radial temperature zone blanket where each zone

supplies a parallel stream to the thermochemical process. The high temperature zone (1060 K) should supply ~ 25% of thermal energy which is used for the SO_3 decomposer. The so-called low temperature stream (850 K) supplies the remaining 75% of the thermal energy. The 75% comes from the direct converter (10%) and the low temperature blanket (65%).

1.7.2.1 Low Temperature Zone

Three low temperature zone blanket designs were studied in parallel with the heat pipe blanket; flowing Li_2O microspheres, flowing Li-Pb, and static Li-Pb helium cooled.

1.7.2.2 Flowing Lithium Oxide Microspheres

This concept, illustrated in Fig. 1.7-4, uses the gravity flow of Li_2O microspheres as the heat generation and transport media. The spheres (400 μm) flow slowly as a dense bed through the blanket flow channels. The hot microspheres flow from the blanket to an intermediate heat exchanger. The thermal energy is transferred to the helium working fluid of the thermochemical process. The cooled microspheres are returned mechanically to the top of the blanket by an Archimedes lift.

1.7.2.3 Flowing Lithium-Lead

This concept is conceptually similar to the flowing Li_2O concept where the Li-Pb flows as liquid through banks of flow tubes where it is heated, as shown in Fig. 1.7-4. The energy is transferred to the helium working fluid of the thermochemical process by an intermediate heat exchanger. The Li-Pb is returned to the blanket by a mechanical pump. While the Li-Pb has a high electrical conductivity and thus the magnetic field retards the flow, the low velocity for this application gives tolerable pressure drop for the Li-Pb loop.

1.7.2.4 Helium-Cooled Lithium-Lead

This concept attempts to compact the overall blanket/heat exchanger design by moving the heat exchanger inside the blanket. The

Fig. 1.7-4 Three Models of the Two-Temperature Zone Blanket

blanket consists of stagnant Li-Pb penetrated by cooling tubes containing helium, as shown in Fig. 1.7-4. The helium working fluid is heated directly without an intermediate heat exchanger.

1.7.2.5 High Temperature Zone

The high temperature zone is placed radially outside the low temperature zone and consists of several rows of flow tubes. The outer wall of each tube provides the pressure boundary for the helium that flows internal to each tube. The tube wall is cooled by flow of helium and is insulated from the hot interior of the tube by internal insulation. The tube wall is thin and the insulation is of low density to reduce heat generation. This is to allow easy penetration of neutrons to the interior where they are absorbed and generate heat. The heat generating material is a dense neutron absorber such as a high temperature refractory material.

1.7.2.6 Low Temperature Zone Conclusions

While the low temperature zone is considerably cooler than the high temperature zone, it is still a "hot" blanket that presents significant design challenges to obtain sufficiently low materials temperature for high structural strength while retaining good thermodynamic characteristics. The low temperature zone could also be operated as a stand-alone blanket (without a high temperature zone) and be used as the heat source for the Joule-Boosted process. Actually it meshes well with the Joule-Boosted decomposer concept, particularly using the flowing Li_2O .

1.7.2.7 Flowing Li_2O

The flowing Li_2O blanket has been considered previously and the issue of sintering the microspheres has been removed as a problem because of the lower temperature in this application. The concept is simple and operates at low pressure. Its drawbacks are: (1) particle attrition and wear, which is not likely to be severe because of the typical low velocities; (2) the large size of the

intermediate heat exchanger (IHX); and (3) the large size of the particulate return system. It is possible to combine the heat exchanger and the return system into one unit. The flowing microsphere design concept would have quite high operating reliability and would not suffer severe consequences to the reactor from a IHX failure.

1.7.2.8 Flowing Li-Pb

This system is attractive because of its simplicity, compactness and relatively small size of the IHX. The magnetic pressure drop is moderate and does not appear to be a problem. The fundamental issue is concerned with the corrosion of the structure by Li-Pb at the upper temperatures. The corrosive properties are known at lower temperatures but are unknown for candidate nickel alloys at these temperatures. Tube failures in the blanket would be of concern because of the consequences of a spill. It would also take the module out of service and it would have to be replaced. Heat exchanger tube failure would be a problem, but could be repaired by tube plugging. Startup and shutdown is a serious drawback because of the freezing of Li-Pb.

1.7.2.9 Helium Cooled Li-Pb

This concept is attractive because of its compactness and the elimination of the IHX. The heat transfer coefficient to helium is rather low and this leads to high tube wall temperatures. The temperatures are greater than for the flowing Li-Pb concept with the attendant higher concern for corrosion. The design is also intolerant to tube failure and could lead to overpressurization of the Li-Pb pool. Tube failure would require module replacement. Structural stresses due to unprogrammed reactor shutdowns and consequent freezing of the Li-Pb would be a severe problem.

Based on this comparison the flowing Li₂O seems to offer the best alternative of these three concepts.

1.7.2.10 High Temperature Zone Conclusions

The fraction of blanket energy available from the high temperature

zone strongly depends on the neutronic performance of the blanket. There is a direct trade-off between tritium breeding and the fraction of energy deposited in the high temperature zone. While the current design configurations produce sufficient tritium breeding, they are marginal regarding the fraction of energy deposited in the high temperature zone. The process requires about 25% delivered as high temperature heat to the process and the current design provides only 18% generated in the high temperature zone.

The fraction of energy deposited in the high temperature zone is further degraded by that fraction deposited in the insulation and tube and the additional heat loss through the tube wall. Alternate design concepts need to be found if the high temperature zone is to meet process requirements.

1.8 Neutronic Performance of the Two Blanket Concepts

Neutronic calculations were performed for the heat pipe blanket concept for two cases: (a) heat pipes immersed in a liquid lithium pool that constitutes the blanket, and (b) heat pipes immersed in a liquid pool blanket of molten $\text{Li}_{17}\text{Pb}_{83}$.

The neutronics for the two-temperature zone blanket concept were performed on three models: (a) one using flowing Li_2O microspheres in the low temperature zone, (b) another using flowing LiPb in the low temperature zone, and (c) a third which used fixed LiPb in the low temperature zone with helium cooling. All three used a refractory rod bundle for the high temperature zone. The comparative results of the neutronics are shown in Table 1.8-1.

In all cases the neutronics results produced the values of tritium breeding and blanket multiplication necessary for any blanket design. In addition the analyses for the heat pipe design showed that the use of a metal reflector (as used in the WITAMIR design) in place of the more traditional graphite reflector is a superior alternative. In most cases the neutron economy is not significantly degraded by this substitution. In any event,

Table 1.8-1. Comparative Neutronics Performance Between Two Blanket Concepts

	Heat Pipe Blanket		Two-Zone Blanket		
	Li	Li ₁₇ Pb ₈₃	Flowing Li ₂ O	Flowing Li ₁₇ Pb ₈₃	Fixed Li ₁₇ Pb ₈₃ He Cooled
Tritium breeding					
T ₆	0.95	1.03	0.62	1.12	1.02
T ₇	<u>0.59</u>	<u>0.02</u>	<u>0.42</u>	<u>0.01</u>	<u>0.01</u>
Total	1.54	1.05	1.04	1.13	1.03
Blanket energy multi.	1.19	1.31	1.19	1.33	1.33
Fractional heating (%)					
First wall	4	7	10	3	3
Breeding region	88	88	89	96	96
Reflector	7	5	1	1	1
Other	<u>1</u>	<u>< 1</u>	<u>-</u>	<u>-</u>	<u>-</u>
	100	100	100	100	100
High temperature zone	N/A	N/A	16.5	18.4	21.3

blankets with inherently high tritium breeding potential (thick Li, Li-Pb, or Li_2O) are not overly constrained by the neutron economy. The metal reflector also acts as a shield and thus leads to significant reductions in the required overall blanket/reflector shield thickness.

The adequacy of the heat pipe blanket described in this report in providing the superconductor insulator shielding is open to question. In order to ensure that the superconductor insulator lifetime would be acceptably long (30 years), a more detailed shielding analysis would be required. Short of this analysis we have estimated that ~ 12 cm of additional shielding should be added around the outside of this particular blanket.

The neutronics calculations for the two-zone blankets did not produce results which satisfied our original design goal. Although numerous contributions of breeder region and high temperature zone materials and thicknesses were considered, it was not quite possible to simultaneously breed tritium and produce a high temperature energy fraction (25%) as large as that demanded by the synfuel plant. There are solutions to this apparent mismatch, some of which are:

- (1) Scale up the fusion plant size to meet the high temperature requirements of the synfuel plant. This produces a small surplus of low (850 K) temperature energy which can be used for the production of electricity that can be marketed.
- (2) Design a two-axial zone and two-radial zone blanket in which a very high tritium breeding blanket occupies a fraction of the axial length and a high temperature, low breeding blanket covers the remainder. This hybrid axial-radial approach may produce more efficient overall neutron economy. The overall design complexity would likely be increased.
- (3) Consider an in situ SO_3 decomposer (i.e., inside the blanket) with either a radial or axial-zoned design. Although this approach incorporates substantial additional risks, there may be attendant performance benefits.

1.9 The Thermochemical Plant Process

The sulfur-iodine cycle, which is the focus of our study, is being developed by the General Atomic Company. It is a pure (without electrolysis) thermochemical cycle and is described by the following major reaction steps:

Major parts of the process are associated with separation and purification of the reaction products. For example, a critical aspect for the successful operation of the process is in the separation of the aqueous reaction products in reaction (1). Workers at the General Atomic Company have solved this problem by using an excess of I_2 , which leads to separation of the products into a lower density phase, containing H_2SO_4 and H_2O , and a higher density phase containing HI , I_2 and H_2O .

Reaction (2) shows the catalytic decomposition of HI , which is in the purified liquid form (80 atm). Laboratory decompositions are around 50% per pass; therefore, a recycle step is necessary. The unreacted HI is condensed out of the H_2 product and distilled away from the I_2 product. Pure H_2 is obtained by scrubbing out the remaining I_2 with H_2O .

The equilibrium for reaction (3) lies to the right at temperatures above 1000 K, but catalysts or higher temperatures are needed to attain sufficiently rapid decomposition rates. Catalysts are available for this process, but careful consideration needs to be given to their costs versus effectiveness.

1.10 Major Components of the Process

The plot plan for the physical plant and the cost of producing our hydrogen product could not have been accomplished had we not first developed the process flow diagram shown in Fig. 1.10-1. This extremely complicated but still abbreviated flow sheet is useful here in the Executive Summary to illustrate the important point

TMR DRIVEN SYNFUELS PLANT (HEAT PIPE BLANKET)

Fig. 1.10-1 Process Flow Diagram

that reactor/synfuel studies are highly iterative--the chemical plant and some units in particular strongly influence the design of the reactor in terms of temperatures, temperature ranges, and quantities of energies. We will illustrate this by discussing just four of the process units, three of which are circled on the flow diagram. These are basically units that we felt needed our first design attention or were high cost items. There are many units yet to be improved or further developed.

1.10.1 Joule-Boosted SO_2 Decomposer

The Joule-Boosted decomposer uses electrical energy to heat SiC heating elements. Such a decomposer design is depicted in Fig. 1.10-2. Large SiC heating elements, ~ 5 cm in diameter and 3 m long, are arranged vertically with the process gases fed in crossflow, very similar to commercial crossflow shell and tube heat exchangers.

Fig. 1.10-2 Joule-Boosted Decomposer

The eight SO_3 decomposer vessels required can be fabricated out of Incoloy 800H to withstand the 0.7 MPa (7 atm) process pressure at a wall temperature of 1200 K and the corrosive $\text{H}_2\text{SO}_4/\text{SO}_3/\text{H}_2\text{O}$ gases. It appears that adequate creep rupture strength (< 1% creep in 20 years at 12 MPa (1760 psi) can be provided using a 5-cm wall.

The Joule-Boosted decomposer is a much more compact and less expensive unit than any of the older concepts of packed bed, fluidized bed, or catalytic cartridge decomposers and offers the best possible safety isolation of the $\text{SO}_2/\text{O}_2/\text{H}_2\text{O}$ process gases from the liquid metal-containing blanket.

The high effectiveness of this decomposer is evident from Fig. 1.10-3 where the conversion efficiency is ~ 84%, compared to a range of 54% to 69% for the fluidized bed decomposer, our parallel design.

Fig. 1.10-3 Equilibrium Decomposition Curves for SO_3

The high effectiveness is simply due to the fact that the surface of the electrically heated silicon carbide heating element can run very hot, 1250 K or higher, while the catalyst in the fluidized bed must be substantially cooler, ~ 1050 K, limited as it is by blanket temperature.

1.10.2 The Fluidized Bed Decomposer

Figure 1.10-4 illustrates a decomposer which provides the necessary production of SO_2 by efficiently using the high temperature heat from the two-zone blanket, our alternate blanket design to the heat pipe blanket. The decomposer is a catalytic fluidized bed reactor operating at 1050 K. The unit contains internal heat exchanger tubes to provide the heat for the highly endothermic SO_3 decomposition. We can achieve a 55% conversion using a CuO catalyst, if sulfation of the substrate does not become a problem around 1050 K, or much more expensive platinum on a titania support can be used if sulfation is a serious problem.

Fig. 1.10-4 Fluidized Bed Decomposer

1.10.3 H₂SO₄ Boiler

The H₂SO₄ boiler illustrated in Fig. 1.10-5 is a particularly challenging design problem since alloys are required to simultaneously withstand the H₂SO₄ corrosion and the 3.0 MPa pressure from the helium heat transfer fluid.

Our concept uses siliconized silicon carbide U-tubes. A special geometric configuration was developed that established a near optimum compromise between the heat transfer and pumping losses in the helium and the boiling instabilities that could occur in the liquid H₂SO₄.

1.10.4 The Decomposer-Recuperator

The decomposer-recuperator is shown in Fig. 1.10-6. The SO₂ produced in the Joule-Boosted decomposer or the fluidized bed

Fig. 1.10-5 H₂SO₄ Boiler Unit

Fig. 1.10-6 Decomposer-Recuperator

decomposer must be quickly quenched so that it does not recombine with oxygen. This quenching is done by introducing cold gases and liquids into the flow stream to reduce the temperature to ~ 1050 K prior to entry into the recuperator heat exchanger. Subsequently this heat exchanger preheats the feed to the decomposer using the sensible heat from the hot SO_2 , O_2 , and other gaseous products leaving the SO_3 decomposer and the quench stream. The recuperator unit is particularly important since it supplies a large quantity of sensible heat in the temperature range of 680 K to 950 K as was shown in the temperature enthalpy diagram of Fig. 1.7-1.

This decomposer-recuperator is a difficult design to accomplish economically since it is a high temperature gas-to-gas heat exchanger with characteristically very low gas-phase heat transfer

coefficients. Large heat transfer areas are required, so the units tend to be large.

1.11

Costs for Producing Hydrogen - Preliminary Estimates

These cost estimates, in 1980 dollars, assume a plant in operation ~ 2030, a decade or so after fusion electric plants have been in operation. Ultimately our fusion-synfuel plants will most likely produce some combination of three different products: (1) pipeline quality synthetic natural gas (e.g., 10% H_2 , 80% CH_4 , and 10% C_2H_6 and higher hydrocarbons and other gases); (2) liquid transportable fuel (i.e., CH_3OH); (3) other chemical feedstocks (C_2H_4 , C_2H_3O , acetic anhydride, NH_3 , etc.). If the production is mostly synthetic natural gas for pipelines, a utility is likely to finance, build, and operate the plant. If the products are fuels and chemicals, a chemical company will be involved. So the method of financing could be by either utility (public or private) or chemical industry. We determined costs for all three methods. The three methods are differentiated as follows: public utility financing (100% debt), private utility financing (50% equity, 50% debt), and chemical industry financing (100% equity). The least cost for producing hydrogen was found to be via public utility financing. Table 1.11-1 shows our estimates for capital costs, annual operating costs and hydrogen production costs using this financing for the two-zone blanket and the heat pipe blanket.

Table 1.11-1. Estimates of Capital Costs, Operating Costs and Hydrogen Production Costs of a TMR-Driven Thermochemical Plant using Public Utility Financing (\$M)

BLANKET TYPE	TWO ZONE		HEAT PIPE	
TMR Driver Technology Level	Low	High	Low	High
<u>TOTAL CAPITAL COST</u>				
T/C plant	1187	1187	1278	1278
TMR	1195	1563	1195	1563
<u>ANNUAL OPERATING COST</u>				
Financing cost:				
T/C plant	93.9	93.9	101.1	101.1
TMR	76.9	100.6	76.9	100.6
Production cost:				
T/C Sec. I-IV	109.3	109.3	109.3	109.3
T/C Sec. V	4.7	4.7	7.2	7.2
TMR	20.4	26.7	20.4	26.7
Other fixed costs:				
T/C Sec. V and TMR	45.8	57.6	49.7	61.5
Total annual operating cost:				
TMR + T/C	351.0	392.0	364.6	406.4
<u>HYDROGEN PRODUCTION COST</u>				
\$/B3	10.32	11.53	10.72	11.95

1.12 Materials

Preliminary materials selections were made for the following:

- Heat pipe blanket
- Two-zone blanket
- GA thermochemical cycle plant

1.12.1 Materials for the Heat Pipe Blanket

Materials for the structural container and for the heat pipes and the approximate limiting use temperatures are summarized in Table 1.12-1.

Table 1.12-1. Summary of Materials Selections for the Heat Pipe Blanket.

Application	Selected Materials
First wall (700 K)	V-10 Ti tubing, 2 cm O.D., 2 mm wall
FELTMETAL insulator (700-1100 K)	D-43, 5 mm thick 40-60% of T.D.
Blanket containment wall (~ 1100 K)	D-43, 3 mm thick
Heat pipe - evaporator section (~ 1100 K)	Layer structure: 1.6 mm D-43 0.2 mm SiC, 0.4 mm D-43
Heat pipe - condenser section (~ 1000 K)	Layer structure: 1.5 mm TZM, 0.2 mm SiC, 0.4 mm TZM
Tritium permeation membrane (1000 K)	D-43, 1 mm thick

1.12.2 Materials for the Two-Zone Blanket

The main materials problems in the two-zone blanket are in the selection of flow channel materials for all three of the low-temperature-zone concepts studies.

For the flowing Li_2O microsphere concept, only pure Ni is known to be resistant to corrosion by Li_2O . To reduce the amount of Ni required and hence the amount of neutron activation, we would Ni-plate alloys such as Inconel-617 or Inconel-625, which have a fair resistance to corrosion by Li_2O at an expected maximum channel temperature of 1083 K. These alloys have the added benefit of good creep strength. Hastelloy-XR should also give the required corrosion protection without a clad, and has a high creep strength as well.

The requirements for channel materials for the flowing Li-Pb concept and helium-cooled Li-Pb concept are similar, with the former having a maximum material temperature of 942 K, and the latter, 1002 K. These requirements are also similar to those for the heat pipe blanket where corrosion by liquid $\text{Li}_{17}\text{Pb}_{83}$ is the main concern. As with the heat pipe blanket, we select the niobium alloy, D-43, because of its good corrosion and long-term creep resistance.

1.12.3 Materials for the GA Thermochemical Cycle

The most critical materials problems in the GA thermochemical cycle are encountered in processing of H_2SO_4 . Two temperature regimes are critical--a low temperature regime (~ 400 -700 K) where H_2SO_4 is concentrated by evaporating water in multi-effect evaporators and then an azeotrope of ~ 98 wt % H_2SO_4 is boiled in H_2SO_4 boilers. The second is a high temperature regime (~ 1050 -1250 K) where gaseous SO_3 is decomposed in the presence of steam to form $\text{SO}_2 + \text{O}_2$.

For the low temperature regime, we select siliconized SiC U-tubes as the heat exchanger material and a silica brick lining on

Teflon-coated mild steel for the containment vessels. These selections apply to both the multi-effect evaporator and H_2SO_4 boiler units.

For the high temperature regime, materials are needed for two types of SO_3 decomposers: the Joule-Boosted decomposer and the fluidized bed decomposer.

The main materials problem for the Joule-Boosted decomposer is selection of electrical heating elements. Based on recent experimental work at the Oak Ridge National Laboratory (ORNL), SiC heating elements are an excellent choice. Extrapolating the ORNL data indicates good corrosion resistance for ~ 20 y to gaseous SO_3/H_2O decomposing vapors at 1 atm total pressure.

For the fluidized bed decomposer we rely on tests that have been carried out at JRC Ispra and GA. Their results at temperatures in the 773-1173 K range indicate that Incoloy-800H and Inconel-625 are the best materials with anticipated lifetimes of a few years. A substantial gain in lifetime can be gained by coating Incoloy-800H with a protective aluminide. Tests at GA have shown that this type of coating stabilizes in weight change behavior after an exposure of 200-300 hours in SO_3/H_2O decomposing vapors, and further demonstrates self-healing characteristics. We anticipate a useful life of ~ 20 y for aluminide-coated Incoloy-800H. Our selection based on corrosion considerations is therefore Incoloy-800H for the heat exchanger material, with an aluminide coating on the SO_3/H_2O side and bare metal on the He side.

Extrapolated creep strength data on Incoloy-800 indicate that the strength may be marginally acceptable at ~ 1100 K for 1% creep in 100,000 h. We expect Incoloy-800H to be somewhat stronger than Incoloy-800, and therefore anticipate that Incoloy-800H will meet creep strength requirements as well as corrosion requirements for the fluidized bed decomposer heat exchanger.

Conclusions and Recommendations

Some examples of how the four major study points cited in Part A of this Executive Summary could be expanded follows:

- Refine the helium-cooled Li_2O blanket that provides the process heat fraction at 850-950 K. (We may wish to push this temperature somewhat higher as a parameter.) This would include more detailed analyses in the areas of: thermal hydraulics, stress analysis, tritium recovery, system integration with the chemical plant, neutronics, induced activity, LOC considerations, etc., and would use experimental property data obtained on Li_2O .
- The fabrication of a small-scale laboratory test version or segment of the Li_2O blanket would help the above analytical work measurably. Experimental studies would include the development of fabrication techniques for Li_2O , material characterization, mechanical property measurements, thermal stress resistance, compatibility with materials of construction, and tritium (or hydrogen mockup) release rates. Flow rates, pressure drops, flow disturbances and many other phenomena could be observed and measured.
- We believe it is particularly advantageous to further develop and refine this Li_2O blanket module so that it could not only be useful for the synfuel application but would be equally useful as an energy source for the production of electricity or in a co-generation mode where the reactor could be used to produce both synthetic fuel and electrical energy. This is a particularly strong advantage of this blanket concept. The concept may be adopted for the Mirror Advanced Reactor Study (MARS), either by its total inclusion as the entire blanket array for electricity/synfuels or by using the Li_2O for the synfuel application only.
- Carry out calculations and conduct experiments on areas requiring additional information in the H_2SO_4 part of the

thermochemical cycle. As examples, calculational information needs to be developed on the energetics of removing water from H_2SO_4 and boiling of the H_2SO_4 . On the experimental side, we need to set up and test a Joule-Boosted laboratory-scale reactor to evaluate such factors as materials corrosion, catalytic activity of the SiC surface, heat transfer, decomposition yields and back reaction in the products.

- A key area in the thermochemical cycle that needs substantial improvement is the separation and purification of HI from the aqueous medium. As currently performed, using H_3PO_4 as a drying agent, the process requires a very major amount of electrical power and high capital cost. New methods that are under development at GA (currently filing for patents) need to be tested on a laboratory scale at GA and incorporated into our fusion/synfuels process.
- Continue to look at advanced system designs with emphasis on improved conversion efficiencies and lower fuel production costs or lower costs for other marketable products such as basic chemicals. An example of the latter would be to investigate a system design that combines the hydrogen produced by a thermochemical cycle with coal to then produce higher energy methanol fuel as a product.

2.0

JOULE-BOOSTED DECOMPOSER CONCEPT*

One of the most significant advances in the Synfuels From Fusion Project at LLNL this past year was the introduction of the Joule-Boosted Decomposer concept. In this section we briefly describe the main elements of that concept as an introduction to the reader. Technical details are supplied in later sections of this report. By joule-boosting, we refer especially to the utilization of electrically heated commercial SiC furnace elements in place of using a heat exchanger to transfer process heat to the highest temperature step of a thermochemical hydrogen cycle, i.e., in our case the SO_3 decomposition step in the GA Sulfur-Iodine Cycle.

The use of Joule-boosting can significantly lower temperature and materials requirements for the blanket, and in addition improves the operation and reliability of the high temperature process units in the Chemical Plant. Tandem Mirror Reactors have a distinct advantage in coupling with a Joule-Boosted Decomposer design in that surplus electricity in the Direct Converter can be used to provide a significant fraction of the electrical requirements for the Decomposer. In future D-D Cycle Reactors, sufficient net electricity (~25% of the total output power) could be available to provide all of the electrical requirements for the Joule-Boosted Decomposer. We therefore view the Joule-Boosted Decomposer concept as an important breakthrough toward achieving the Fusion/Synfuels tie.

2.1

Advantages in Blanket Design

The most important advantage of this approach from the TMR Blanket standpoint is that the temperature requirements for the blanket are reduced by about 200 K. This greatly reduces the severity of a number of materials problems for the blanket, especially in regard

*

The Joule-Boosted Decomposer concept was jointly conceived of by Dick Werner, Oscar Krikorian, and Terry Galloway, and has been submitted to DOE for patent applications.

to materials corrosion and materials creep strengths. Corrosion and creep strength are design limiting factors for many blanket designs, and they are materials characteristics that degrade exponentially with increasing temperature. Hence, the reduction of blanket temperatures by 200 K is a major change, and puts us into a new regime where more reliable and lower cost materials can be utilized in blanket designs.

2.2

Application in the Chemical Plant

The Joule-Boosted Decomposer (see Figure 2.2-1) is used in the Chemical Plant to decompose SO_3 into SO_2 and O_2 . The decomposer design is relatively simple. Electrical power is supplied to commercially available SiC heating elements arranged in an array with the decomposing gases passing through the array. Electrodes are cooled in isolated end chambers, and the vessel consists of a silica brick-lined Teflon-coated steel. The heating elements are operated at a temperature of ~ 1250 K, which is a very conservative temperature for SiC, and the gas which consists of SO_3 , SO_2 , O_2 , and H_2O , is at a total pressure of 7 atm.

In considering further the advantages of the Joule-Boosted Decomposer in the Chemical Plant, we find the following:

- A SiC furnace decomposer unit is simpler, more reliable, and much lower-cost than a heat exchanger unit for providing heat for SO_3 decomposition. SiC furnaces have proven to be reliable low-cost designs through many years of use, and recent work at ORNL has shown excellent compatibility of SiC with the gases present in the SO_3 decomposer.
- The Joule-Boosted Decomposer gives higher decomposition yields for SO_3 than with a heat exchanger design ($\sim 80\%$ vs 50%). This means less recycle of SO_3 , smaller and less costly equipment for a number of process units, and reduced pumping power for circulating of chemicals.

Figure 2.2-1 Design for the Joule-Boosted SO_3 Decomposer.

- There is no need for a catalyst in the Joule-Boosted Decomposer, since it operates at a sufficiently high temperature (~ 1250 K) that SO_3 decomposition kinetics are rapid. This not only reduces costs but improves reliability.
- The Joule-Boosted Decomposer provides the best possible safety isolation of the Chemical Plant from the Blanket, and vice versa, i.e., the Chemical Plant needs to be isolated from tritium in the Blanket, and the Blanket structure and liquid metals need to be isolated from the O_2 and corrosive sulfur-containing gases in the decomposer.

A disadvantage of the Joule-Boosted Decomposer concept is that the efficiency for hydrogen production is reduced compared to a heat exchanger approach because of the need to provide additional electrical power. This means that we need to increase blanket capacity to generate the required supplemental electricity. However, since only about 20% of the total energy for the GA Cycle is required for SO_3 decomposition, the use of additional power does not seriously reduce the overall cycle efficiency. Furthermore, the reduced requirement for pumping power for recycle of chemicals as compared with a heat exchanger approach (see above), partially offsets the electrical needs.

Although the Joule-Boosted Decomposer concept greatly alleviates a number of engineering and materials problems, we recognize that important engineering and materials problems still remain in the cycle. For example, we still need to rely on SiC based ceramics for the H_2SO_4 Boiler and for the Multi-Effect Evaporators used to concentrate H_2SO_4 solutions.

In the final analysis, in comparing the Joule-Boosted Decomposer approach with a heat exchanger approach, we need to compare the costs for producing hydrogen (and derived synfuels products). This comparison can only be made by looking at the entire TMR/ H_2 Plant

system. Later in this report, we make such a comparison where an LLNL Heat Pipe Blanket design is coupled with a Joule-Boosted SO_3 decomposer and compared with a Two-Zone Blanket design that uses a heat exchanger to drive a Fluidized Bed SO_3 Decomposer. We find that the hydrogen production costs are identical within the uncertainties of the costing methods. The gain in reliability and safety of the joule-boosted case however presents us with significant advantages.

SECTION 3

THE TANDEM MIRROR REACTOR - THE FUSION DRIVER

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SECTION 3

THE TANDEM MIRROR REACTOR - THE FUSION DRIVER

3.1 GENERAL DESCRIPTION OF THE REACTOR

The tandem mirror is a new reactor concept that was devised by Dimov et al.⁽¹⁾ and Fowler and Logan⁽²⁾ in 1976. In their design, the fusion plasma is confined in a long solenoid terminated at each end by classical magnetic mirrors (plugs). Neutral beam injection maintains the plugs at a density higher than that of the central-cell. Consequently, the electrostatic potential in the plugs is higher than in the central-cell, thus forming a barrier to the leakage of central-cell ions. However, this configuration has two technical disadvantages:

1. the use of large amounts of injection power⁽³⁾ and therefore a limited Q, which is the ratio of power out-put over power in-put,
2. a low β_c ratio of the central-cell plasma pressure to the magnetic pressure. Above this β_c limit, ballooning modes in the central-cell may still grow despite perturbations going to zero at the plugs.

In April 1979, D.E. Baldwin and B.G. Logan⁽⁴⁾ of the Lawrence Livermore Laboratory presented an improved version of a tandem mirror reactor, where the power in-put requirement was dramatically reduced. They proposed adding thermal barriers between the central-cell and the plugs. By creating a potential drop at the plugs' entrances, plug electrons become thermally insulated from central-cell electrons. This allows the increase of end plug potential by merely heating plug electrons rather than heating all electrons in the machine or increasing density by neutral beam injection as in the earlier model. The resulting decrease in power in-put to the plasma allows much higher Q values. This configuration was used in the fusion/synfuel study reported last year.⁽⁵⁾ Despite this improvement, the reactor performance is

still limited by the low β_c needed for stability. A low value of β_c implies low fusion power produced per unit volume. For a reactor this suggests a high plasma radius which means a higher power consumption, thus a smaller Q value. The addition of A-cells is an attempt to improve the reactor performance. As shown in Fig. 3-1, A-cells are auxiliary mirror cells added beyond the yin-yang mirror cells of a standard tandem mirror magnet set. Electron cyclotron resonance heating in the A-cell establishes the peak potential barrier to confine center-cell ions; and the yin-yang cell retains the function of providing MHD stability to the whole configuration. In this way, the functions of electrostatic confinement and MHD stability are separated, which allows the yin-yang cells and the A-cells to be better optimized in achieving their respective functions.

Figure 3-2.a illustrates the magnetic field generated by the magnet set of Fig. 3-1. Also shown are the potential and density profiles at one end of the reactor. They are tailored by the magnetic field, the neutral beams, the ECRH heating, and by charge-exchange pumping of ions by means of

- In the center-cell, neutral beams maintain the ion density as well as the ion temperature.
- In the yin-yang cell, the neutral beams give hot ions which provide the beta necessary for MHD stability.
- The thermal barrier at the point (b) serves to insulate colder center-cell electrons from the heated electrons in the outer potential peaks. The cold ions are prevented from accumulating in the thermal barrier potential depression by charge-exchange pumping. The ECRH at the thermal barrier minimum serves to reduce the fraction of cold center-cell electrons passing into the A-cell and confine them magnetically. By reducing the cold electron density required for quasi-charge neutrality, the thermal barrier potential minimum is further depressed.

Fig. 3-1 Tandem mirror reactor
with A-cell barriers

Fig. 3-2 Profiles of field B , potential ϕ , and density n vs distance z at one end of a Tandem mirror reactor operated with A-cell-type thermal barriers.

• The potential maximum in the A-cell is formed by heating electrons trapped within the outer peak in ion density. This outer peak is the result of neutral beam injection off the midplane in such a manner that ions "slosh" between magnetic maxima B_{mb} and B_{ma} . Together with charge-exchange pumping at the midplane, the injection off-midplane peaks the density and potential off-midplane so that cold ions can be trapped between the peaks to stabilize loss-cone instabilities.

3.2 THE PLASMA MODEL

The physics model for this reactor is described in detail in a Lawrence Livermore Laboratory document entitled "Physics Basis for MFTF-B."⁽⁶⁾ The model assumes no buildup of alpha-particles in the reactor. It furthermore assumes that neutrals produced by charge-exchange of the pumping beam with ions in the barrier cell necessarily leave the plasma, and that radial losses (particle and energy) are negligible compared to axial losses. The model also assumes radial density profiles of the form

$$n_c = n_0 \left[1 - \left(\frac{r}{r_c} \right)^\alpha \right] \quad (3-2-1)$$

Similar profiles are used for all the regions in the reactor. The fusion power P_{fus} and the betas are derived from:

$$\begin{aligned} P_{fus} &= \int_0^{r_c} \frac{1}{4} n_c^2 \langle \sigma v \rangle_{DT} E_{fus} \cdot 2\pi r \, dr \cdot L_c \\ &= \frac{1}{4} n_{0c}^2 \langle \sigma v \rangle_{DT} E_{fus} \pi r_c^2 L_c \frac{\alpha^2}{(\alpha+1)(\alpha+2)} \end{aligned} \quad (3-2-2)$$

and

$$\bar{\beta}_c = \frac{1}{\pi r_c^2} \int_0^{r_c} \beta_c \cdot 2\pi r \, dr = \frac{\alpha}{\alpha+2} \beta_c \quad (3-2-3)$$

Equation (3-2-2) suggests the definition of a zero dimensional radius:

$$r_{co} = r_c \frac{\alpha}{[(\alpha+1)(\alpha+2)]^{1/2}} \quad (3-2-4)$$

The different terms in the above equations are:

r_c	the center-cell radius
n_c	the center-cell ion density
L_c	the center-cell length
α	profile parameter
$\langle \sigma v \rangle_{DT}$	fusion reaction rate averaged over a maxwellian plasma
β_{co}	center-cell plasma beta on axis.

3.2.1 Reactor Dimensions and First-Wall Loading

The center-cell radius is derived from the fusion power equation:

$$r_c = \left(\frac{P_F}{0.25 n_c^2 \langle \sigma v \rangle_{DT} E_{FUS} \pi L_c} \right)^{1/2} \frac{[(\alpha+1)(\alpha+2)]^{1/2}}{\alpha} \quad (3-2-5)$$

where $\langle \sigma v \rangle_{DT}$ is modeled by:⁽⁷⁾

$$\langle \sigma v \rangle_T = \frac{3.05 \cdot 10^{-13} \exp(-17.7/T_c^{.348})}{(1.0 + .1554 T_c^{1/3} - .05 T_c + .0364 T_c^{4/3})} \quad (3-2-6)$$

T_c being the center-cell ion temperature. The plug radius is given by flux conservation:

$$r_p = r_c \left[\frac{\beta_{oc,vac}}{\beta_{op,vac}} \left(\frac{1-\beta_c}{1-\beta_p} \right)^{1/2} \right]^{1/2} \quad (3-2-7)$$

which gives a volume

$$V_p = \frac{4}{3} \pi r_p^3$$

where $B_{oc,vac}$ and $B_{op,vac}$ are the vacuum magnetic fields in the central-cell and the plugs respectively. The A-cell radius follows from

$$r_A = r_c \left[\frac{B_{oc,vac}}{B_{ob,vac}} \left(\frac{1 - \bar{\beta}_c}{1 - \bar{\beta}_b} \right)^{1/2} \right]^{1/2}, \quad (3-2-8)$$

where $B_{ob,vac}$ is the barrier vacuum magnetic field. The first wall loading is derived from

$$\Gamma = \frac{E_n}{E_{FUS}} \frac{P_F}{2\pi (r_c + 3x \frac{0.2711}{B_{oc,vac}}) L_c}, \quad (3-2-9)$$

where $E_n = 14.1$ Mev, $E_{FUS} = 17.6$ Mev and $3 \times \frac{0.2711}{B_{oc,vac}}$ represents a length equal to three alpha particles gyroradii.

3.2.2 Plasma Densities and Potential Profiles

The center-cell density is derived from the center-cell beta equation:

$$n_c = \frac{\beta_c B_{oc,vac}}{2\mu_0 (T_c + T_{ec} + 8.3 \cdot 10^{10} \langle \sigma v \rangle T_{ec}^{1.5} E_\alpha F_{\alpha e})} \quad (3-2-10)$$

where T_c and T_{ec} are the center-cell ion and electron temperatures, E_α is the alpha creation energy, and $F_{\alpha e}$ the fraction of alpha-particle energy deposited into electrons. $F_{\alpha e}$ is given by: (8)

$$F_{\alpha e} = 0.88155 \exp(-T_{ec}/67.4036) \quad (3-2-11)$$

Similarly, the yin-yang midplane density is derived from the yin-yang beta equation:

$$n_p = \frac{\beta_p \frac{B_{op,vac}}{2\mu_0} + n_c (0.9 E_p - T_c) \exp(-\frac{\Delta\phi_{pc}}{T_c})}{0.9 E_p + T_{ec}} \quad (3-2-12)$$

The factor 0.9 appears because neutral beam injection causes most of the plug energy to be perpendicular. The potential $\Delta\phi_{pc}$ is indicated in Fig. 3.2. In the above equation, the hot ion pressure is

$$2 \left[n_p - n_c \exp \left(\frac{-\Delta\phi_{pc}}{T_c} \right) \right] E_p \text{ where the hot ion density is repre-}$$

$$\text{sented by } n_p - n_c \exp \left(\frac{-\Delta\phi_{pc}}{T_c} \right) .$$

The cold ion pressure and the electron pressure in the plug are

$$2 n_c T_c \exp \left(\frac{-\Delta\phi_{pc}}{T_c} \right) \text{ and } 2 n_p T_{ec} , \text{ respectively.}$$

Fig. 3-3.a shows the ambipolar potential and the magnetic field profiles in the A-cell of a tandem mirror reactor with thermal barriers. Detailed electron- and ion-density profiles are shown in Figs. 3-3.a and 3-3.b. As required by quasi-charge neutrality, the total ion density is equal to the total electron density. There are three classes of ions and three classes of electrons. For the ions, we have:

- magnetically trapped hot ions of density $n_{ih}(z)$
- passing cold center-cell ions of density $n_{ic}(z)$
- cold ions of density $[q_b(z) - 1] n_{ic}$, trapped in the potential well of lower point (b).

For the electrons, we have;

- cold center-cell electron of temperature T_{ec} , and density $n_{ec}(z)$,
- hot electrons of mean energy $\bar{E}_{eh}$, and density $n_{eh}(z)$.
- warm electrons of temperature T_{ew} , and density $n_{ew}(z)$.

Fig. 3-3 Profiles of electron (a) and ion (b) densities within the A-cell barrier, corresponding to Fig. 3-2.

The density of center-cell ions reaching the A-cell midplane is

$$n_{ic}(b) = \frac{n_c}{R_b} \exp\left(-\frac{\Delta\phi_{pc}}{T_c}\right) \left(\frac{T_c}{\pi\phi_b + T_c}\right)^{1/2}, \quad (3-2-13)$$

where

$$R_b = \frac{B_{mp,vac}}{B_{ob,vac} (1-\beta_b)^{1/2} \left[R_p - (R_p-1) \exp\left(\frac{-\Delta\phi_{pc}}{T_c (R_p-1)}\right) \right]}. \quad (3-2-14)$$

with

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

Here R_b is an effective mirror ratio intermediate between the mirror ratio B_p/B_b which uses the mirror midplane field and the mirror ratio B_{mb}/B_b which uses the yin-yang mirror field. The $(1-\beta_b)^{1/2}$ factor in the denominator characterizes the field reduction in the barrier. A similar factor does not appear in the numerator because there is not much plasma at the yin-yang density peaks. The $n_{ic}(b)$ formula was obtained by integrating a maxwellian distribution function over the untrapped region in velocity space of the center-cell ions.

Once the plug and center-cell densities are calculated, the potential $\Delta\phi_{pc}$ can be obtained from

$$\Delta\phi_{pc} \approx T_{ec} \ln \frac{n_p}{n_c}, \quad (3-2-15)$$

which results from using the Boltzman formula $n = n_0 \exp [(E+\phi)/T]$ for the plug and for the center-cell.

If the cold electrons at location (b) (see Fig. 3-3) were still maxwellian, the drop in potential from the yin-yang cell to the A-cell midplane would be given by

$$\phi_b = T_{ec} \ln \frac{n_p}{n_{ec}(b)} = T_{ec} \ln \frac{n_p}{F_{ec} n_b}, \quad (3-2-16)$$

where $n_{ec}(b)$ is the cold electron density at (b), n_p and n_b are the yin-yang and barrier midplane densities, and F_{ec} is the passing cold electron fraction at (b). Because we should have $\phi(z) = \phi_e$ for $n_{ec} = n_c$ and $\phi(z) = 0$ for $n_{ec} = 0$, the cold electron density is assumed to be

$$n_{ec} = n_c \frac{\exp \left\{ -(\phi_c - \phi(z))/T_{ec} \right\} - \exp(-\phi_c/T_{ec})}{1 - \exp(-\phi_c/T_{ec})}, \quad (3-2-17)$$

which gives the barrier potential

$$\phi_b = T_{ec} \ln \left\{ \left(\frac{n_p}{F_{ec} n_b} \right) \left[\frac{\exp(\phi_p/T_{ec})}{\frac{n_p}{F_{ec} n_b} + \exp\left(\frac{\phi_p}{T_{ec}}\right) - 1} \right] \right\}. \quad (3-3-18)$$

The approximation $\exp(\Delta\phi_{pc}/T_{ec}) \approx 1$ has been used. The passing cold electron fraction at (b) is assumed to be⁽⁹⁾

$$F_{ec} = \frac{n_{ec}(b)}{n_b} = \frac{0.8 E_b' n_{ic}(b)}{3.2 E_b' n_{ic}(b) + \frac{B_{ob,vac} \beta_b}{2\mu_0}} \quad (3-2-19)$$

where E_b' is a cutoff energy (energy above which the electrons are passing to the central-cell), given by⁽⁹⁾

$$E_b' = \frac{\phi_e - T_{ec}}{\left(\frac{B_{mp,vac}}{B_{oc,vac} (1-\beta_b)^{1/2}} - 1 \right)} \quad (3-2-20)$$

Here $\delta\phi_{mb} - \delta\phi$ is replaced by $\phi_e - T_{ec}$ and n_b is the total ion/electron density at the point (b). The other parameters were introduced earlier.

If we suppose that most of the energy of the anisotropic mirror-trapped hot electrons is perpendicular, ($E_{eh} = 0.8 \bar{E}_{eh}$), and that these electrons carry all of the pressure in the A-cell, we find

$$\beta_h \approx \frac{4 n_{ec}(b) (1-F_{ec}) \times 0.8 \bar{E}_{eh}}{B_{ob,vac}^2 / 2 \mu_0} \quad (3-2-21)$$

We assume that the hot electron density at (b) is equal to four times the passing cold electron density. The above formula gives the anisotropic mirror-trapped hot electron energy

$$\bar{E}_{eh} = \frac{B_{ob,vac} \beta_b}{3.2 \times 2 \mu_0 n_{ec}(b) (1-F_{ec})} \quad (3-2-22)$$

Finally, the warm electron temperature is modelled by

$$T_{ew} = 0.1537 \exp(20.5 F_{ec}) (\phi_c + \phi_e - T_{ec}) \quad (3-2-23)$$

The above equation is a fit to solutions of a more elaborate warm electron particle balance.⁽¹⁰⁾

3.2.3 The Yin-Yang Plug Equations

The Logan-Rensink plug model⁽¹¹⁾ is used to calculate most of the yin-yang cell parameters. This model is a set of nonlinear equations that can be solved separately. It includes:

- The plug confinement product $(n\tau)_p$ which combines both electron-drag and ion-scattering

$$(n\tau)_p = (n\tau)_{scat} \times (n\tau)_{drag} / ((n\tau)_{scat} + (n\tau)_{drag}) \quad (3-2-24)$$

with

$$(n\tau)_{scat} = 3.9 \cdot 10^{12} E_{injp}^{3/2} \ln_{10} (R_{peff}) / \ln A_{ii} \quad (3-2-25)$$

and

$$(n\tau)_{\text{drag}} = 2.10^{13} T_{\text{ec}}^{3/2} / \ln \Lambda_{\text{ei}} \quad (3-2-26)$$

- The effective mirror ratio

$$R_{\text{peff}} = \frac{R_p}{\left(1 + \frac{\Delta\phi_{\text{pc}}}{E_{\text{injp}}}\right)} \quad (3-2-27)$$

- The average ion loss energy

$$\bar{E}_L = \frac{E_{\text{injp}}}{1 + (\tau_{\text{ii}}/\tau_{\text{drag}})} + \frac{E_p (\tau_{\text{ii}}/\tau_{\text{drag}})}{1 + (\tau_{\text{ii}}/\tau_{\text{drag}})} \quad (3-2-28)$$

with

$$\tau_{\text{ii}}/\tau_{\text{drag}} = .11 \frac{E_{\text{injp}}}{T_{\text{ec}}}^{1.5} \frac{\ln 10^{R_{\text{peff}}}}{\ln (E_{\text{injp}}/\bar{E}_L)} \frac{\ln \Lambda_{\text{ei}}}{\ln \Lambda_{\text{ii}}} \quad (3-2-29)$$

- The electron and ion logarithms

$$\ln \Lambda_{\text{ei}} = \ln (5.7 \cdot 10^{13} T_{\text{ec}} / \sqrt{n_p}) \quad (3-2-30)$$

and

$$\ln \Lambda_{\text{ii}} = \ln \left(1.4 \cdot 10^{15} (T_{\text{ec}} E_p / n_p)^{1/2} \right)$$

- The plug-ion energy balance

$$E_{\text{ih}} = \frac{E_{\text{injp}} - \bar{E}_L}{(n\tau)_p} (n\tau)_{\text{drag}} (E_{\text{injp}} - \bar{E}_L) + \frac{3}{2} T_{\text{ec}} \quad (3-2-31)$$

3.2.4 Central-Cell Particle and Energy Balance

From quasi-neutrality, we have

$$I_{\text{inj}}^c - .5 n_c^2 \langle \omega \rangle_T - \frac{n_c^2}{(n\tau)_{\text{ci}}} = - \frac{n_c^2}{(n\tau)_{\text{ce}}} + I_{\text{inj}}^c \quad (3-2-32)$$

where the Pastukhov⁽¹²⁾ confinement parameters for central-cell ions and electrons are

$$(n\tau)_{ci} = 3.1 \cdot 10^{11} \sqrt{A_i} \sigma_i / \ln \Lambda_{ii} T_c^{1/2} \phi_c \exp\left(\frac{\phi_c}{T_c}\right) \quad (3-2-33)$$

(3-2-33)

$$(n\tau)_{ce} = 3.6 \cdot 10^9 \sigma_e / \ln \Lambda_{ee} T_{ec}^{1/2} \phi_e \exp\left(\frac{\phi_c}{T_{ec}}\right)$$

with

$$\sigma_i = \sigma(R/2) = \left(1 + \frac{1}{R}\right)^{1/2} \ln \left[\frac{\left(1 + \frac{1}{R}\right)^{1/2} + 1}{\left(1 + \frac{1}{R}\right)^{1/2} - 1} \right] \quad (3-2-34)$$

$$\sigma_e = \sigma(R)$$

and

$$R = R_s \frac{B_{ob,vac} (1 - \bar{\beta}_b)^{1/2}}{B_{oc,vac} (1 - \bar{\beta}_c)^{1/2}}, \quad R_s = \frac{B_{vac,a}}{B_{vac,b}} \quad (3-2-35)$$

The different Coulomb logarithms are:

$$\ln \Lambda_{ii} = \ln \left[2 \cdot 10^{15} (2.5 T_{ec} T_c / n_c)^{1/2} \right] \quad (3-2-36)$$

$$\ln \Lambda_{ee} = \ln \left[4.66 \cdot 10^{13} T_{ec} / n_c^{1/2} \right]$$

In Eq. (3-2-32), the current I_{inj}^c (see Fig. 3.2) serves to maintain the ion temperature and density needed in the center-cell.

The electron energy balance is written:

$$P_{ECRH}(a) + P_{ECRH}(b) + \frac{1.5 q n_c^2 (T_c - T_{ec}) V_c}{(n\tau)_{ei}} + I_{inj}^p (E_{injp} - \bar{E}_L) + q n_c^2 \langle \sigma v \rangle_{DT} E_\alpha F_{ae} V_c = 1.5 I_{inj}^{(a)} \phi_c + \frac{q n_c^2 V_c}{(n\tau)_{ce}} (\phi_c + T_{ec}) \quad (3-2-37)$$

The different physical processes considered in the above equation are;

- a. the ECRH power at (a) of Fig. 3-2, modeled by

$$P_{\text{ECRH}}^{(a)} = \frac{7.479 \cdot 10^{-22} F_{\text{ec}} n_{\text{ec}}^2(b) (\phi_c + \phi_e - T_{\text{ec}}) V_b}{4.0 \cdot 10^8 T_{\text{ew}}^{1/2} T_{\text{ec}}} \quad (3-2-38)$$

- b. the ECRH power at (b) ⁽¹³⁾

$$P_{\text{ECRH}}^{(b)} = \frac{2.344 \cdot 10^{22} (1-F_{\text{ec}}) n_{\text{ec}}^2(b) (\bar{E}_{\text{eh}} - \phi_c - \phi_e + T_{\text{ec}}) V_b}{4.8 \cdot 10^8 \bar{E}_{\text{eh}}^{1.5} n_{10} R_{\text{beff}}} + p_{\text{sync}} \quad (3-2-39)$$

where

$$p_{\text{sync}} = 3.303 \cdot 10^{-20} B_{\text{ob,vac}}^2 n_{\text{ec}}(b) V_b (\bar{E}_{\text{eh}} + \bar{E}_{\text{eh}}^2) \quad (3-2-40)$$

and where

$$R_{\text{beff}} = \frac{B_{\text{mp,vac}} / [B_{\text{ob,vac}} (1-\beta_b)^{1/2}]}{1 + \frac{\phi_e - T_{\text{ec}}}{\bar{E}_{\text{eh}}}} \quad (3-2-41)$$

is an effective mirror ratio (for the hot electrons) reduced by the expelling potential drop $\delta\phi_{\text{mb}} - \delta\phi_b$ (approximated by $\phi_e - T_{\text{ec}}$).

- c. the energy transfer between the central-cell ions and electrons, with the energy exchange parameter

$$(n\tau)_{\text{ei}} = 2.5 \frac{10^{13} T_{\text{ec}}^{3/2}}{\ln \Lambda_{\text{ei}}} \quad (3-2-42)$$

where

$$\ln \Lambda_{\text{ei}} = \ln (9.34 \cdot 10^{13} T_{\text{ec}} / n_c^{1/2}) \quad (3-2-43)$$

- d. the plug neutral beam power where the current I_{inj}^p is calculated from

$$I_{inj}^p = \frac{q n_{ih}^{(p)} n_p v_p}{(n\tau)_p} \quad (3-2-44)$$

- e. the alpha particle heating of electrons
- f. the neutral beam power at (a) with

$$I_{inj}^{(a)} = \frac{q n_a^2 V_{A-cell}}{K_s \sqrt{2.5} E_{inja}^{3/2} n_{10} R_{aeff}} \quad (3-2-45)$$

where the coefficient K_s is obtained by fitting the above current to match the effective sloshing ion $(n\tau)$ parameter. The result is $K_s = 4.2 \times 10^{10}$.

- g. the Pastukhov energy loss where each lost electron carries out an energy equal to $(\phi_e + T_{ec})$.

The center-cell ion energy balance is given by:

$$\begin{aligned} & \frac{1}{4} n_c^2 \langle \sigma v \rangle \frac{d}{dt} \left[E_\alpha \left(1 + \frac{E_R}{E_{FUS}} \right) F_{\alpha i} - E_R \right] + I_{inj}^p (\bar{E}_L + \Delta\phi_{pc}) \\ & = \frac{q n_c^2 v_c}{(n\tau)_{ci}} (\phi_c + T_c) + \frac{1.5 q n_c^2}{(n\tau)_{ei}} (T_c - T_{ec}) v_c \quad (3-2-46) \end{aligned}$$

The different terms in the above equation represent:

- a. the fractional alpha-particle heating of ions with

$$F_{\alpha i} = 1 - F_{\alpha e} \quad (3-2-47)$$

$E_R \approx 45. + \frac{3}{2} T_c$ is a fusion reactant ion energy.

- b. the escaping yin-yang cell ion power. Those ions fall through the potential drop $\Delta\phi_{pc}$ and go to the central-cell.

- c. the Pastukhov loss of ion energy
- d. the energy transfer between ions and electrons.

3.2.5 Reactor Performance

The reactor Q value is defined as the ratio of the fusion power produced by deuterons and tritons interacting in the central-cell to the total input power:

$$Q = \frac{P_{FUS}}{P_{NBp} + P_{pumpb} - P_{NBA} + P_{ECRHA} + P_{ECRHB}} \quad , \quad (3-2-48)$$

where most of the input powers were defined earlier:

$$P_{NBp} = E_{inj} I_{inj}^p \text{ in Eq. (3-2-42),}$$

$$P_{NBA} = 1.5 I_{inj}^a \phi_c \text{ in Eq. (3-2-43),}$$

$$P_{ECRHA} \text{ from Eq. (3-2-37) and } P_{ECRHB} \text{ from Eq. (3-2-38).}$$

The barrier pumping power is the result of Fokker-Planck calculations. It is equal to the trapping rate of passing cold ions in the thermal barrier of the A-cell⁽¹⁴⁾ multiplied by an injection energy. In a typical auxiliary cell, the ions are pumped out with beams of different energies. In this simplified barrier scheme, an average injection energy of ϕ_b is used, which gives

$$P_{pumpb} = I_{\phi\text{-trap}} \phi_b \approx \frac{q n_{ic}^2 (b) V_{ib} (1 + .185 R_b) \phi_b}{2.28 \cdot 10^9 T_c^{3/2}} \quad . \quad (3-2-49)$$

Here V_{ib} is the volume of plasma between the injection point (a) and the outer magnetic field peak of the yin-yang cell.

3.3 THE PLASMA MODELING CODE

3.3.1 In-put and Out-put Parameters

The computer program used in this study was developed at Lawrence Livermore Laboratory by B. Boghosian.⁽¹⁵⁾ As a first step, the program uses the following in-put parameters:

T_c = central-cell ion temperature
 $\beta_c, \beta_p, \beta_b$ = central-cell, plug, and barrier betas
 $B_{oc,vac}, B_{op,vac}, B_{ob,vac}$ = central-cell, plug, and barrier vacuum magnetic fields
 $R_{p,vac}$ = vacuum magnetic field ratio in the plugs
 P_{Fus} = the fusion power
 α = the profile parameter
 L_c = the central-cell length
 $E_{inj}^{(a)}, E_{inj}^{(p)}$ = the A-cell and plug injection energies
 R_s = the vacuum magnetic field ratio $\frac{B_{vac}^{(a)}}{B_{vac}^{(b)}}$

The out-put parameters consist of:

- a. all plasma densities
- b. temperature-averaged energies of the different particle kinds
- c. different fueling rates and fueling powers
- d. plasma dimensions and first wall radius
- e. the reactor Q value

3.3.2 Program Structure

The program contains four iterative loops (see Fig. 3-4). Starting from the innermost one, these different loops calculate:

- a. the average hot ion plug energy,
- b. the ambipolar potential,
- c. $n_c = \phi_c / T_{ec}$ and T_{ec} using the center-cell quasi-neutrality equation and the center-cell electron energy balance.
- d. $n_c = \phi_c / T_c$, using the center-cell ion energy balance.

After reading the input parameters, the program is started by calculating the ratio between the ionization and charge-exchange cross-sections in the

center-cell

$$\sigma_i/\sigma_{cx} = 2 (110/T_c^8 - 1)/3$$

This ratio can be used to calculate the number of ions lost to charge exchange (ions from the different neutral beams). Because this number is too small and does not affect any particle balance, the above ratio is not used. Before entering the ion energy balance loop, the code also calculates the fusion parameter $\langle\sigma v\rangle_{DT}$ (Eq. (3-2-6)). The next step in the program is to enter the ion energy balance and the quasi-neutrality and electron energy balance loops where estimates of T_{ec} , $\Delta\phi_{pc}$, $n_b = \phi_b/T_{ec}$, $n = \phi_c/T_c$, and $n_e = \phi_e/T_{ec}$ are needed in order to proceed.

The program then calculates:

- F_{ae} using Eq. (3-2-11)
- n_c as given in Eq. (3-2-10)
- r_c and Γ [Eq. (3-2-5) and Eq. (3-2-9)] or r_c and L_c , depending on the input option (see flow-chart).
- The plug radius and volume (Eq. 3-2-7)
- The A-cell length and volume (Eq. 3-2-8)
- The plasma barrier mirror ratio (Eq. 3-2-14)
- The density of central-cell ions reaching the A-cell midplane (Eq. 3-2-13)
- The electron cutoff energy E_b' (Eq. 3-2-21)
- The passing cold electron fraction F_{ec} (Eq. 3-2-19)
- The warm electron temperature (Eq. 3-2-24).

The next step in the program is to enter the $\Delta\phi_{pc}$ loop and to calculate the effective mirror ratio R_{beff} . The program then enters the innermost loop to calculate all the yin-yang cell parameters (Eq. 3-2-12, and

INPUT PARAMETERS: $T_e, \beta_e, \beta_p, \beta_h, B_{oc, vac}, B_{ap, vac}, B_{bh, vac}, R_p, v_{oc}, X_p$ $\begin{cases} -\Gamma \sim -P_{Fav} \\ -L_c \sim -P_{Fav} \end{cases}$ $\begin{matrix} (a) & (p) \\ E_{in} & E_{in} \end{matrix}$ $\frac{B_{vac}(a)}{B_{vac}(b)} = \text{slosh}$

Fig. 3-4 TMR Program Flow Chart

Eq. 3-2-24 through Eq. 3-2-31). Once the right value of E_{ih} is reached, $\Delta\phi_{pc}$ (Eq. 3-2-15) and ϕ_b (Eq. 3-2-16) are computed. The code then iterates until the absolute relative error between two successive values of $\Delta\phi_{pc}$ is very small.

The following in-put powers are then calculated:

- P_{NBp} (Eq. 3-2-44)
- P_{pumpB} (Eq. 3-2-49)
- P_{NBA} (Eq. 3-2-45)
- P_{ECRHA} (Eq. 3-2-38)
- P_{sync} (Eq. 3-2-40)
- P_{ECRHB} (Eq. 3-2-39)

To compute the electron temperature and the ratio $n_e = \phi_e/T_{ec}$, the quasi-neutrality equation (3-2-32) and the electron energy balance (3-2-37) are given the forms $f(T_{ec}, n_e) = 0$ and $g(T_{ec}, n_e) = 0$. If (T_{eco}, n_{eo}) is a guess, we attempt to determine values of h and k such that

$$f(T_{eco} + h, n_{eo} + k) = 0 \text{ and } g(T_{eco} + h, n_{eo} + k) = 0.$$

A development in Taylor series gives:

$$f_0 + h f_{T_{ec}0} + k f_{n_e0} = 0$$

$$g_0 + h g_{T_{ec}0} + k g_{n_e0} = 0$$

where we use the notation

$$f_{T_{ec}0} = \left. \frac{\partial f}{\partial T_{ec}} \right|_{T_{eco}, n_{eo}} \text{ and } f_0 = f(T_{eco}, n_{eo})$$

The above equations result in:

$$h = \frac{- \begin{vmatrix} f_o & f_{\eta_e o} \\ g_o & g_{\eta_e o} \end{vmatrix}}{\frac{\partial(f, g)_o}{\partial(T_{ec}, \eta_e)}} , \quad k = \frac{- \begin{vmatrix} f_{T_{ec} o} & f_o \\ g_{T_{ec} o} & g_o \end{vmatrix}}{\frac{\partial(f, g)_o}{\partial(T_{ec}, \eta_e)}}$$

with

$$\frac{\partial(f, g)_o}{\partial(T_{ec}, \eta_e)} = f_{T_{ec} o} g_{\eta_e o} - f_{\eta_e o} g_{T_{ec} o} .$$

Finally, Newton's method is used to find the root $\eta_c = \phi_c/T_c$ which satisfies the ion energy balance (Eq. 3-2-46). After exiting the outer loop, the central-cell neutral beam current I_{inj}^c (see Fig. 2) is calculated from the central-cell electron particle balance $I_{inj}^c = n_c^2/(n\tau)_{ce}$. The program ends by computing the Q value and by writing the out-put parameters.

3.3.3 Computed Tandem Mirror Reactor Parameters

Tables 3-1 and 3-2 show the reactor parameters for $P_{Fus} = 2301$ MWf, appropriate for coupling to a synfuel plant with a two-zone decomposer. The key output parameters are $Q = 15$ and $Q = 25$, respectively, with a center-cell length of 97.8 m for both reactors. The wall loading is $\Gamma = 2$ MW/m², and the first wall radius equal to 1.5 m in both cases.

Tables 3-3 and 3-4 show the reactor parameters for a synfuel plant with a joule boosted blanket driving the decomposer. The fusion power in this case is $P_{Fus} = 2634$ MWf. The Q-values again are 15 and 25, respectively. Both reactors have a center-cell length of 112 m, a wall loading of 2 MW/m² and a first wall radius of 1.5 m.

Central Cell Output

Density, $\text{cm}^{-3} = 1.34 \text{ E}+14$	Electron temp., $\text{keV} = 37.6$
Ion temp., $\text{keV} = 40$	Length, $M = 97.8$
Radius, $M = 1.29$	$\beta_{\text{cta}} = 0.4$
Vacuum field, $T = 3.77$	
Φ_{he} , $\text{keV} = 258.5$	
Ion confinement, $\text{sec}/\text{cm}^3 = 1.05 \text{ E}+15$	Electron confinement, $\text{sec}/\text{cm}^3 = 7.4 \text{ E}+14$

Plug Output

Total density, $\text{cm}^{-3} = 3.4 \text{ E}+14$	
Hot ion density, $\text{cm}^{-3} = 2.84 \text{ E}+14$	
Injection energy, $\text{keV} = 400$	
Av. ion energy, $\text{keV} = 455.8$	Loss ion energy, $\text{keV} = 185$
Radius, $M = 0.9$	
Vac mid field, $T = 8.7$	
Vac mir ratio = 1.5	$\beta_{\text{ta}} = 0.7$
Confinement, $\text{sec}/\text{cm}^3 = 2.0 \text{ E}+14$	Φ_{ipc} , $\text{keV} = 35.1$

Barrier Output

Barrier vacuum field, $T = 1.8$	
Passing ion density at B, $\text{cm}^{-3} = 2.1 \text{ E}+12$	
EB' , $\text{keV} = 26.4$	EEH , $\text{keV} = 480$
TEW , $\text{keV} = 70.7$	$\text{FEC} = 1.32 \text{ E}-2$
Barrier length, $M = 7.7$	
A-cell mirror field, $T = 5.5$	
Injection energy at A, $\text{keV} = 333$	
$\text{Pla mir ratio} = 5.5$	$\text{EFF mir ratio} = 6.4$
Φ_{ib} , $\text{keV} = 272$	Φ_{ic} , $\text{keV} = 130$

Power Balance

N.B. power at: p, $\text{MW} = 58.35$	A, $\text{MW} = 5.5$
ECRH power at: A, $\text{MW} = 9.4$	B, $\text{MW} = 14.8$
Pumping power, $\text{MW} = 65.7$	
Sync radiation power, $\text{MW} = 12.27$	
Fusion power, $\text{MW} = 2301$	Wall loading, $\text{MW}/\text{m}^2 = 2$
The Bottom Line: $Q = 15$	

Table 3-1 TMR parameters for coupling to the synfuel plant with a two zone decomposer; $Q = 15$.

Central Cell Output

Density, cm⁻³=1.43 E+14

Ion temp., kev= 40

Radius, M= 1.2

Vacuum field, T= 2.7

Phie, kev= 260

Ion confinement, sec/cm³= 1.15 E+15

Electron temp., kev= 38.4

Length, M= 97.8

Beta= .8

Electron confinement, sec/cm³= 7.9 E+14

Plug Output

Total density, cm⁻³= 3.39 E+14

Hot ion density, cm⁻³= 2.76 E+14

Injection energy, kev= 400

Av. ion energy, kev= 460.6

Radius, M= 0.66

Vac mid field, T= 8.65

Vac mir ratio= 1.5

Confinement, sec/cm³= 2.04 E+14

Loss ion energy, kev= 186.2

Beta=0.7

Phipc, kev= 33.15

Barrier Output

Barrier vacuum field, T=2.5

Passing ion density at B, cm⁻³=3.3 E+12

EB', kev= 38.9

TEW, kev= 74.5

Barrier length, M= 5.60

A-cell mirror field, T=12

Injection energy at A, kev= 333

Pla mir ratio= 4

Phib, kev= 263

EEH, kev=592.4

FEC=1.56 E-2

EFF mir ratio=4.87

Phic, kev= 129.6

Power Balance

N.B. power at: p, mw= 21.4

ECRH power at: A, mw= 7.17

Pumping power, mw= 36.9

Sync radiation power, mw= 13.55

Fusion power, mw= 2301

The Bottom Line: Q= 25

A, mw=10.2

B, mw=16.25

Wall loading, mw/m²=2

Table 3-2 TMR parameters for coupling to the synfuel plant with a two zone decomposer; Q=25.

Central Cell Output

Density, $\text{cm}^{-3}=1.34 \text{ E}+14$	Electron temp., keV=37.6
Ion temp., keV=40	Length, M=112
Radius, M=1.29	Beta=0.4
Vacuum field, T=3.8	
Phie, keV=258.5	
Ion confinement, $\text{sec}/\text{cm}^3=1.06 \text{ E}+15$	Electron confinement, $\text{sec}/\text{cm}^3=7.46 \text{ E}+14$

Plug Output

Total density, $\text{cm}^{-3}=3 \text{ E}+14$	
Hot ion density, $\text{cm}^{-3}=2.4 \text{ E}+14$	
Injection energy, keV=400	
Av. ion energy, keV=454	Loss ion energy, keV=183
Radius, M=.95	
Vac mid field, T=8	
Vac mir ratio=1.5	Beta=.7
Confinement, $\text{sec}/\text{cm}^3=2 \text{ E}+14$	Phipc, keV=30.25

Barrier Output

Barrier vacuum field, T=1.8	
Passing ion density at B, $\text{cm}^{-3}=2.6 \text{ E}+12$	
EB', keV=29.2	EEH, keV=399.4
TEW, keV=77	FEC=1.73 E-2
Barrier length, M=7.7	
A-cell mirror field, T=12	
Injection energy at A, keV=333	
Pla mir ratio=5.3	EFF mir ratio=5.5
Phib, keV=259	Phic, keV=130.5

Power Balance

N.B. power at: p, mW=48	A, mW=8
ECRH power at: A, mW=17.1	B, mW=13.5
Pumping power, mW=88.7	
Sync radiation power, mW=11.6	
Fusion power, mW=2634	Wall loading, $\text{mW}/\text{m}^2=2$
The Bottom Line: Q=15	

Table 3-3 TMR parameters for coupling to the synfuel plant with a joule boosted decomposer; $\theta = 15$.

Central Cell Output

Density, $\text{cm}^{-3}=1.43 \text{ E}+14$

Ion temp., keV=40

Radius, M=1.2

Vacuum field, T=2.76

Phi_e, keV=260.4

Ion confinement, $\text{sec}/\text{cm}^3=1.16 \text{ E}+15$

Electron temp., keV=38.4

Length, M=112

Beta=.8

Electron confinement, $\text{sec}/\text{cm}^3=7.96 \text{ E}+14$

Plug Output

Total density, $\text{cm}^{-3}=3.1 \text{ E}+14$

Hot ion density, $\text{cm}^{-3}=2.4 \text{ E}+14$

Injection energy, keV=400

Av. ion energy, keV=459

Radius, M=.68

Vac mid field, T=8.12

Vac mir ratio= 1.5

Confinement, $\text{sec}/\text{cm}^3=2.05 \text{ E}+14$

Loss ion energy, keV=185

Beta= 0.7

Phi_{pc}, keV= 29.7

Barrier Output

Barrier vacuum field, T=2.5

Passing ion density at B, $\text{cm}^{-3}=3.8 \text{ E}+12$

EB', keV=41.95

TEW, keV=80

Barrier length, M=5.6

A-cell mirror field, T=12

Injection energy at A, keV= 333

Pla mir ratio= 3.9

Phi_b, keV=251.6

EEH, keV=518

FEC= 1.9 E-2

EFF mir ratio= 4.4

Phi_c, keV= 130

Power Balance

N.B. power at: p, mw= 18.7

ECRH power at: A, mw=11.1

Pumping power, mw= 45.9

Sync radiation power, mw= 12.9

Fusion power, mw= 2634

The Bottom Line: Q=25

A, mw= 13.4

B, mw= 16.1

Wall loading, mw/m²=2

Table 3-4 TMR parameters for coupling to the synfuel plant with a joule boosted decomposer; Q=25.

SECTION 4
ENERGY BALANCE - HOW THE TANDEM MIRROR REACTOR
IS USED AS AN ENERGY SOURCE

Contributors:

F. L. Ribe, R. W. Werner

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4.0 ENERGY BALANCE - HOW THE TANDEM MIRROR REACTOR IS USED AS AN ENERGY SOURCE

4.1 Limits on Direct Converter Electric Power and Total Thermal Power in Relation to Q

A unique property of the TMR is the direct converter from which plasma and alpha particle energy is transformed into high voltage, direct current power. This power would normally be used to furnish the injection requirements of the reactor, both ECRH and neutral injection, but depending on Q, the fusion power gain, appreciable electric and thermal power is left over to help drive the synfuel plant, adding to the thermal power from the first wall and blanket. This is depicted in the energy flow diagram of Fig. 4.1-1 where we establish the following quantities:

$$AM = \text{the blanket thermal energy} \quad (4.1)$$

$$B(1-\eta_{dc}) = \text{the direct converter thermal energy} \quad (4.2)$$

$$B\eta_{dc} = \text{the direct converter electrical energy} \quad (4.3)$$

$$C = \text{thermal energy contribution from injected neutrals} \quad (4.4)$$

where, for the D-T cycle:

$$A = 0.8 P_{inj} \eta_A Q \quad (4.5)$$

(the neutron energy fraction)

$$B = 0.2 P_{inj} \eta_A Q + P_{inj} \eta_A \quad (4.6)$$

(the alpha or charged particle fraction)

$$C = P_{inj} (1 - \eta_A) \quad (4.7)$$

(the fraction of injected neutrals that don't get ionized)

The definitions of terms needed for Fig. 4.1-1 are:

$$r_A = \text{ionization efficiency}$$

$$M = \text{blanket energy multiplication (from exothermic reactions)}$$

72-279-2

Fig. 4.1-1 Power Balance-Mirror Reactor with Direct Conversion Process Heat Production for Synfuels

η_{inj}	=	injector efficiency
η_{dc}	=	direct converter efficiency (converting charged particle kinetic energy to electrical)
η_{TH1}	=	thermal converter efficiency (converting thermal energy to electrical)
η_{TH2}	=	thermal converter efficiency (converting direct converter thermal energy to electrical)
P_{AUX}	=	power required for pumps, refrigeration, and other fusion plant needs
Q	=	fusion power produced/power injected

The value of P_{aux} is taken as a fraction, f , of the thermal energy before conversion

$$P_{aux} = f (AM + C) \quad (4.8)$$

By definition

$$P_{circ} = P_{inj}/\eta_{inj} \quad (4.9)$$

4.1.1 The Influence of Q

To use the reactor as an energy source supplying process heat for synfuel (H_2) production, a stipulation is made: the dc electrical energy component $P_{dc} = B\eta_{dc}$ of the direct converter is first used to satisfy the circulating power needs of the reactor. There will be a specific value of Q at which this demand is exactly satisfied. For higher Q values, there will be a surplus of dc electrical energy that can be used by the thermochemical plant. For lesser values of Q , some thermal energy from the reactor blanket or from the thermal part of the direct converter would have to be converted to electrical energy to help drive the reactor. Table 4.1-1 shows the various power quantities of Fig. 4.1-1 as functions of Q for the following assumed values: $\eta_{INJ} = 0.6$, $\eta_{DC} = 0.60$, $P_{AUX} = 0.02 (AM + C)$ and $M = 1.2$, which is appropriate for the joule-boosted heat pipe reactor

Table 4.1-1 Energy balance as a function of Q for a chemical plant energy demand of 2930 MW

4-1

1	Output from Reactor			Input to Reactor		Energy Available to Synfuel Process Chem. Plant		
	2	3	4	5	6	7	8	9
Reactor Q	Thermal energy from blanket = AM	Thermal energy from direct conv. = $B(1-\eta_{DC})$	Elec. energy from direct conv. = $B(\eta_{DC})$	Elec. energy to drive the reactor	Aux. energy to reactor $P_{AUX} = f(AM + C)$	As thermal energy $2 + 3 - 6$	As electric HVDC $4 - 5$	Total $7 + 8$
5	2844	474	711	1042	57.5	3260	-330	2930
10	2640	330	495	483.5	53.0	2918	11.6	2930
Joule Boost. 15	2581	287	430	315	51.8	2815	115	2930*
Therm. Driven 15	2488	250	374	274	45.0	2692	100	2792**
20	2551	266	398	233	51.2	2765	165	2930
25	2533	253	380	186	50.7	2736	194	2930
30	2522	245	368	154	50.5	2716	214	2930
40	2507	235	353	115	50.2	2692	238	2930
50	2499	229	344	91.5	50.0	2678	252	2930

Assumes: $\eta_{DC} = .60$, $\eta_{INJ} = .60$, $M = 1.2$, $f = .02$, $\eta_A = .95$.

* Reference case.

+ $M = 1.33$ instead of 1.2

blanket of Section 5.0. It may be seen that the net direct converter power becomes positive at $Q \approx 10$.

A value $M = 1.33$ is appropriate for the two-temperature zone blanket of Section 6.0. Two M values and a Q of 15 are used to fix the reactor fusion power to accommodate the synfuel plant power requirements as developed in Section 9.8 and summarized below in Section 4.2 and 4.3. The derived values of P_F , the fusion power, are used as input to size the TMR fusion driver in Section 3.

4.2 Joule-Boosted Decomposer Summary Energy Balance

A detailed power flow diagram showing the interface of the TMR and the synfuel plant is given in Fig. 9.8-1. In Fig. 4.2-1 is shown a simplified diagram representing the essentials of the Brayton-topping, Rankine-botcoming power interface. The total thermal power $P_{TA} = 2816$ MWt matches the assumed hydrogen power output of 1440 MWt (4876 g-mole/sec at 50 atm). The plant thermal efficiency is $1440/(2816 + 115) = 0.497$. From Fig. 4.2 the fusion power is $P_F = 2688$ MWf.

4.3 Two-Zone Blanket, Thermally Driven Decomposer, Summary Energy Balance

A description is given for the TMR two-zone blanket and synfuel plant interface for $Q = 15$ in Section 9.8.3. Figure 4.3-1 is a simplified power flow diagram showing the essentials of the split thermal process power flow, of which 630 MWt tops the Rankine thermal converter which bottoms into the remaining 720 MWt. (The power flow shown here is somewhat different from that shown in Reference 1.) The thermal energy delivered to the synfuel plant is $P_{TA} = 2693$ MW and the H_2 production is 4876 g-mole/sec at 50 atm or equal to 1440 MW. The plant thermal efficiency is therefore $1440/(2693 + 100) = 0.516$. The fusion power is 2338 MWf. The fraction of hot-zone blanket heat is $611/2693 \approx 0.23$.

4.4 Conclusions

For a reference TMR Q value of 15 there is appreciable direct

Fig. 4.2-1 Simplified power-flow diagram of the TMR synfuel system for the heat-pipe blanket and joule-boosted SO_3 decomposer. Assumed values are $Q = 15$, $M = 1.2$.

Fig. 4.3-1 Simplified power-flow diagram of the TMR synfuel system for the two-zone blanket and thermally driven SO_3 decomposer. Assumed values are $Q \approx 15$, $M = 1.33$. Various process temperatures are indicated.

converter power for application to the synfuel process. Summary power flows of the TMR-synfuel plant interface show thermal efficiency of 49% for the joule-boosted decomposer and 51% for the two-zone blanket, thermally driven SO_3 decomposer.

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SECTION 5.0

THE HEAT PIPE LIQUID CAULDRON BLANKET

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5.0

THE HEAT PIPE LIQUID POOL BLANKET

The heat pipe blanket described herein is used in conjunction with the joule-booster SO_3 decomposer discussed in Section 2. In this concept electricity obtained directly as HVDC output from the tandem mirror reactor's direct convertor, supplemented by some electricity that is blanket derived, is used to satisfy in situ the high temperature (~ 1200 K) process demands of decomposing SO_3 to SO_2 within the chemical processor. Generating high temperatures at the processor itself, rather than delivering high temperature process heat ($\sim 25\%$ of the overall process energy demands), eliminates the need for a very high temperature blanket and transport system.

The blanket in this case can operate at a more manageable temperature level, ~ 900 K, as a design goal.

The heat pipe blanket concept selected for this study provides the remaining 75% of the process heat at a temperature in the blanket itself somewhat higher than this 900 K goal.

The SO_3 decomposer can actually run hotter, and thus more effectively, using electrical heating elements, than if the decomposer were one using a fluidized bed such as we discuss in Sections 6 and 9.

5.1

Evolution of the Concept

The blanket concepts for the tandem mirror reactor described in this section were developed to produce process heat ($\sim 850 - 950$ K) for the General Atomic sulfur-iodine thermochemical cycle for producing hydrogen described in Section 9.

In our first year of study on fusion-synfuels our original liquid pool blanket concept was chosen to obtain a high-temperature blanket (~ 1200 K) to satisfy the then known temperature demands of the GA cycle.⁽¹⁾ Figure 5.1-1a describes how the cauldron works. Figures 5.1-1b and 5.1-1c show how these blankets are assembled in the reactor's central cell.

Fig. 5.1-1a,b Sketch of the Cross Section of the Original Liquid Cauldron Blanket Concept

Fig. 5.1-1c Cross Section through Two Unit Cells of the TMR. Each unit cell consists of four blanket modules and one solenoidal coil. The unit cells are joined in series to form the main central cell of the reactor. The axial distance between coils is approximately 4 meters.

During the course of the year's design study, two related things occurred:

1. We invented and filed patent on the Joule-boosted decomposer, effectively decreasing the high temperature process heat demand imposed on the reactor blanket from 1200 K to ~ 850 -950 K.
2. We found that the void fraction in the Li-Na cauldron blanket was excessive ($\sim 60\%$) when operated at 1200 K. This high void was due to a limited bubble rise velocity of ~ 0.8 m/s, further slowed by MHD effects due to the reactor's magnetic field. The product of velocity, vapor density, and latent heat of vaporization was simply too low at 1200 K to remove the energy from the blanket.

To resolve this void fraction problem we could have increased the cauldron temperature to ~ 1400 K, thereby increasing the vapor density fourfold and decreasing the void to $\sim 15\%$. For very high temperature blankets this would be a good solution. However, with the Joule-boosted decomposer breakthrough relaxing our blanket temperature design requirements to 850-950 K, we elected instead to introduce heat pipes into the cauldron volume. The heat pipes permit vapor velocities approaching sonic velocity, thereby overcoming the vapor velocity limitation of the boiling cauldron and permitting operation of the lithium pool at temperatures more in keeping with the new and lesser demand of 850-950 K.

5.1.1 Vapor Velocities in Heat Pipes

Heat pipes can operate reliably with vapor velocities approaching the speed of sound, so that the vapor volume required by the heat pipes would be negligible compared to the vapor void fraction in a boiling liquid pool at the same temperature. This approach effectively eliminates the void problem. However, with this new approach, vapor volume is not the only consideration. Heat pipes do introduce structural materials into the liquid pool, but the

structural fractions turn out to be relatively small. The major area of study required is the heat transport capability of a heat pipe due to limits other than vapor velocity. Not surprisingly a new set of constraints are introduced. We will now discuss these new constraints.

5.1.2 MHD Pressure Drop Constraint and Its Solution

The MHD pressure drop in the heat pipe liquid is roughly proportional to the wall conductance ratio, $C \equiv (\delta_{w1}\sigma_w/w_{||}\sigma_L)$, where δ_{w1} is the heat-pipe inner-wall thickness in contact with the liquid, $w_{||}$ is the half width of the liquid flow passage parallel to the magnetic field, σ_w is the wall electrical conductivity and σ_L is the liquid electrical conductivity.⁽²⁾ In our efforts to improve the performance of the heat pipes in a relatively strong magnetic field by increasing $w_{||}$ so as to reduce C , we evolved the ideas shown in Fig. 5.1-2.⁽³⁾ We started with a conventional, round heat pipe containing channels on the inner wall that are covered with a fine mesh screen, where $w_{||}$ was on the order of 1 mm. We then went to an annular wick design which increased $w_{||}$ on the upper and lower quadrants of the round heat pipe to values on the order of one-quarter of the pipe diameter. However, this still left $w_{||} \sim 1$ mm in the side regions. The resulting large MHD pressure drops in these side regions would produce such low liquid velocities that local boiling would probably occur. Since we were uncertain as to the effects of such boiling in the annular wick, this concept had to be rejected. We then hit upon the idea of flattening the heat pipe as shown in Fig. 5.1-2d. In this FHP (flat heat pipe) design, $w_{||}$ can be made very large and the side regions can be made very small. Our preliminary estimates indicate that the heat transport capability in a 2 Tesla magnetic field can be increased by an order of magnitude or more over that of a conventional channel-wick heat pipe of Fig. 5.1-2b.

5.1.3 Variable Width Heat Pipe Sheets

As a final step in the evolution of the heat pipe concept, we

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5.1.3 Variable Width Heat Pipe Sheets

As a final step in the evolution of the heat pipe concept, we

Fig. 5.1-2 Evolution of the Heat Pipe Shape and Wick Design for High Heat Transport Capability in a Magnetic Field

decided to fabricate the flat heat pipes in sheets where each sheet is made up of six or more flat heat pipes. These sheets would lie parallel to the magnetic field and be of variable width to get around the solenoidal magnets, as indicated by the sketches in Figs. 5.1-3c and 5.1-4 (design B¹). This has several additional important advantages over individual flat heat pipes of constant width, not the least of which is the possibility of significantly lower fabrication costs (e.g., by forming multiple heat pipes out of large seam-welded sheets). In addition, since the sheet width at the first wall is approximately equal to the width of the module the heat flux to the heat pipe evaporators is reduced over that for narrower individual heat pipes. This will reduce the peak liquid pool temperatures (and also the structural temperatures in the case of a failed heat pipe) compared to the more conventional constant-width heat pipe (design A of Fig. 5.1-4).

As a first approximation, we modelled the stepped width approximation shown in Fig. 5.1-4. The wider evaporators of designs B or B¹ result in higher heat transport capability than for design A. As a result of structural considerations, it was later found necessary to subdivide the flat heat pipe of design B into three equal heat pipes, as indicated by the design labelled C3 on Fig. 5.1-4. (Each individual heat pipe of the set of three is referred to as design C1.) Each of the three heat pipes has a smaller width and is thus better able to support the external pressure of the helium at the condenser end (about 30 atm) and the somewhat lower external pressures (about 2 atm for Li and 6 atm. for Li-Pb) on the evaporator and part of the adiabatic section.

5.1.4 Physical Arrangement -- Coupling to the Heat Exchanger

Figure 5.1-3a indicates how the sheets of heat pipes are arranged at an angle with the horizontal axis to use gravity to help return the liquid in the wick. Figure 5.1-3b illustrates how the sheets of heat pipes are divided into four groups to make a compact,

HEAT PIPE BLANKET MODULE

72-240-1

Fig. 5.1-3 The Heat Pipe Blanket Module
 (a) End view of a TMR heat pipe blanket module showing the four bundles of heat pipes; (b) side view of the reactor showing the arrangement of the four-pass, heat-pipe-condenser heat exchanger (Section A-A); (c) Section B-B of the heat pipe blanket showing a typical sheet of flat heat pipes tapered to fit around the solenoidal magnet coil.

Fig. 5.1-4 Three Basic Flat Heat Pipe Designs
Top view showing the evolution of the geometry of the reference flat heat pipe C3 required to fit around the solenoidal magnets and to take the external pressure stresses.

four-pass heat exchanger at the condenser end. This multipass arrangement provides the necessary bulk temperature rise of the helium coolant (from 490 K inlet temperature to 875 K at the exit). The arrangement also results in an acceptably low helium film temperature drop of about 44 K under normal operating conditions. A graded insulation layer in the heat pipe condenser wall allows all heat pipes to operate at essentially the same vapor temperature.

5.1.5 Tritium Removal

The heat pipes intrinsically also provide for tritium removal from the liquid pool.⁽⁴⁾ A material with high tritium permeability (niobium, for example) is used for the evaporator section, and one of low tritium permeability such as TZM (a molybdenum alloy) is used for all of the adiabatic and condenser sections except for a "permeation window" at the tip of the condenser. The tritium diffuses through the relatively large area of the evaporator section of the heat pipe at low partial pressures ($\sim 10^{-6}$ torr), is swept to the extreme end of the condenser by the sodium vapor flow, accumulates there at the vapor pressure (0.2 atm), and thus diffuses readily through a window of small area (Fig. 5.7-1).

5.1.6 Fluids Used in the Cauldron Pool

Two different liquids for the pool have been considered: pure lithium and $\text{Li}_{17}\text{Pb}_{83}$. These result in different materials corrosion problems and different maximum structural temperatures, as will be described.

5.2 Heat Pipe Equations For the Wicking Limit

The theory of heat pipe operation is thoroughly covered in Cotter's original paper⁽⁵⁾ and in several subsequent books and papers. In general, the heat transport capability of a heat pipe is limited by the capillary pumping capability of the wick (referred to as the wicking limit), by choking of the flow (referred to as the sonic limit), by the onset of nucleate boiling in the wick, and several

other phenomena as well. In this section, we consider the highly important wicking limit; the other limits will be discussed in a later section.

5.2.1 Wicking Limit Equations

The equations used to model the flat heat pipes are based on those of Cotter for the inertia flow regime with the following modifications;

- (1) Flat heat pipe geometry was accounted for.
- (2) MHD effects on liquid flow were added.
- (3) Different flow areas for the evaporator, adiabatic section and condenser were included.
- (4) The wet point (WP) was not required to be at the end of the condenser, as originally postulated by Cotter.

The inertia flow regime is defined as the case where the vapor velocity from the evaporating liquid is so large that the radial Reynolds number in the evaporator, $Re_r \equiv (\rho_v u_{v(rad)} D_v / \mu_v) \gg 1$.

At the condition of maximum heat transport, Cotter assumed that the liquid pressure equaled the vapor pressure at the end of the condenser so that the liquid meniscus was flat at this point. The point where this pressure equality exists has been referred to as the wet point (WP) by Busse⁽⁶⁾ and others.

It has been postulated by Busse⁽⁶⁾, and Ernst⁽⁷⁾ that the wet point moves from the condenser end (point 3 on Fig. 5.2-1) if the liquid pressure exceeds the vapor pressure in the condenser. We found that the wet point was at point 2, the exit of the adiabatic section, and that the resulting heat transport capability was lowest for this WP location for almost all cases of interest. This is referred to as the EA loop (evaporator plus adiabatic section loop). The maximum heat transport capability, called the wicking limit of the heat pipe, occurs when the liquid and vapor

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Fig. 5.2-1 Pressure Distribution for a Gravity-Assisted Heat Pipe
Typical pressure distribution in the vapor and in the liquid for a gravity-assisted heat pipe with low effective wall conductance ratio, C_{eff} , operating in the EA loop (i.e., with the wet point at x_2) in the inertia flow regime.

pressure drops in these two sections equals the maximum capillary pumping pressure.

The wicking limit is obtained by solving the pressure balance equation (see Table 5.2-1 and the nomenclature for definitions):

$$\Delta p_v(\Delta x) + \Delta p_L(\Delta x) + \Delta p_B(\Delta x) = \Delta p_G(\Delta x) + \Delta p_{c(max)} \quad (5.1)$$

where the pressure drops are evaluated for an appropriate Δx between the wet point and the dry point. The point of maximum separation of the vapor and liquid pressure profiles is called the dry point, DP. If the dry point occurs at x_0 , then $\Delta x = \Delta x_I$ on Fig. 5.2-1. This is the usual case for a horizontal heat pipe or one with the evaporator above the condenser. However, Busse and Kemme⁽⁸⁾ have shown that for a gravity-assisted heat pipe without an adiabatic section, the dry point is often at some location such as DP-II on Fig. 5.2-1 for a straight heat pipe at a constant inclination angle. For our case with an adiabatic section, we have assumed that Δx in Eq. (5.1) should be taken as Δx_{II} , and that the pressure drops should be evaluated between point x_2 and DP-II.

The wicking limits are generally lower for DP-II than for DP-I. However, preliminary calculations indicated that the differences between the wicking limits for the two possible dry point locations, DP-I and DP-II, were usually not large. Consequently, for all the results presented here, the simpler EA loop calculations with the dry point at DP-I were used.

5.2.2

Heat Transport Safety Factor

To account for the somewhat lower wicking limits for DP-II, as well as for other uncertainties in the analysis, a heat transport safety factor, SF_Q , was applied; i.e., the wicking-limited heat flow for the flat heat pipes was required to be this safety factor times the actual heat flow expected under normal operating conditions. This safety factor also allows the nearest-neighbor heat pipes to operate with one failed heat pipe without exceeding the wicking

Table 5.2-1

Summary of Flat Heat Pipe Equations
for the Wicking Limit
in the Inertia Flow Regime

$$(Re_r \equiv \rho_v u_v(\text{rad}) D_v / \mu_v \gg 1)$$

$$\Delta p_c(\text{max}) + \Delta p_g = \Delta p_v + \Delta p_L + \Delta p_B$$

$$\text{where } \Delta p_{c(\text{max})} = \frac{2\gamma}{r_c}$$

$$\Delta p_g(\Delta x) = \rho_L g(\Delta x_E + L_A n_A + L_C n_C) \sin \phi$$

$$\Delta p_v(\Delta x) = \frac{\pi^2 \dot{Q}^2}{8 \rho_v A_{VE}^2 h_{fg}^2} + \Delta p_{VA} n_A - \left(\frac{\dot{Q}^2}{2 \rho_v A_{VC}^2 h_{fg}^2} \right) n_C$$

$$\Delta p_L(\Delta x) = \frac{b \mu_L \dot{Q}}{\rho_L w_{||}^2 h_{fg}} \left(\frac{\Delta x_E}{2 A_{LE}} + \frac{L_A n_A}{A_{LA}} + \frac{L_C n_C}{2 A_{LC}} \right)$$

$$\Delta p_B(\Delta x) \approx \left(\frac{H^2 C_{\text{eff}}}{b} \right) \Delta p_L(\Delta x) \quad \begin{cases} H \gg 1 \\ C \sqrt{H} > 1 \end{cases}$$

$$H \equiv \text{Hartmann Number} = 8 w_{||} \sqrt{\frac{\sigma_L}{\mu_L}}$$

$$C_{\text{eff}} \quad \left\{ \begin{array}{l} \text{Effective wall conductance ratio} \\ \text{for a flattened heat pipe} \end{array} \right\} \approx \frac{2 w_{\perp}}{w_{||}} \left(\frac{\sigma_w \delta w_{\perp}}{\sigma_L w_{||}} \right)$$

$$\Delta x = \Delta x_E + L_A n_A + L_C n_C$$

$$\Delta x_E = \text{Distance from dry point to exit of evaporator, } x_1.$$

$$n_A = 1 \text{ for EA loop and Cotter loop } (= 0 \text{ for evaporator loop})$$

$$n_C = 1 \text{ for Cotter loop } (= 0 \text{ for EA loop and evaporator loop})$$

limits; i.e., it provides for some effective redundancy. A heat pipe can fail to operate due to at least four causes:

1. A pinhole can develop in the evaporator end.
2. A pinhole can develop in the condenser end.
3. Two pinholes can develop, one in the evaporator and one in the condenser.
4. Structural failure resulting in a collapsed heat pipe.

In all four cases the heat pipe fails to function; i.e., it is incapable of heat flux transport. Physically it is assumed to remain in place while nearby heat pipes pick up the load.

In addition to the above considerations, the safety factor helps compensate for the approximation used in most of the calculations that the heat input was uniform along the evaporator. In reality, the neutronic internal heat generation falls off approximately exponentially as we go into the liquid pool with an e-folding length on the order of 0.22 m. This results in somewhat lower wicking limits than the uniform heat input case (but usually not more than 10% lower).⁽⁹⁾ The safety factor, SF_Q , we have chosen is 1.5.

The integral equations used for our EA loop calculations are summarized in Table 5.2-1. (An alternative finite difference computational scheme developed by Rowe is described in Reference (10.)) Other loops with the wet points in other locations were calculated as well and verified that we were always taking the case with the lowest wicking limit. We also verified that the radial Reynolds numbers, Re_r , were much greater than unity, so that the vapor flow was always inertia-dominated in the evaporator. In the adiabatic section, turbulent incompressible flow was assumed for the calculation of the wicking limit. The liquid flow was assumed to be in the viscous flow regime. The heat pipe working fluid chosen was sodium, and operation at a vapor temperature of 1000 K

at the beginning of the evaporator was assumed for the preliminary design calculations.

5.2.3 Flat Heat Pipes and Lower Effective Wall Conductance

A very important scaling law is that used to account for the beneficial effect of the larger $w_{||}$ of the flat heat pipes in reducing the MHD pressure drop. While no complete theory exists for this geometry, a simplified model indicates that the effective wall conductance ratio is roughly:

$$C_{eff} \approx C \left(\frac{2w_{\perp}}{w_{||}} \right) \approx \left(\frac{\delta_{w1} \sigma_w}{w_{||} \sigma_L} \right) \left(\frac{2w_{\perp}}{w_{||}} \right) \quad (5.2)$$

(See Fig. 5.2-2 for definitions of $w_{||}$ and $w_{\perp}$.) This implies that C_{eff} is inversely proportional to $w_{||}^2$. It should be noted that a lower value of C_{eff} will result in a higher heat pipe wicking limit. A result similar to Eq. (5.2) can be obtained from a theoretical analysis of Hunt⁽¹¹⁾ for a related problem of MHD flow in a rectangular duct for the limiting case of $C \sqrt{H} > 1$, where the Hartmann number, H , is defined in Table 5.2-1. As a result, this scaling law was adopted for our study.

For the flat heat pipe design A of Fig. 5.1-4, a conservative reference value of $C_{eff} = 0.001$ was determined using Eq. (5.2). This same value of C_{eff} was used for the entire flat heat pipe design B of Fig. 5.1-4, even though Eq. (5.2) indicates that C_{eff} should be less than 0.001 for the evaporator. This is an additional "safety factor" in our calculations.

5.3 Flat Heat Pipe Design and Construction

The cross section of a flat heat pipe is shown in somewhat idealized form on Fig. 5.2-2. A sandwich wall construction with a silicon carbide or alumina insulation layer is necessary in the evaporator and adiabatic sections to prevent the MHD-induced eddy currents from circulating out into the liquid pool; i.e., to keep the inner wall thickness, δ_{w1} , small. As a result, the wall

Fig. 5.2-2 Cross Sections of Flattened Heat Pipe Evaporator
 (a) Original thick-walled heat pipe construction permits excessive eddy currents and MHD pressure drops. (b) Improved sandwich-wall construction results in acceptably small eddy currents and MHD pressure drops.

thickness for the calculation of C_{eff} is the relatively low value, $\delta_{wl} = 0.4$ mm. The condenser also requires a sandwich construction since the total heat pipe wall thickness required to take the helium pressure would result in too high a value of C_{eff} . The tentative choices for the materials for the flat heat pipes are listed in Table 5.3-1 for the tritium removal scheme.

5.3.1 Maximum Material Temperatures

The maximum heat pipe material temperatures shown in Table 5.3-1 are those on the surface of a failed heat pipe. This was felt to be the most logical abnormal operating condition to use as a design goal. These material temperatures are the ones which occur for reference heat pipe designs B or C3 with 51 flat heat pipes per square meter (17 rows of heat pipe sheets with three type B or C3 flat heat pipes per sheet).

5.3.2 Heat Transport Design Goals

The tradeoffs between the number of flat heat pipes per square meter, heat transport requirements and resultant void fractions and structural fractions are shown on Fig. 5.3-1. For 51 heat pipes per square meter and a nominal neutron wall loading of 2 MW/m^2 , each heat pipe must carry about 47 kW under normal operating conditions. With a heat transport safety factor, $SF_Q = 1.5$, the heat pipes must then be designed to carry 70 kW at the wicking limit. The resultant average void fraction near the first wall is about 31% for pure Li and 36% for $\text{Li}_{17}\text{Pb}_{83}$, while the structural fraction is about 4.5% for pure lithium and about 9.5% for $\text{Li}_{17}\text{Pb}_{83}$ (higher than for pure Li due to its higher density and higher hydrostatic loads on the lower heat pipes).

In summary, the reference design goals were to find a flat heat pipe design with a wicking limit of at least 70 kW at a nominal heat pipe vapor temperature around 1000 K. In addition, we specified that this 70 kW wicking limit should be obtainable at relatively low inclination angles of 20° , because the reactor

Table 5.3-1 Tentative Choice of Heat Pipe Materials (See Fig. 5.2-2)

Pool Fluid and Maximum Temperatures(a)	Tritium Removal Scheme	Composite Heat Pipe Wall Materials			
		Evaporator Section	(Nominal Thickness)	Adiabatic Condenser Section	
Pure Li(h) in pool (Max. structural temp. ~ 1050 K to 1130 K)	Permeation into HP evaporator, and removal from permeation "window" in condenser	Nb alloy(c) Flashed Mo(d) SiC(e) Flashed Mo Nb alloy	(1.6 mm) (microns) (0.2 mm) (microns) (0.4 mm)	TiZr Tungsten ^{d,f} SiC ^{d,f} Tungsten TiZr	(1.5 mm) (microns) (0.2 mm) (microns) (0.04 mm)

- The maximum temperatures are for normal operation and for one failed HP, respectively.
- The material choices are tentatively the same for $\text{Li}_{17}\text{Pb}_{83}$ as for pure Li. However, the maximum structural temperatures would now be about 1160 K to 1300 K, and the corrosion problems would be much more severe (or possibly intractable).
- A tantalum alloy is a possible alternative to the Nb alloy. It would have higher creep strength but lower tritium permeability.
- Both flashed Mo and the tungsten serve as accommodation layers between SiC and the structural alloy.
- The SiC would be porous or it would be deposited in a matrix or precracked to allow tritium permeation into the evaporator.
- A thin layer of tungsten is used to enhance the tritium barrier.

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Fig. 5.3-1 Required Heat Transport Per Heat Pipe
Required heat transport per heat pipe and the resultant average void fraction and structural fraction in the first wall region for flat heat pipe design C3 in a liquid lithium pool.

geometry limits us to rather low evaporator inclination angles for the heat pipes near the bottom of the pool (Fig. 5.1-3).

5.4

Wicking Limits for the Reference Heat Pipe Designs

The three reference heat pipe designs shown in Fig. 5.1-4 all have $L_E = 3L_A = 1.5L_C = 1.5$ m. These are typical lengths required for the curved heat pipes shown in Fig. 5.1-3. Calculations for different evaporator lengths for designs A and C3 are shown on Fig. 5.4-1 for a constant inclination angle of 20° .

From Fig. 5.4-1, it can be seen that as L_E is increased, the wicking limit actually increases somewhat due to the increased gravity head on the liquid return flow in the wick. This is fortunate because the longest heat pipes are near the bottom of the pool where their average inclination angle is rather small (Fig. 5.1-3). Conversely, the shorter heat pipes near the top of the pool can more easily be inclined at higher angles to get additional gravity assistance.

The effect of varying the condenser width of the flat heat pipe designs A, B and C of Fig. 5.1-4 is shown on Fig. 5.4-2 for $\phi = 20^\circ$. For the constant width heat pipe design A with $w_C = 0.10$ m, the wicking limit occurs at an axial heat flux of about 3.8 kW/cm^2 (corresponding to a heat transport capability of about 53 kW). This is insufficient to meet the design goal of 70 kW per heat pipe (with $SF_Q = 1.5$). By widening only the evaporator to 0.33 m, the design B heat pipe has a wicking limit of about 6.4 kW/cm^2 (corresponding to about 90 kW per heat pipe). This more than meets the goal of 70 kW per heat pipe. However, it is difficult to design these wide heat pipes to take the external pressure loads. By subdividing design B into three heat pipes, the resultant design C can take the pressure loads with the addition of external ribs (see Section 5.9).

To meet both the heat transport and the structural requirements, design C was selected. The smaller w_{II} of design C results in a

Fig. 5.4-1 Effect of Evaporator Length on the Heat Transport Capability of the Flat Heat Pipes

Fig. 5.4-2 Effect of Condenser Width on Wick-Limited Heat Transport Capability For constant width and stepped width flat heat pipes of Fig. 5.1-4.

larger value of C_{eff} and a larger MHD pressure drop. This results in a lower wicking limit of about 5.0 kW/cm^2 as shown on Fig. 5.4-2. This just corresponds to 70 kW for the set of three narrow heat pipes (referred to as design C3) for the relatively low inclination angle of 20° . Thus design C meets the reference design goals defined in the preceding section.

The effect of inclination angle on the wicking limit is shown in Fig. 5.4-3. It can be seen that increasing ϕ can give us significantly higher wicking limits. Where higher ϕ can be employed we can either operate with higher values of SF_Q , or we can reduce the number of heat pipes.

Also shown on Fig. 5.4-3 are results for a conventional round heat pipe with a channel wick design such as sketched on Fig. 5.1-2b. The effective w_{II} was taken as 1.0 mm, giving a wall conductance ratio, $C \approx 0.1$. The resultant heat transport capability at $\phi = 20^\circ$ is only about 4 kW, compared to the 70 kW for the improved flat heat pipe designs. This clearly illustrates how important it is to use the flat heat pipe design in a strong magnetic field.

Finally, the wicking limit has been calculated for a range of Na vapor temperatures and plotted on Fig. 5.4-4. The wicking limit can be seen to increase rapidly with the heat pipe temperature.

5.5 Other Heat Pipe Operating Limits

In addition to the wicking limits, heat pipes can be limited by many other phenomena including:

- (1) Boiling limits.
- (2) Sonic limits.
- (3) Entrainment limits.
- (4) Flooding limits.
- (5) Startup limitations.

Fig. 5.4-3 Effect of the Inclination Angle on the Wick-Limited Heat Transport Capability of the three flat heat pipe designs and a conventional cylindrical heat pipe.

72-245-1

Fig. 5.4-4 Three Key Heat Transport Limits for the Flat Heat Pipe Design Design C3 with $L_E = 3$ $L_A = 1.5$ $L_C = 1.5$ m and $\phi = 20^\circ$.

5.5.1 Boiling Limits

We will use a conservative definition of the boiling limit which assumes that it occurs at the onset of nucleate boiling (ONB), even though our wick can probably tolerate some nucleate boiling with no ill effects. The ONB occurs first at the end of the evaporator (point x_0 on Fig. 5.2-1) where the local heat flux is estimated to be five times the average heat flux to the evaporator. Using estimates of the pipe wall superheat, ΔT_{SH} , required for ONB from Reference (12), the boiling limit for no magnetic field was estimated from

$$\dot{Q}_{ONB} = \frac{k_{Na} \Delta T_{SH} \times A_E}{10w_L}$$

where w_L is the half thickness of the sodium in the wick (taken as 1 mm) and A_E is the evaporator surface area. This limit is shown as the dashed curve on Fig. 5.4-4.

In the presence of a 2 Tesla magnetic field, it is estimated in Reference (1) that the superheat required for ONB is roughly doubled (based on experimental results from Reference (13)). The resulting boiling limit thus moves up by a factor of two as shown on Fig. 5.4-4. This boiling limit lies above the desired 70 kW heat transport capability for heat pipe operating temperatures less than about 1040 K. The boiling limit can easily be raised 25% to 50% by reducing the liquid sodium thickness near the end of the evaporator. This has been shown to reduce the wicking limit by only a few percent.⁽⁹⁾

5.5.2 The Sonic Limit

The sonic limit is defined when the vapor first reaches sonic velocity at the end of the adiabatic section (point 2 on Fig. 5.2-1). As the Mach number at this point 2, M_{V2} , approaches unity, the heat transport capability of the heat pipe reaches a maximum for fixed T_{VG} . Also, the temperature drop along the heat

pipe becomes large, and the heat pipe vapor can no longer be considered nearly isothermal. Consequently, we would normally prefer to operate with $M_{V2} \ll 1.0$. Note that we define M_{V2} as follows:

$$M_{V2} \equiv \frac{u_{V2}}{a_{V2}} = \frac{u_{V2}}{\sqrt{\gamma R T_{V2}}}$$

Since the sonic limit occurs when $M_{V2} = 1.0$, the sonic heat transport limit is given by:

$$\dot{Q}_S = \rho_{V2} \sqrt{\gamma R T_{V2}} A_V h_{fg}$$

For a specified vapor temperature at the beginning of the evaporator, T_{V0} , the temperature and density at the end of the evaporator were calculated for quasi-one-dimensional compressible flow with mass addition using the model of Reference (14). Then the conditions in the adiabatic section were calculated assuming Fanno line flow; i.e., quasi-one-dimensional frictional flow in a constant area duct (e.g. see Reference (15)).

These approximate models yield the sonic limit curve shown on Fig. 5.4-4 for constant-width heat pipe such as design A. This design is predicted to have a sonic-limited heat transport capability of about 56 kW at 1000 K. For our chosen design C3, the vapor flow area and taper of the adiabatic section can be optimized to raise the sonic limit even higher.

We note that for a heat pipe normally transporting 47 kW at about 990 K, the temperature would automatically rise to about 1040 K for the abnormal condition of one adjacent failed heat pipe. This is because the helium film drop and the heat pipe wall temperature drop at the condenser end will automatically increase when the heat transported rises from 47 kW to 70 kW.

5.5.3 Liquid Entrainment

The liquid entrainment by the vapor flow is usually limiting only

for open channels without a screen covering. However, in a gravity-assisted heat pipe, it is possible for liquid to pool at the bottom of the evaporator before startup even with no overfilling, since the wicking force will usually not be sufficient to keep the wick channel filled with liquid to the top of the heat pipe. When the heat pipe is started up, there will be a complex interaction between the high-velocity, low-density vapor and the liquid, some of which will probably return on the outside of the screen (i.e., on the vapor side) rather than inside of it. We may thus encounter some entrainment and possibly also choking during startup. Once the startup phase is over, the heat pipes should be free of the entrainment and flooding problems. However, these complex areas certainly warrant further study.

5.6 Temperature Considerations

The bulk temperature of the helium at the exit of the condenser heat exchanger, $T_{B(EX)}$, must be at least 875 K to meet the requirements of the thermochemical process for producing hydrogen (see Section 9). We have fixed $T_{B(EX)}$ at 875 K and then determined the temperature profile along a representative heat pipe near the exit of the condenser heat exchanger. Typical temperature profiles for both normal operation and with one failed heat pipe are shown in Fig. 5.6-1 for pure lithium in the liquid pool. When one heat pipe fails in the pure lithium case, its surface temperature will rise from about 1000 K to about 1130 K for our design point with 51 heat pipes/m².

If the pure lithium is replaced by $Li_{17}Pb_{83}$, the surface temperature of a failed heat pipe (and the nearby portion of the first wall structure as well) will rise to almost 1300 K because the thermal conductivity of $Li_{17}Pb_{83}$ is only about one-quarter that of pure lithium. In the presence of a strong magnetic field, we assume only pure conduction heat transfer from the liquid to the heat pipes, since free convection is suppressed.

72-244-1

Fig. 5.6-1 Temperatures for the Hottest Type-C Heat Pipes in the Lithium Pool With the condensers cooled by the exiting helium coolant at a bulk temperature of about 875 K.

The reference design with an average of 51 of the C3 heat pipes (153 of the C1 heat pipes) per square meter of first-wall area has been chosen as a compromise between many factors. If we attempt to reduce these maximum structural temperatures by increasing the number of heat pipes, the void fractions and the structural fractions increase rapidly. We cannot reduce the number of heat pipes (nor increase the neutron wall loading) without a further improvement in the heat transport capability of the flat heat pipe design above the present value of 5 kW/cm^2 in a 2 Tesla magnetic field.

5.7 The Array Effect - An Assembly of Heat Pipes

Due to neutronics requirements (i.e., minimized neutron streaming in the heat pipe vapor cavities) and heat pipe gravity-assisted requirements, heat pipes do not go out radially in the blanket (see Fig. 5.7-1 and Fig. 5.1-3). Thus, they not only see a variable heat flux due to radially variable neutronic heat generation, but also due to variable heat pipe spacing and orientation. The normal operating temperature of all the heat pipes has been selected to be about 1000 K. The temperature of the blanket fluid then varies with the variable heat flux and variations in heat pipe materials.

The radial variation of heat generation in the blanket is given approximately by

$$\dot{q}^{\text{III}} = \left(\dot{q}_{\text{max}}^{\text{III}} \right) \exp B \left(\frac{r - r_I}{r_0 - r_I} \right) \quad 5.7.1$$

where $\dot{q}_{\text{max}}^{\text{III}} \cong 8 \text{ w/cm}^3$ (value at the first wall) and $B \cong -4.1$. These parameters are defined from the neutronics analysis of the heat pipe blanket. The radii, r_0 and r_I , are 2.5 m and 1.5 m, respectively. The surface heat flux in the evaporator of the heat pipe is given by

$$\dot{q}^{\text{II}} = \dot{q}^{\text{III}} t_f \quad 5.7.2$$

where t_f is the half spacing between heat pipe sheets (Fig.

Fig. 5.7-1 Cross Section View of the TMR Central Cell
Shows the 88 sheet heat pipes per module (grouped in 4 bundles)
to remove the heat and tritium generated by neutrons in the
liquid lithium or lithium-lead.

5.7-2). For a straight heat pipe originating on the first wall θ degrees from the horizontal plane and inclined at ϕ degrees from the horizontal (Fig. 5.7-1 and Fig. 5.7-2), the radial coordinate, r , as a function of distance along the heat pipe, x , is given by

$$r^2 = r_I^2 + 2r_I x \cos (\theta - \phi) + x^2 \quad 5.7.3$$

where $\phi_{\max} = (90 + \theta)$ due to geometric considerations and $\phi < 60^\circ$ based on condenser grouping requirements.

To minimize neutron heating and damage of the magnets, we initially assumed that the actual heat pipes should be given enough curvature so that the line-of-sight length for a neutron traveling along the vapor channel was less than one-half the blanket thickness (Fig. 5.7-2). The thinness of the vapor channel (8 mm) allows for a large radius of curvature. Thus a straight line model of the heat pipe for use in calculating the heat load and corresponding temperature profile is acceptable. Substituting equations 5.7.1 and 5.7.3 into 5.7.2 gives the heat flux seen by the evaporator section of the heat pipe.

The maximum local fluid temperature in the pool near the first wall is given by

$$T_{\max, f}(x) = T_v + \dot{q}_{III}(x) t_f(x) \left[\frac{t_f(x)}{2k_f} + R_w(x) \right] \quad 5.7.4$$

where T_v = heat pipe evaporator vapor temperature

k_f = fluid thermal conductivity

R_w = thermal resistance of the vapor/liquid/wall sandwich.

(Figure 5.7-3 shows that the vapor temperature is strongly dependent on the condenser wall properties and helium conditions.) As discussed at the end of Section 5.6, the vapor temperature for optimum heat pipe performance is about 99C K for normal operation and about 1040 K maximum for the case of one failed heat pipe. To

- a. Maximum liquid lithium temperatures occur along the mid-plane between sheet heat pipes.

- b. Within the liquid lithium the heat pipe must be curved so that the line-of-sight length for a neutron in the vapor core is less than half of the blanket thickness.

72-247-1

Fig. 5.7-2 Details from Fig. 5.7-1 Showing Spacing and Curvature

72-248-1

Fig. 5.7-3 Maximum Blanket Temperature vs Helium Bulk Temperature
 The maximum temperature of the liquid blanket is a strong function of the He bulk temperature and wall thermal resistance. Without increased thermal resistance in the condenser wall, the heat pipe would be sonic limited to a heat transport capability less than required.

deliver this same vapor temperature to the evaporator for heat pipes subjected to vastly different bulk helium temperatures, the condenser wall thermal resistance (Al_2O_3 thickness) must be varied accordingly. If we did not impose this heat pipe vapor condition, many of the heat pipes would be sonic limited to less than the design heat load requirements. The maximum liquid lithium temperature profiles are shown in Fig. 5.7-3. The helium temperatures are based on the 4-pass heat exchanger discussed in the next section.

An abnormal local operating condition occurs when a heat pipe fails and its normal heat load must be carried by adjacent heat pipes. A simplistic mean conduction length model for redistributing the heat load shows that the highest incremental load to the adjacent heat pipes is approximately 38% of the normal thermal load. Thus, the heat pipes must be designed at least to 138% of normal operating conditions. To include additional capacity for other unknowns, a 1.5 safety factor was used. Calculating the temperatures using Equation 5.7.4 for fixed maximum vapor temperature at the normal and abnormal (design point) conditions results in the maximum fluid temperature profile shown in Fig. 5.7-3. The temperature profile of the heat pipe for these two conditions is given in Fig. 5.6-1. Note that for $\text{Li}_{17}\text{Pb}_{83}$ as a blanket material, the maximum liquid temperature at the first wall for the abnormal case is about 1300 K (shown by the open circle on Fig. 5.7-3.).

The heat pipes are divided into four bundles to make the most effective use of the He coolant (Section 5.8). Routing the heat pipes in four bundles (Fig. 5.7-1) leaves some open areas in the blanket. While the heat generation in these areas is much smaller than that near the first wall, the local tritium inventory in these open areas would be excessive unless some heat pipes were located there as tritium removal devices. Thus, some eight additional short heat pipe sheets were added to the 80 originating near the first wall.

5.8 Heat-Pipe Condenser Heat Exchanger

The helium coolant enters the module at 490 K and leaves at 875 K. To minimize the temperature gradient across the width of a sheet of heat pipes and to minimize the He film temperature drop to the heat pipe, we decided to have the He coolant make multiple passes through the module heat exchanger. Calculations were made to find the optimum number of passes. We considered a two-pass, a four-pass, and a six-pass heat exchanger. Fins were initially included to satisfy structural requirements (Section 5.11). The minimum sheet separation is set by these fins. The results of this analysis are shown in Fig. 5.8-1 in terms of ΔT_{film} , ΔT_{bulk} per pass and pumping power vs number of passes. The optimum condition, including structural requirements, was a four-pass heat exchanger with $\Delta T_{\text{film}} = 44$ K and ΔT_{bulk} per pass = 96 K. The pumping power is approximately 1% of the thermal power. A schematic of the four-pass heat exchanger is shown in Fig. 5.1-3.

The manifolding for the condenser heat exchanger provides an inlet on the highest bundle in a module near the centerline between modules (Fig. 5.7-1) and an inlet on the lowest bundle also near the intermodule centerline. Additional manifolding is provided for first wall cooling (discussed in last year's report) and tritium scavenging flow (He) from the first wall, vapor dome and heat pipe tritium window channel.

5.9 Structural Analysis

The structural loads for the blanket are split into four major types: liquid metal blanket container loads, heat pipe evaporator loads, He heat exchanger container loads, and heat pipe condenser loads. The liquid metal blanket container structural load is due primarily to the 1.5 atm He pressurizing gas load used to provide a positive pressure against the external-blanket environment and the gravity head of the liquid. Figure 5.9-1 shows the loading on the container walls and heat pipes for both a liquid lithium blanket

*Range on pumping power based on limits of He density for inlet and exit conditions (actual power depends on heat load distribution between bundles).

Fig. 5.8-1 Pumping Power and Film Temperature Drops as a Function of Heat Exchanger Design. The heat pipe/helium heat exchanger can be optimized for minimum film temperature drop and reasonable pumping power. The design condition of 4 passes gave a $\Delta T_{\text{film}} = 57$ K and pumping power = 1.2%.

Fig. 5.9-1 Effect of Blanket Breeding Material on Blanket Container and Heat Pipe Structural Loading. The choice of the blanket breeding material (Li or $\text{Li}_{17}\text{Pb}_{83}$) can significantly affect the structural loads on the blanket container and heat pipes. The larger density of $\text{Li}_{17}\text{Pb}_{83}$ increases the structural load up to a factor of 4.

and a liquid lithium-lead ($\text{Li}_{17}\text{Pb}_{83}$) blanket. As would be expected the $\text{Li}_{17}\text{Pb}_{83}$ pressure load is 4 to 5 times higher than the liquid lithium load.

Last year's first wall design was sized for structural support of the liquid Li-Na cauldron and thus would be acceptable for our liquid lithium design. An additional 1.4 atm to 4.4 atm pressure added to the first wall load for a $\text{Li}_{17}\text{Pb}_{83}$ blanket would require a much cooler first wall and/or more first wall material. Since the $\text{Li}_{17}\text{Pb}_{83}$ blanket is also more than 160 K hotter than the Li blanket for the abnormal design case, the first-wall design is even more difficult. The shield/reflectors, since they are thick, provide more than adequate support for either case. Intermodule walls bear against each other and thus can be designed for minimal material.

The wall of the evaporator end of the heat pipe is subject to a compressive pressure ranging from 1.5 atm to 1.8 atm for a liquid lithium blanket and 1.5 atm to 6.5 atm for a liquid $\text{Li}_{17}\text{Pb}_{83}$ blanket. Structural support of the heat pipe walls to keep the heat pipes from collapsing can be accomplished using fins attached to the outside surface. Design of the finned wall structure was accomplished using the simplified cross section and equations in Fig. 5.9-2. At the normal operating temperature (1100 K) the structural design limits are set by the materials' 1% creep over 100,000 hours. The niobium alloy (Nb - 10% W - 1% Zr - 0.1% C) at 1100 K would be used in the evaporator end of the heat pipe. It is relatively permeable to tritium (see Fig. 5.7-2 of Reference (1)). Its creep rupture stress limit at this temperature is ~ 200 MPa (29 kpsi). Since 1% creep data for 100,000 hours are not available, we used the creep rupture strength data with a safety factor of 2.5 giving a design strength of 82.6 MPa (12 kpsi). The material property data for this section come from References (16) and (17) as described in Section 5.13 of this report.

For a lithium blanket, an acceptable evaporator wall/fin design is shown in Fig. 5.9-3. For the 0.85-cm-tall fins located axially

$$\frac{d}{c} = \frac{2 \left[(b_1/b) + (1 - b_1/b)(t/d) \right]}{(b_1/b) + (1 - b_1/b)(t/d)^2} = \text{location of centroid}$$

$$\frac{I_c}{Ac^2} = \frac{1}{3} \left(\frac{d}{c} \right)^2 \left[\frac{(b_1/b) + (1 - b_1/b)(t/d)^3}{(b_1/b) + (1 - b_1/b)(t/d)} \right] = \text{moment of inertia about centroid, } c$$

$$A = b_1 d + (b - b_1)t$$

For fixed ends (FE); the maximum moment (at the fixed end) $M_{\max, FE} = -\frac{\Delta p b l^2}{12}$

the maximum stress (at the fixed end) $\sigma_{\max, FE} = \frac{M_{\max}}{I_c/c}$

the maximum deflection (at the center) $y_{\max, FE} = -\frac{\Delta p b l^4}{384 E I_c}$

For a simply supported structure (SS); $M_{\max, SS} = -\frac{3}{2} M_{\max, FE}$ at midpoint

$\sigma_{\max, SS} = -\frac{3}{2} \sigma_{\max, FE}$ at midpoint

$y_{\max, SS} = 5 y_{\max, FE}$ at midpoint

Fig. 5.9-2 Structural Analysis of Sheet Heat Pipes Using Reinforcing Fins
The reinforcing fins on the sheet heat pipes are structurally analyzed by assuming a "U" channel-type model supported on the ends under a uniform pressure load, p .

$\sigma_{\max} = 12 \text{ kpsi}$
 Max defl. = .15 mm

Heat pipe evaporator
 Fin design
 (Li blanket)

a. Schematic of the finned heat pipe evaporator section

$\sigma_{\max} = 11 \text{ kpsi}$
 Max. defl. = .03 mm

b. Schematic of the finned heat pipe condenser section

Fig. 5.9-3 Schematic of Finned Heat Pipe Evaporator and Condenser Sections
 The fins provide efficient structural support for the flat heat pipes as well as increased heat transfer capacity.

every 10 cm, the maximum stress, equal to 75.8 MPa (11 kpsi), occurs at "A" in Fig. 5.9-2. The maximum deflection occurs at "B" and is equal to 0.3 mm. For the $\text{Li}_{17}\text{Pb}_{83}$ blanket, the corresponding maximum stress equals 82.7 MPa (12 kpsi) and the maximum deflection is 0.15 mm for fins with a 1.4 cm height and wall and fin thickness set at twice that of the lithium blanket heat pipe evaporator.

The condenser end of the heat pipe heat exchanger sees helium at a maximum temperature of 875 K and a maximum wall temperature of about 1000 K. The helium is pressurized to 30 atm. The TZM alloy (Mo - 0.5% Ti - 0.08% Zr) at 1000 K can be used for the condenser end of the heat pipe. It is a high strength alloy and is relatively impermeable to tritium. Its 1% creep strength for 10,000 hours is ~ 270 MPa (39 kpsi)⁽¹⁷⁾. Because of the high operating pressure and the design requirements for guaranteeing isolation of the main He stream from the tritium in the heat pipe, the structural design criteria for the condenser are more stringent than for the evaporator. We believe that a safe design criterion is to set a safety factor of 2.5 on the 1% creep stress for 10,000 hours. This results in an allowable design stress of ~ 108 MPa (16 kpsi).

The container for the He heat exchanger is bounded on the inner side by a thick blanket/reflector and on the outer side by the wall interfacing with the tritium removal channel (Fig. 5.9-4). Using Incoloy-800H for a 5-cm-thick channel wall with support ribs, the stress can be kept low enough for good structural integrity.

As with the evaporator end, the heat pipe condenser end also needs integral fins for structural support. Figure 5.9-3 gives the final condenser/heat exchanger design. The maximum stress for fins 6-mm tall and spaced axially every 8 mm is 75.8 MPa (11 kpsi). This is below the design stress limit. The extra fins were added for improved heat transfer.

On the basis of these structural needs, the average void fractions and structural fractions for the liquid lithium blanket range from

Fig. 5.3-4 Heat Pipe Blanket/He Heat Exchanger Design
The design is very complex, requiring large brazing ovens and a modular buildup.

31% and 4.5%, respectively, near the first wall to 19% and 3.0%, respectively, near the reflector. For the $\text{Li}_{17}\text{Pb}_{83}$ blanket, the average void and structural fractions near the first wall are 36% and 9.5%, respectively.

5.10 Blanket Assembly/Disassembly

The overall module concept for the heat pipe blanket is the same as presented in last year's report (Section 5.1.7 and Figs. 5.1-5 through 5.1-8 of Reference (1)). This concept is shown in Fig. 5.1-1b of this section. Each blanket module is made of seven components: a) first wall, b) liquid blanket/shield, c) heat pipes, d) condenser heat exchanger cavity, f) manifolding, and g) tritium removal channels.

The first wall is made up of the He-cooled tube wall structure, FELTMETAL insulation and blanket inner membrane. The FELTMETAL is sealed between the tube wall and the blanket inner membrane. The condenser ends of the heat pipes are sealed from the liquid lithium region by welds at the shield. This assembly is then joined with the first wall. The heat exchanger baffles/piping are installed around the condenser end of the four heat pipe bundles (Fig. 5.9-4). The tritium window ends of the heat pipes are then sealed to the inner wall of the tritium removal channel.

5.11 Tritium Recovery

Tritium is generated in the liquid lithium of the main blanket volume. The tritium in the liquid lithium pool or liquid $\text{Li}_{17}\text{Pb}_{83}$ pool is recovered from the blanket mainly using the heat pipe evaporator walls as a permeable window as described in Section 5.7 of Reference (1). The tritium is swept through the heat pipes by the action of the sodium vapor and is removed through a permeable window at the end of the condenser into a He sweep gas stream (Fig. 5.9-4). The problems associated with tritium removal through heat pipes are discussed in Reference (4). Tritium is also removed at the vapor dome over the liquid lithium pool. The

lithium and lithium tritide vapors mixed with the tritium and He are condensed and the lithium returned to the liquid blanket pool. Tritium that permeates into the FELTMETAL passage of the first wall is also removed with a He sweep gas. The tritium is removed from the combined He sweep stream according to methods described in References (18) and (19) and He is reintroduced to the blanket.

5.12 Neutronic Analysis

In this section the computational methods used and the results of neutronics analyses performed for the heat pipe blanket are described. The neutronics results relevant to the two-temperature zone blanket design are given in Section 6.3.

5.12.1 Computational Methods

Two types of neutronics calculations are performed in this project. For those cases possessing geometry having sufficient azimuthal symmetry, one-dimensional neutron transport calculations offer the advantages of high accuracy, convenient usage, and relatively small computer running time requirements. When three-dimensional geometric features are important, it is possible to use multi-dimensional versions of the neutron transport codes, but most often Monte Carlo calculations are preferable.

The ONEDANT Code continues to be used for one-dimensional calculations in this project. This is the most recently developed version of the discrete ordinates neutron transport code ONETRAN,⁽²⁰⁾ and includes the synthetic diffusion approximation developed by Alcouffe.⁽²¹⁾ The calculations repeated here were performed with S_4 quadrature and used 30 neutron groups coupled with 12 gamma groups to facilitate neutron transport and gamma ray production and transport. The group constants are of order P_3 and are obtained from the MATXS library by the TRANSX Code.⁽²²⁾ A similar translation produces the neutron and gamma kerma factors utilized in the heating edits. Most of the calculations used data originating from ENDF IV files. Toward the end of the reporting

period data from ENDF V became available. Some calculations were repeated with the newer data with no significant changes in the important results. Subsequent calculations will be performed using the data from ENDF V. All neutronics calculations have been performed on the CDC 7600 computer at the Magnetic Fusion Energy Computer Center (MFECC) at Lawrence Livermore National Laboratory using the remote job entry terminal at the University of Washington.

Some aspects of the analysis of the heat pipe blanket required the use of Monte Carlo calculations. The code used is the MCNP Code.⁽²³⁾ MCNP is a general purpose Monte Carlo transport code for neutron and/or photon transport developed by Los Alamos National Laboratory. MCNP treats three-dimensional configurations of arbitrary materials in cells bounded by first and second degree surfaces. MCNP has its own specific libraries containing point-wise cross section data originating with ENDF IV. Thermal neutrons can be treated by both the free-gas and $S(\alpha,\beta)$ models. Photon interactions include incoherent and coherent scattering, photoelectric absorption with fluorescent emission, and pair production and annihilation. MCNP contains numerous variance reduction schemes with particle summary information to permit an analysis of variance reduction efforts. Standard tallies include particle current, flux, and heating. Any flux-dependent tally must be created by the user, and all tallies may be time and energy dependent if desired. For the shielding calculations repeated below, considerable effort in variance reduction was necessary. The methods used include source biasing and geometric splitting with Russian roulette.⁽²⁴⁾

5.12.2 Li₁₇Pb₈₃ Heat Pipe Blanket

A general description of the heat pipe blanket is given in Section 5.1. Two different representations of this design were used for neutronics calculations: (1) a one-dimensional representation of a Li₁₇Pb₈₃ blanket, (2) a one-dimensional representation of a Li metal blanket, and (3) a three-dimensional Li metal blanket.

The first model is given in Table 5.12-1. The objectives of this set of calculations were to determine the adequacy of the tritium breeding and to obtain a rough estimate of the severity of the shielding problems in protecting the insulation in the superconductor. The heat pipe "voids" and structure are homogenized in the one-dimensional representation. Since the cross sectional area of the heat pipe evaporators remains constant, their volume fractions crease with increasing radius in the blanket. The volume fraction of $\text{Li}_{17}\text{Pb}_{83}$, therefore, increases as shown in Table 5.12-1.

The blanket described in Table 5.12-1 is fairly similar to the WITAMIR design.⁽²⁵⁾ Both use $\text{Li}_{17}\text{Pb}_{83}$ with natural lithium for the breeding region, a $\text{Pb-H}_2\text{O}$ shield (at least in some cases), and generally similar sizes. They differ in first wall design, thickness of the breeder region (0.725 m vice 1.0 m), reflector type and thickness (steel/ H_2O -28 cm vice graphite-20 cm), shield thickness (60 cm vice 35 cm), and details of superconductor composition. A comparison of results for WITAMIR and the blanket described in Table 5.12-1 is given in Table 5.12-2.

The WITAMIR and heat pipe blanket results are consistent with the design differences. The overall breeding ratios are similar, but both are marginal. The former has less heating in the thinner first wall, more heating in the more dense Li-Pb (no heat pipes), much more heating in the reflector, and less heating in the shield (reflector acts as a shield). The most important difference is in the radiation dose to the superconductor which is much higher in the heat pipe blanket. This is due to the combined effects of the heat pipes in the Li-Pb and the thicker and more dense WITAMIR reflector-shield combination. The shielding effect of a graphite reflector is very small.⁽²⁶⁾ The combined thickness of the metal reflector and shield of the WITAMIR design is 88 cm as compared with a shield thickness of only 35 cm in the heat pipe design. Although the $\text{Li}_{17}\text{Pb}_{83}$ region is thicker in the heat

Table 5.12-1 Composition of One-Dimensional $\text{Li}_{17}\text{Pb}_{83}$ Heat Pipe Blanket

Outer radius (cm)	Description
150	Void (plasma radius = 135 cm)
150.2	Structure - V-10% Ti
151.8	First wall coolant - Na
152	Structure
152.5	First wall insulation - structure (60%)
152.7	Structure
172.7	$\text{Li}_{17}\text{Pb}_{83}$ (81.56%)
192.7	$\text{Li}_{17}\text{Pb}_{83}$ (83.58%)
212.7	$\text{Li}_{17}\text{Pb}_{83}$ (85.20%)
232.7	$\text{Li}_{17}\text{Pb}_{83}$ (86.53%)
252.7	$\text{Li}_{17}\text{Pb}_{83}$ (87.64%)
253.7	Stainless steel
288.7	Graphite
388.7	Void (He heat exchanger)
423.7	Shield - Pb (89%) + borated H_2O
433.7	Dewar - Al (67%)
435.7	S.C. insulation
485.7	S. C. coil - Cu

The first model is given in Table 5.12-1. The objectives of this set of calculations were to determine the adequacy of the tritium breeding and to obtain a rough estimate of the severity of the shielding problems in protecting the insulation in the superconductor. The heat pipe "voids" and structure are homogenized in the one-dimensional representation. Since the cross sectional area of the heat pipe evaporators remains constant, their volume fractions decrease with increasing radius in the blanket. The volume fraction of $\text{Li}_{17}\text{Pb}_{83}$, therefore, increases as shown in Table 5.12-1.

The blanket described in Table 5.12-1 is fairly similar to the WITAMIR design.⁽²⁵⁾ Both use $\text{Li}_{17}\text{Pb}_{83}$ with natural lithium for the breeding region, a $\text{Pb-H}_2\text{O}$ shield (at least in some cases), and generally similar sizes. They differ in first wall design, thickness of the breeder region (0.725 m vice 1.0 m), reflector type and thickness (steel/ H_2O -28 cm vice graphite-20 cm), shield thickness (60 cm vice 35 cm), and details of superconductor composition. A comparison of results for WITAMIR and the blanket described in Table 5.12-1 is given in Table 5.12-2.

The WITAMIR and heat pipe blanket results are consistent with the design differences. The overall breeding ratios are similar, but both are marginal. The former has less heating in the thinner first wall, more heating in the more dense Li-Pb (no heat pipes), much more heating in the reflector, and less heating in the shield (reflector acts as a shield). The most important difference is in the radiation dose to the superconductor which is much higher in the heat pipe blanket. This is due to the combined effects of the heat pipes in the Li-Pb and the thicker and more dense WITAMIR reflector-shield combination. The shielding effect of a graphite reflector is very small.⁽²⁶⁾ The combined thickness of the metal reflector and shield of the WITAMIR design is 88 cm as compared with a shield thickness of only 35 cm in the heat pipe design. Although the $\text{Li}_{17}\text{Pb}_{83}$ region is thicker in the heat

Table 5.12-1 Composition of One-Dimensional $\text{Li}_{17}\text{Pb}_{83}$ Heat Pipe Blanket

Outer radius (cm)	Description
150	Void (plasma radius = 135 cm)
150.2	Structure - V-10% Ti
151.8	First wall coolant - Na
152	Structure
152.5	First wall insulation - structure (60%)
152.7	Structure
172.7	$\text{Li}_{17}\text{Pb}_{83}$ (81.56%)
192.7	$\text{Li}_{17}\text{Pb}_{83}$ (83.58%)
212.7	$\text{Li}_{17}\text{Pb}_{83}$ (85.20%)
232.7	$\text{Li}_{17}\text{Pb}_{83}$ (86.53%)
252.7	$\text{Li}_{17}\text{Pb}_{83}$ (87.64%)
253.7	Stainless steel
288.7	Graphite
388.7	Void (He heat exchanger)
423.7	Shield - Pb (89%) + borated H_2O
433.7	Dewar - Al (67%)
435.7	S.C. insulation
485.7	S. C. coil - Cu

Table 5.12-2 Neutronics Results for $\text{Li}_{17}\text{Pb}_{83}$ Blankets

<u>Tritium Breeding</u>	<u>WITAMIR</u>	<u>This Study</u>
T_6	1.04	1.03
T_7	<u>0.02</u>	<u>0.02</u>
Total	1.06	1.05

Heating, MeV/sn. (%)

First wall	0.34 (2)	1.34 (7)
Li-Pb	17.07 (88)	15.17 (82)
Reflector	1.92 (10)	1.01 (5)
Shield	0.06 (<1)	0.85 (5)
Other	<u>-</u>	<u>0.09 (<1)</u>
Total	19.38	18.46

Radiation dose to

Superconductor insulation,

<u>Grays/yr.</u>	1.5×10^4	4.4×10^7
<u>(Rad/yr.)</u>	(1.5×10^6)	(4.4×10^9)

pipe design, the difference is compensated for by the void fraction. The ratio of about 3×10^3 in the radiation doses in the two cases is, therefore, not surprising and is, in fact, consistent with Fig. 1X-A-8 of Reference (25). These results illustrate fairly dramatically the benefits to be obtained in using a metal reflector as compared with graphite. The required thickness of the blanket/reflector/shield combination can be reduced by approximately the thickness of the reflector. Thus we would also adopt a WITAMIR-type shield/reflector design.

5.12.3 Li-Metal Heat Pipe Blanket

This design is similar to the description in Table 5.12-1 except for the following: the breeding matrix is Li metal rather than $\text{Li}_{17}\text{Pb}_{83}$; the reflector is stainless steel rather than graphite; the shield is composed of B_4C and stainless steel vice Pb-borated H_2O and is 0.8 m thick vice 0.35 cm.

The three-dimensional model used to represent the Li metal heat pipe design is shown in Fig. 5.12-1. This configuration is not an exact reproduction of the magnet configuration as shown in Fig. 5.1-1. However, it is conservative in that the superconducting insulation is "viewed" more directly by the neutron and gamma ray streaming through the heat pipes. The heat pipes in Fig. 5.12-1 are bent halfway through the blanket/shield thickness in order to reduce streaming. Earlier calculations have shown that such bending reduces the radiation dose to the superconductor by less than 10% and, therefore, is clearly not worth the added cost of construction and possible compromises in heat pipe performance. This is not necessarily the case for heat pipes of larger cross sectional dimensions where streaming is more pronounced.

Results of both one-dimensional (ONEDANT) and three-dimensional Monte Carlo (MCNP) calculations for the Li metal heat pipe design are given in Table 5.12-3. The results are within acceptable agreement limits. The tritium breeding ratio is somewhat higher

Table 5.12-3 Results of Monte Carlo Calculations for Li Metal Heat Pipe Blankets

	MCNP-I (Straight-tube)	MCNP-II (Bent-tube)
<u>Tritium Breeding</u>		
T_6	0.59	0.60
T_7	<u>0.62</u>	<u>0.62</u>
Total	1.21	1.22

Heating, MeV/sn. (%)

First wall	1.5 (8)	1.5 (8)
Li-Pb	12.1 (65)	12.1 (65)
Reflector	0.2 (1)	0.2 (1)
Shield	<u>4.7 (26)</u>	<u>4.7 (26)</u>
Total	18.5 (100)	18.5 (100)

Radiation dose to

Superconductor insulation

Grays/yr.	2.11×10^5	1.98×10^5
(Rad/yr.)	(2.11×10^7)	(1.98×10^7)

Fig. 5.12-1 Three-Dimensional Neutronic Model of the Heat Pipe Blanket
Used to determine the effect of neutron streaming in the
heat pipe vapor channel on the solenoid magnet insulation.

and the radiation dose to the superconductor insulation is somewhat lower in the MCNP results. These differences are due to the fact that the heat pipe spacing in the MCNP is not sufficiently close-packed to represent the actual design spacing. In the ONEDANT calculations the Li volume fraction is linearly increased from 0.69 to 0.81 from the first wall to the reflector consistent with the heat pipe design. The tritium breeding ratio is clearly more than adequate in any case, which is to be expected from a thick Li metal breeding region. The overall average power density in the Li region decreases exponentially from a value of 5.2 MW/m^3 adjacent to the first wall to a value of 0.57 MW/m^3 adjacent to the reflector (for a first wall loading of 2.0 MW/m^2).

Unfortunately, the impact of the most important neutronics results for this design, the radiation dose to the superconductor insulation, is not clear. The difference in the cases as calculated by the two methods is fairly large as is the statistical uncertainty in the Monte Carlo results, about 25% after 12,000 case histories. Based on these results and a criterion of 5×10^7 Grays as the limiting dose, the superconductor lifetime would be on the order of five years, with wall loading of 2 MW/m^2 an unacceptably short lifetime. However, relatively small increments of shielding could increase the lifetime to an acceptable value. Approximately 12 cm of metal, for example, which might be included in the construction of the superconductor could add a factor of about four, for a predicted lifetime of 30 years. Very detailed shielding calculations would be required for the final design of a configuration of this type.

5.13

Materials Sections - Heat Pipe Blanket

The main materials considerations for the heat pipe blanket are for the following blanket elements (see Figs. 5.1-3, 5.2-2, 5.7-1, and 5.9-4):

- Structural wall (includes first wall)
- Heat pipes

Materials selections are described for both of these elements along with the basis for making the selections. Many of these selections should be considered as tentative, since they are often based on estimated data due to the lack of data on materials compatibility and/or property information. Nonetheless, the selections serve our current purpose by allowing us to make an initial assessment of the viability of the heat pipe blanket concept.

5.13.1 Structural Wall Materials

The structural wall serves several functions: (1) it provides the first wall interface between the plasma region and the blanket, (2) it provides containment and structural support for the liquid metal pool, and (3) it provides the interface for penetration of heat pipes into the liquid metal pool.

The structure of the first wall is taken to be identical to that developed last year for the cauldron blanket,⁽¹⁾ i.e., structural strength is provided by a helium-cooled first wall and containment is provided by a membrane in contact with the liquid pool. A commercial product called FELTMETAL is used to insulate the cooled structural wall from the containment wall for the liquid metal. The structural wall is constructed of tubular passages 2 cm O.D. with 2-mm-thick walls, with the tubes formed together in a single layer to form the first wall. FELTMETAL is used because it has the right insulating properties for this application, and also because FELTMETAL has the characteristics of good compressive strength and low shear strength, thus permitting the containment membrane to move and orient itself relative to the structural wall during reactor startup and shutdown or during any temperature excursions. The FELTMETAL serves yet another function. It provides a sweep passage where a low flow rate of helium can be used to sweep out the accumulated tritium originating in the first wall region.

We retain V-10% Ti as our selection for the first wall material and provide enough helium cooling to obtain a temperature of ~ 700 K.

V-20% Ti is an equivalent alternative choice. V and Ti should undergo minimal neutron activation. In regard to radiation damage, there is evidence that both V-10% Ti and V-20% Ti suppress He gas bubble formation,⁽²⁷⁾ and that triton impact from the plasma side should not cause problems of localized tritium buildup in these materials because of the rapid permeation rates of tritium through V and its alloys.⁽²⁸⁾ From the standpoint of safety, these V alloys should hold up well for extended periods (months; in case there is a leak of liquid metal to the first wall from the liquid metal pool. The technology for producing tubing or sheet and of welding V is similar to that for Nb and Ta, and presents no problems as long as interstitial O, N, and H are kept at low levels. The current cost of V is high (~ \$90/kg which amounts to about \$10M for a TMR first wall), because the current demand for V is low and because V is recovered primarily as a byproduct. V resources are quite large however, and future demands should bring prices down. Additional details on selection of the first wall material are given in last year's report.⁽¹⁾

One of the main problems in the selection of the FELTMETAL material is to maintain adequate creep strength in the highest temperature region near the liquid metal containment wall. The FELTMETAL layer is taken to be 5-mm thick at a density in the range of 40-60% of theoretical. The Nb alloy, D-43 (Nb-9W-12Zr), is selected on the basis of good creep strength. Neutron activation of the D-43 is a concern. It may be possible to select suitable alloys with lower neutron activation if further work on this system is justified.

The selection of the alloy for the containment wall is D-43 at a thickness of 3 mm. This selection is made primarily on the basis of corrosion resistance and long-term creep strength. The thickness of 3 mm is more than is required for the first wall region, but the side walls and outer wall, which are not cooled, require this greater thickness. A more durable structure is obtained in the radiation environment by hot-forming the sides and

inner containment wall as an integral unit, thus avoiding any welds in the first wall region. Although D-43 is only a developmental alloy, it is felt that it could be commercially produced if needed. D-43 has desirable characteristics for production of sheet and plate, and good weldability.

In selecting containment wall materials, Fe-based alloys were eliminated on the basis of both corrosion and creep strength for a liquid $\text{Li}_{17}\text{Pb}_{83}$ blanket; while for a liquid Li blanket, it was felt that suitable alloys could be found that would be corrosion resistant, but creep strength would still present problems at temperatures of 1000-1100 K. If, in a future hydrogen plant design, lower blanket operating temperatures can be considered, Fe-based alloys should be reevaluated.

In considering the refractory metals, we expect Ti, Zr, V, Nb, Ta, Mo, and W all to be compatible with liquid Li; whereas only Nb, Ta, Mo, and W should be compatible with liquid $\text{Li}_{17}\text{Pb}_{83}$. Chemically, $\text{Li}_{17}\text{Pb}_{83}$ behaves similarly to pure Pb because it has a high chemical activity of Pb and a low chemical activity of Li. Thus $\text{Li}_{17}\text{Pb}_{83}$, like Pb, is expected to react to form intermetallic compounds with Ti, Zr, and V (and to dissolve unacceptably large amounts of these metals), while no reaction nor significant solubility is expected with Nb, Mo, and W (and also Ta although there is no reported information). The above conclusions are based on phase diagram data summarized in "Constitution of Binary Alloys." (29-31) If we now introduce the additional requirement for less than 1% creep in 100,000 h at ~ 1100 K, we conclude that only alloys of Nb, Ta, Mo, and W are acceptable with either liquid Li or liquid $\text{Li}_{17}\text{Pb}_{83}$. For example, D-43 is expected to undergo 1.7% creep in 100,000 h at 1135 K under 82.6 MPa (12,000 psi) tensile stress. (32)

5.13.2 Heat Pipe Materials

For purposes of discussion of heat pipe materials, we can consider a heat pipe to be made up of three regions: (1) an evaporator

region, (2) a condenser region, and (3) a tritium removal region. The evaporator region serves the dual function of accepting both heat and tritium from the liquid pool (see Table 5.3-1 and Figs. 5.1-3, 5.1-4, 5.2-2, 5.7-1, 5.9-3, and 5.9-4). The condenser region delivers heat to the He coolant while maintaining minimal tritium migration into the He. The tritium removal region utilizes a permeable metallic membrane to eject tritium from the heat pipe into a He sweep stream.

The main materials requirements for the heat pipe body in the evaporator region are:

- compatibility with the liquid Li at ~ 1130 K and/or liquid $\text{Li}_{17}\text{Pb}_{83}$ at ~ 1300 K
- compatibility with the liquid Na heat pipe fluid
- long-term (100,000 h) creep resistance at ~ 1100 K
- fabrication into an insulated sandwich structure
- good formability and weldability
- high permeation rate for tritium

Considering these very stringent requirements, we find ourselves limited very quickly to Nb-base alloys as the best choice, with Ta-base alloys as a possible backup. The Nb-base alloys have the distinct advantage of high tritium permeability, while the Ta-base alloys have advantages of higher strengths and shorter half-life neutron activation products. We have selected D-43 as the best candidate here. It should meet all of the materials requirements listed above. Compatibility with Li and $\text{Li}_{17}\text{Pb}_{83}$ is discussed in Section 5.13.1, as well as long-term creep and fabrication and welding issues. Compatibility with liquid Na is known to be good. Fabrication into an insulated sandwich structure, however, deserves comment (see Fig. 5.2-2). The coating of both Mo and W heat pipes with a 0.1-0.3 mm layer of CVD-SiC has been established.⁽³³⁻³⁶⁾ On the basis of this work, we anticipate that a D-43 heat pipe can also be vapor plated with CVD-SiC, but may require a thin

accommodation layer of Mo or W on the D-43 before coating. We further anticipate that it should be possible to develop a process to vapor deposit D-43 alloy on the outer surface of the SiC to attain the sandwich structure. We further expect to control the structure of the SiC so as to maintain sufficient porosity for tritium permeation.

Although D-43 meets the creep strength requirements at ~ 1100 K, in the advent of a heat pipe failure in the $\text{Li}_{17}\text{Pb}_{83}$ blanket, local temperatures would rise to ~ 1300 K. Under the design conditions of 82.6 MPa tensile stress on the D-43, 1% creep would occur in ~ 1 month at 1300 K.⁽³²⁾ Hence, the $\text{Li}_{17}\text{Pb}_{83}$ blanket module would need to be replaced within that period.

For the heat pipe condenser region (including the adiabatic heat pipe region), the materials requirements remain essentially the same as for the evaporator region, the main difference being that the tritium permeation should be as low as possible through the heat pipe walls so that the tritium can be transmitted to the far end of the heat pipe for removal. We select TZM (Mo - 0.5 Ti - 0.08 Zr - 0.03 C) for this application. The calculated tensile creep for TZM after 100,000 h at a stress level design point of 179 MPa (26,000 psi) is 0.04% at 1000 K and 1.23% at 1100 K.⁽³²⁾

Although the permeation rate of tritium through Mo is down about 4 orders of magnitude compared to Nb, it does not appear acceptable for this application. As a result, a layer of tungsten may have to be added as a tritium barrier.

For the tritium removal section, we select D-43 at a thickness of 1 mm as the tritium permeation membrane (see Fig. 5.9-4). The combined characteristics of high creep strength and high tritium permeability qualify D-43 as our best choice.

5.13.3 Summary of Materials Selections - Heat Pipe Blanket

The materials selections for the heat pipe blanket are summarized in Table 5.13-1. These selections are only initial selections that

have been made for the purpose of evaluating the heat pipe blanket concept. If it develops that further work on the heat pipe blanket is justified, then the materials issues need to be looked at in greater depth.

Table 5.13-1 Summary of Materials Selections for the Heat Pipe Blanket

Application	Selected Materials
First wall (700 K)	V-10 Ti tubing, 2 cm O.D., 2 mm wall
FELTMETAL insulator (700-1100 K)	D-43, 5 mm thick 40-60% of T.D.
Blanket Containment wall (~ 1100 K)	D-43, 3 mm thick
Heat pipe - evaporator section (~ 1100K)	Layer structure: 1.6 mm D-43 0.2 mm SiC, 0.4 mm D-43
Heat pipe - condenser section (~ 1000 K)	Layer structure; 1.5 mm TZM, 0.2 mm SiC, 0.4 mm TZM
Tritium permeation membrane (1000 K)	D-43, 1 mm thick

Two issues in particular would need further consideration: (1) neutron activation of materials, and (2) fabrications details for the heat pipe structures. Both D-43 and TZM show significant neutron activation and have long half-life activation products that present radioactive waste disposal problems. Ta- and W-base alloys present superior alternatives here. Half-lives of activated products are short for these elements. Strength is also in their favor, i.e., Ta alloys are much stronger than Nb alloys, and W alloys are much stronger than Mo alloys. The higher strength of these alloys may simplify the structure of the heat pipes. In any event, fabrication of the complex heat pipe structure is an important issue that would have to be resolved by experimentation.

Summary and Conclusions

The final parameters for the reference heat pipe design for one typical blanket module (about 118 modules are required for the entire TMR design for synfuel production) are summarized in Table 5.14-1.

We have done a thorough job of studying heat pipes in this fusion blanket environment and have considered the problems very critically. We have found that a relatively large number of heat pipes is required and that an individual heat pipe cannot be a simple tube with a wick fitted against its inner wall, but must be more complicated. Immersing the heat pipe in a conducting fluid complicates the problem because eddy currents can circulate into this fluid and thus increase MHD pressure drop in the heat pipe liquid. Thus, we have introduced a sandwich wall where the filler for the sandwich is a thin coating of silicon carbide. The filler provides the electrical insulation required to reduce MHD effects. It is possible to die form the structural outer member, add on the fins, and chemical-vapor deposit the SiC and the inside metal barrier. After the metal wicks are installed, the sandwich sheets must then be edge-welded together. These steps are established technologies.

The assembly and disassembly of an array of these heat pipes into a blanket module is not as straight forward as we had hoped. We have compiled Table 5.14-2 which reviews some additional design, installation and repair considerations requiring further investigation.

On the plus side, the flat heat pipe concept appears to offer a solution to the problem of operating liquid metal heat pipes in relatively strong magnetic fields. The heat pipe volume fractions and structural fractions near the first wall are lower than in most helium-cooled blankets. However, when we look at the overall result with objectivity, we are forced to conclude that in its present form the heat-pipe-cooled liquid pool blanket concept is

Table 5.14-1 Flat Heat-Pipe-Cooled Module Parameters

Neutron wall loading	2 MW/m ²
Central cell magnetic field strength	2 Tesla
Thermal power/module	22,600 kW
No. of sheet heat pipes/module	88
No. of C1 heat pipes/sheet	18
No. of C1 heat pipes/module	1584
Normal heat pipe operating temperature	~ 1000 K
Average no. of C1 heat pipes/m ² at the first wall	153
Average void fraction near the first wall for Li (Li-Pb)	31% (36%)
Average structural fraction near the first wall for Li (Li-Pb)	4.5% (9.5%)
Design-point heat transport/C3 heat pipe = 3 C1 heat pipes (with $SF_Q = 1.5$, $\phi = 20^\circ$)	70 kW
Design-point heat transport/C1 heat pipe (with $SF_Q = 1.5$, $\phi = 20^\circ$)	23.3 kW
Design-point axial heat flux based on condenser total cross sectional area (with $SF_Q = 1.5$, $\phi = 20^\circ$)	5.0 kW/cm ²
Helium coolant inlet bulk temperature	490 K
Helium coolant exit bulk temperature	875 K

Table 5.14-2 Some Heat Pipe Study Areas Requiring Further Investigation

A. Materials and Structure

1. Heat-pipe sandwich wall fabrication.
2. Joining Nb alloy sandwich to TZM sandwich.
3. Compatibility of Nb alloy (or TZM) with $\text{Li}_{17}\text{Pb}_{83}$.
4. Pressure loads on evaporator due to hydrostatic head of $\text{Li}_{17}\text{Pb}_{83}$: adequate creep strength of Nb alloy?
5. Helium pressure loads on condensers.
6. Welding or brazing of heat pipes to shield-wall and to tritium-removal-channel wall.
7. Assembly, disassembly and maintenance.

B. Neutronics and Tritium

1. Effect of neutron irradiation on SiC (or Al_2O_3) insulator, particularly the thermal and electrical conductivities.
2. Helium evolution in heat pipes due to neutron reactions.
3. Tritium removal rates through heat pipes and inventory in the blanket for Option #1 of Table 5.3-1.
4. Tritium inventory in the liquid pool for Option #2 of Table 5.3-1.
5. Contamination of the tritium permeation "windows."
6. Activation of the Nb alloy (or TZM).

C. Heat Pipe Performance Estimates

1. Startup, flooding and entrainment limitations.
2. Verification of the effective wall conductance ratio and the MHD pressure drop for flat heat pipes.
3. Effect of some nucleate boiling on heat pipe performance.
4. Possible boiling at the edges of the flat heat pipes.
5. Refined sonic limit estimates for tapered flat heat pipes.

D. Overall Concept

1. Methods for reduction of the number of heat pipes, the overall heat pipe volume and structural fractions.
2. Simplification of the design.

not as attractive as it first appeared to be. Nevertheless, we feel confident that the improved liquid metal heat pipe we have invented for use in a magnetic field will prove useful in some future fusion reactor for heat and tritium removal applications.

SECTION 5 NOMENCLATURE

A	=	area (m^2)
B	=	magnetic field, Tesla
b	=	effective tortuosity
C	=	wall conductance ratio
C_{eff}	=	effective wall conductance ratio
D	=	effective (hydraulic) diameter (m)
g	=	acceleration of gravity (m/s^2)
H	=	Hartmann number
h_{fg}	=	enthalpy of evaporation (J/kg)
k	=	thermal conductivity (W/m-K)
L	=	length (m)
n	=	index
p	=	pressure (Pa)
Q	=	heat flow (W)
R	=	gas constant (J/kg-K)
Re	=	Reynolds number
r	=	radius (m)
u	=	velocity (m/s)
w	=	half width (m)
W	=	full width (m)
γ	=	surface tension coefficient (or ratio of specific heats)
δ	=	wall thickness (m)
μ	=	dynamic viscosity (kg/m-s)
ϕ	=	angle of inclination from the horizontal

Subscripts

A	=	adiabatic section
B	=	magnetic field
C	=	heat pipe condenser
c	=	capillary

E = heat pipe evaporator
G = gravity
L = liquid
r = radial
V = vapor
w1 - inner wall
w2 = outer wall
∥ = parallel to B field
⊥ = perpendicular to B field

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SECTION 6

TWO-TEMPERATURE-ZONE BLANKET AND HEAT EXCHANGERS

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6.1 PROCESS AND DESIGN REQUIREMENTS

The blanket design must satisfy the requirements of the thermochemical process and geometric constraints of the Tandem Mirror Fusion Driver, tritium breeding, temperature limitations and a variety of factors that comprise a set of design criteria. This section considers the process requirements in summary detail as it affects the blanket design and presents a set of criteria to help evaluate candidate designs.

6.1.1 System Process Requirements

Section 4.3 discusses the system energy flow and temperature requirements as they would apply to a two-radial-temperature-zone blanket. There are two parallel streams from the blanket to the process. One is at high temperature and supplies energy to the SO_3 decomposer. The other is at low temperature and supplies energy to other parts of the process and to electrical energy production. The thermal and temperature requirements of the blanket are summarized as follows:

	<u>Direct Converter</u>	<u>Low-Temperature-Zone and First Wall</u>	<u>High-Temperature Zone</u>
Energy Required	276 MWt	1838 MWt	611 MWt
Fraction of Total	0.10	0.68	0.22
Blanket T_{in}	-	564 K	1060 K
Blanket T_{out}	-	850 K	1125 K
Working Fluid	-	He	He
Pressure	-	20 atm	7 atm

Helium is used as a working fluid because of its technology base in high-temperature process-heat applications. Liquid metals could also be used but were not selected because of potential safety problems. The pressure of 7 atm is used to pressure balance the SO_3 decomposition process. The 20 atm pressure is a compromise between the desire to have a low pressure system and low pressure drop.

6.1.2 Design Criteria

In addition to the system process requirements there are a number of other factors affecting design. Table 6.1-1 summarizes the important requirements and criteria.

The blanket design must accommodate the geometric constraints imposed by the fusion driver shown in Figure 6.1-1. The blanket is assumed to be modular with a 4 m length and a 1.5 m radius first wall. The wall loading is taken to be 2.0 MW/m^2 .

The lower temperature zone is used for tritium breeding and the tritium breeding ratio is to be at least 1.1. The tritium breeding and fraction of energy deposited in the high-temperature-zone must both be satisfied since these related requirements are critical to the success of the concept. The high-temperature-zone does not breed tritium.

The flow rates in the process components are fixed by the process requirements. The blanket design however must maintain temperatures of the flow and structure to acceptable levels consistent with the performance capability of the materials involved. The flow channel and piping design must also have sufficiently low pressure drop. A pressure drop of 0.04 of the absolute helium pressure is a typical limit for high temperature helium loops.

The heat exchangers provide a dual function of transporting energy and isolating the blanket from the process. A mean temperature difference of 20 K is taken to provide a reasonable and consistent basis for the concepts considered. Materials selection is closely tied to the temperatures of the blanket and the chemical compatibility of materials.

Reliability and safety of the design are very important. While it is difficult to define specifics at this time, the designs should have a low probability of failure. Recognizing the failures could occur, they should not lead to successive failures or a catastrophic failure. Failures of some components should also have a repair or operational fix that could allow continued plant operation. Small or local failures that lead to blanket removal would not be acceptable.

Table 6.1-1. Process and Design Requirements for Two-Temperature-Zone Blanket Module.

	<u>Low Temperature Zone</u>	<u>High Temperature Zone</u>
Process:		
Energy Fraction	0.76	0.24
Inlet Temperature (K)	564	1060
Outlet Temperature (K)	850	1125
Working Fluid	He	He
Pressure (bar)	7	20
Geometry:		
Length (m)		4
Inside Dia. (m)		1.5
Outside Dia. (m)		2.5
Field Coil		Figure 6.1-1
Wall Loading:		
Neutron Flux (MW/m^2)		2.0
Neutronics:		
Tritium Breeding Ratio	1.1	3
Fraction Energy Deposited		Satisfy Process Requirements
Thermal Hydraulics:		
Coolant Temperature		Uniform Exit, Minimal Peaking
Flow Rate		Satisfy Process Requirements
Max Temperature		Consistent with Material Performance
Pressure drop		1loop $\Delta P < 0.04 \text{ P}$
Heat Exchanger:		
Type		Isolation of Exchange Media
Mean ΔT		20 K

Figure 6.1-1, Blanket Geometric Constraints Based on Fusion Driver Dimensions.

6.2 SUMMARY DESCRIPTION OF DESIGN CONCEPTS

Several blanket designs are described here for the two-temperature-zone concept. Figure 6.2-1 through 6.2-4 illustrate four blanket concepts with two temperature zones. In all cases the low-temperature zone surrounds the first wall and the high-temperature zone surrounds the low-temperature zone. This arrangement appears to be the best for satisfying the requirements of tritium breeding and fraction of high-temperature-zone energy. Other arrangements are possible and are discussed in Section 6.3.

6.2.1 Low-Temperature Zone

Figure 6.2-1 through 6.2-3 illustrate three low-temperature-blanket-zone concepts. These concepts could also be used as alternates to the heat pipe concept in Section 5.0, by coupling a low-temperature blanket with a Joule-Boosted Decomposer.

Flowing Lithium-Oxide Microspheres

This concept illustrated in Figure 6.2-1 uses the gravity flow of Li_2O microspheres as heat generation and transport media. The spheres (400 μm dia.) flow slowly as a dense bed through the blanket flow channels. Each channel flow (0.01 to 0.04 m/sec) is controlled at the bottom in proportion to the radial power such that the average exit temperature of each zone is the same. The hot microspheres flow from the blanket to the shell side of an intermediate heat exchanger. The thermal energy is transferred to the helium working fluid on the tube side, and the helium flows to the thermochemical process. The cooled microspheres are returned mechanically to the top of the blanket by an Archimedes lift. This blanket concept is similar to our prior year concept with the important difference that it runs cooler and avoids the sintering problem encountered previously.⁽²⁾ The concept is also similar to a predecessor to this study,⁽¹⁾ to SOLASE,⁽³⁾ and to the flowing bed concept at Argonne National Laboratory.⁽⁴⁾

Flowing Lithium-Lead

This concept is illustrated in Figure 6.2-2 and is conceptually similar to the flowing Li_2O concept. In this case the Li-Pb (at the eutectic composition $\text{Li}_{17}\text{Pb}_{83}$) replaces the flowing Li_2O microspheres. The Li-Pb flows as liquid through banks of flow tubes. The flow in each tube is controlled in proportion to the radial power to provide a uniform average exit temperature. The energy is transferred to the helium working fluid of the thermochemical

Figure 6.2-1. Two-Temperature Zone Blanket with Flowing Li_2O Microspheres With the Low-Temperature-Zone.

Figure 6.2-2. Two-Temperature Zone Blanket With Flowing Li-Pb Low-Temperature Zone.

Figure 6.2-3. Two-Temperature Zone Blanket With Helium-Cooled Lithium-Lead Temperature Zone.

process by a shell and tube heat exchanger where the Li-Pb is on the shell side. The Li-Pb is returned to the blanket by a mechanical pump. While the Li-Pb has a high electrical conductivity, and thus the magnetic field retards the flow, the flow velocity for this application gives a tolerable pressure drop for the Li-Pb loop. This concept is similar to that proposed for the WITAMIR design.⁽⁵⁾ It has the advantage of being simple and provides more efficient heat exchange than Li_2O . It has the disadvantage of having temperature peaking in the Li-Pb occurring near structure surfaces and this could lead to unacceptable corrosion.

Helium Cooled Lithium-Lead

This concept is illustrated in Figure 6.2-3. It attempts to compact the design by moving the heat exchanger inside the blanket. The blanket consists of stagnant Li-Pb penetrated by cooling tubes containing helium. The tubes have variable spacing in the radial direction in an attempt to have the same heat removal by each tube. Each tube has the same flow rate and therefore the same exit temperature. The helium working fluid is heated directly by this process without an intermediate heat exchanger. The concept has the disadvantage of being intolerant of a tube failure. Such a failure could lead to damage of the Li-Pb pool container. Plugging a failed tube may not be an acceptable repair procedure.

6.2.2 High-Temperature Zone

Figure 6.2-4 illustrates a high temperature zone concept in conjunction with the flowing Li_2O low-temperature-zone blanket. The high temperature zone consists of banks of flow tubes. The outer tube wall provides the pressure boundary for the helium that flows internal to each tube. The tube wall is cooled by ambient flow of helium and is insulated from the hot interior of the tube by internal insulation. The tube wall is thin and the insulation of low density to reduce heat generation. This is to allow easy penetration of neutrons to the interior where they are absorbed and generate heat. The heat generating material is a dense neutron absorber and is a high temperature refractory material. The refractory rod bundle and coolant passages are sized in proportion to the radial power gradient to maintain uniform helium exit temperature.

Figure 6.2-4. Refractory Rod Bundle for High-Temperature Zone.

6.3 NEUTRONICS ANALYSIS

The neutronics analyses of two-temperature zone blankets differ significantly from those for the heat pipe blanket designs described in Section 5.10. The differences cover both the types of results of primary interest and the analytical methods used. In the heat pipe designs the most important issue is the protection of the superconducting toroidal field coils from radiation damage. Adequate treatment of this problem required the use of a three-dimensional Monte Carlo Code. Although radiation damage is at issue with any design, the primary concern with the two-temperature blanket design is the trade-off between tritium breeding and the fractional energy deposited in the high temperature (≥ 1100 K) zone. The neutronics calculations for these analyses have been performed with the ONEDANT Code. This is a one-dimensional, discrete-ordinates, neutron transport code developed at LLNL and is discussed in more detail in Section 5.10. The two-zone blankets are, in general, well described by a one-dimensional representation. The tritium breeding calculations are not subject to significant uncertainties from this assumption. The high temperature zones which involve pressure tubes do have three-dimensional features and more precise calculations of both heating and shielding results would require more accurate models. The results of the ONEDANT calculations are adequate, however, to reach the conclusions presented here.

Numerous efforts have been made to combine tritium breeding and high temperature power production in the same region of the blanket,⁽⁶⁻⁸⁾ This approach leads to fewer types of regions in the blanket and greater simplicity in the design of the cooling system(s). The difficulties associated with the single-zone designs arise from complications in using Li-bearing compounds at high temperatures.

A two-zone design seeks to reduce the difficulties of high temperature operation by performing tritium breeding at temperatures compatible with the

composition of the region containing the Li. A separate high temperature region is then added, the composition of which can be dictated solely by requirements for heat removal, radiation damage and structural integrity. Thus greater overall design complexity is accepted in return for flexibility in the design for high temperature operation.

The idea of separating tritium breeding and high temperature operation is not original to this study. Two-zone or three-zone blanket designs have been investigated at LANL, BNL, and ANL.⁽⁹⁻¹¹⁾ Both general and detailed configurations have been studied and the breeding region has been placed both inside and outside the high temperature zone. The percentage of energy deposited in the high temperature zone, while maintaining the constraint of breeding tritium, has been found to depend mostly on the concentration of Li, the isotopic percentage of ^6Li , and the amount of structure in the breeding zone.

In this study the objective has been to determine if the twin requirements of tritium breeding and a 24% fraction of high temperature energy delivered to the chemical process system could be met with designs that were credible from the standpoint of thermal-hydraulics. Another important result is the spatial distribution of the power density, which is an essential feed-back result for input to the thermal-hydraulics calculations.

6.3.1 Energy Splits and Tritium Breeding

Three different types of low-temperature zones (LTZ) and two types of high-temperature zones (HTZ) have been analyzed. The LTZ designs include (a) flowing lithium-oxide microspheres, (b) flowing lithium-lead, and (c) helium cooled lithium lead. These designs are described in Section 6.2.1 and in Figures 6.2-1 through 6.2-3. The HTZ designs include the pressure tube configuration described in Section 6.2.2 and in Figure 6.2-4 and a

homogeneous particulate configuration. The latter option has not been fully developed in this study. It is of interest, however, because such a configuration might be plausible in a design which incorporates the decomposer (the high-temperature element of the chemical process) into the blanket. Such a design would probably be a high risk/high benefit option. Safety issues and neutron activation of the process stream are negative risk issues. Significant improvements in system efficiency and reductions in capital cost are important potential benefits. It is also of interest to note the coupling effects of the LTZ and the HTZ, i.e., the impact of changing HTZ composition upon the performance of the LTZ.

The general layout of the two-zone blankets is illustrated in Fig. 6.3-1. The inner region of the blanket is the LTZ. A small optional thickness of graphite follows and the HTZ forms the outer blanket region. The thickness of each of these regions is variable, but the most important variable is the thickness of the LTZ. In all cases a separately cooled first wall complex was included. The first wall has an inner radius of 1.5 m and consists of the following components:

<u>Inner Radius (m)</u>	<u>Description</u>
1.500	Structure, Stainless Steel or V (90%), Ti (10%)
1.502	Coolant tubes w/Li metal coolant; Structure (24%), Coolant (54%), Void (22%)
1.508	Structure
1.510	LTZ

Flowing Lithium-Oxide Microspheres

The tritium breeding ratio, T, and the HTZ heating fraction are shown in Fig. 6.3-2 for designs in which the LTZ consists of Li_2O and the HTZ is

Fig. 6.3-1 Li₂O-particulate/Pressure Tube
Blanket Configurations.

composed of ceramic particulates. The Li_2O has a 60% packing fraction in these cases and is divided into baffled zones each 0.08 m thick and separated by structural baffles of thickness 0.002 m. The particulate in the HTZ has a 60% packing fraction, no interior structures, and 0.002 m of structure separating it from the LTZ.

The results shown in Fig. 6.3-2 indicate a slight coupling between the HTZ and LTZ with Al_2O_3 as the slightly preferred choice. If 1.1 is taken as the minimum acceptable value for T (allowing for various uncertainties and the known discrepancies in the ${}^7\text{Li}$ (n,n' α) T cross section), then the minimum Li_2O thicknesses are all approximately 0.26 m. The corresponding HTZ heating fractions are approximately 22%, 26% and 27% for C, SiC, and Al_2O_3 , respectively.

A similar set of comparisons is given in Fig. 6.3-3 for pressure tube-type HTZ designs (see Fig. 6.2-4). For those HTZ's including B_4C the composition is as follows:

<u>Material</u>	<u>Volume Fraction (%)</u>
Ta	30
SS	19
B_4C	6
VOID	45

For the designs not including B_4C the composition is:

<u>Material</u>	<u>Volume Fraction (%)</u>
Ta	36
SS	7
C (50% density-insulation)	12
VOID	45

Fig. 6.3-2 Tritium Breeding and HTZ Heating vs Li_2O Thickness for Particulate HTZ Designs.

The results shown in Fig. 6.3-3 indicate that the addition of B_4C to the HTZ is not necessary to enhance the power density in the heating elements of the HTZ and, in fact, its presence reduces the overall blanket performance significantly. The addition of C between the LTZ and HTZ has only a minor impact.

It does not appear to be possible to simultaneously achieve the twin objectives desired. The maximum HTZ heating fraction for T-1.1 is approximately 20%.

Flowing Lithium-Lead

The flowing lithium lead design for the LTZ is similar to the WITAMIR design. It is described in Section 6.2.1 and in Fig. 6.2-3. Tritium breeding and fractional heating results are given in Table 6.3-1 for a series of such designs. Both $LiPb$ and $Li_{17}Pb_{83}$ are included with 6Li concentrations varying from natural lithium (7.5%) to 60%.

Some of the conclusions that can be drawn from the results in Table 6.3-1 are as follows:

(a) Tritium breeding increases, i.e., thinner breeding regions are required, for $LiPb$ (as compared with $Li_{17}Pb_{83}$), and for increasing concentrations of 6Li .

(b) Overall performance in terms of breeding/heating fraction trade-off is significantly improved by increasing 6Li concentration but not by switching from $Li_{17}Pb_{83}$ to $LiPb$.

(c) There is a small penalty in specific energy production (MeV/source neutron) as the thickness of the breeding region increases. This result is a consequence of the fact that the average Q value is higher for reactions in the HTZ than for those in the LTZ.

Fig. 6.3-3 Tritium Breeding and HTZ Heating
vs Li_2O Thickness for Pressure
Tube HTZ Designs

Table 6.3-1 Tritium Breeding and Fractional Heating in Li-Pb Blankets.

<u>Composition</u>	<u>Breeding Region Thickness (cm)</u>	<u>T₆</u>	<u>T₇</u>	<u>T</u>	<u>Total Blkt. Heating (MeV/Neut.)</u>	<u>Frct.^c in HTZ (%) x .92</u>
LiPb (nat)	36	0.738	0.105	0.843	18.80	24.0
"	45	0.902	0.107	1.009	18.49	17.5
LiPb (20%)	36	1.043	0.092	1.135	18.35	18.0
"	45	1.199	0.094	1.293	18.08	12.1
LiPb (40%)	28	1.070	0.069	1.139	18.31	20.4
"	36	1.238	0.071	1.309	18.05	13.6
Li ₁₇ Pb ₈₃ (nat)	36	0.444	0.035	0.479	19.68	31.6
"	45	0.588	0.035	0.623	19.36	25.0
"	54	0.715	0.035	0.750	19.06	19.5
Li ₁₇ Pb ₈₃ (20%)	45	0.821	0.030	0.851	19.01	20.8
"	54	0.954	0.030	0.984	18.73	15.5
Li ₁₇ Pb ₈₃ (40%)	36	0.887	0.029	0.916	19.02	23.0
"	45	1.066	0.025	1.091	18.72	16.7
Li ₁₇ Pb ₈₃ (60%)	28	0.765	0.010	0.775	19.18	31.5
"	36	0.958	0.010	0.968	18.91	24.1
"	44	1.120	0.010	1.130	18.68	18.4
"	44 ^a	1.170	0.010	1.180	18.71	18.4
"	44 ^b	1.020	0.010	1.030	18.74	21.3

^aHe-cooled with uniform distribution of coolant channels.

^bHe-cooled with cooling ~ spatial distribution of power density.

^cThe fractions listed are those delivered at high temperature, i.e., 92% of the total energy deposited in the HTZ.

(d) The overall performance of the helium-cooled $\text{Li}_{17}\text{Pb}_{83}$ blankets is not greatly affected by the manner in which the helium voids are distributed.

Helium Cooled Lithium-Lead

The helium cooled lithium-lead design for the LTZ is described in Section 6.2.1 and in Fig. 6.2-3. From a neutronics standpoint this type is almost identical to the flowing lithium-lead type except for the addition of voids, i.e., He coolant. The coolant voids and their associated structure are not uniformly distributed, however, in an optimal design. The last two sets of results in Table 6.3-1 are for helium cooled configurations. First, the coolant is assumed to be uniform and the power distribution is computed. Then the coolant distribution is modified accordingly. The effect of placing additional structure near the first wall is to slightly reduce tritium breeding, but to increase the fractional heating in the HTZ. There is little change in overall blanket performance.

6.3.2 Radial Power Distribution

Energy disposition is computed with the ONEDANT Code by first computing the spatial and energy distributions of the neutron and gamma ray fluxes and then calculating reaction ratio using the appropriate neutron and gamma kerma factors. These later calculations are, in general, not as accurate as the former, and uncertainties of about 10% are typical. Results expressed in MeV/source neutron are invariant for a particular configuration. To convert to MW/m^3 requires specification of the wall loading. A wall loading of one MW/m^3 is typically assumed, and the results can then be scaled linearly with other assumptions.

The spatial distribution of the power density is shown in Fig. 6.3-4 for a Li_2O /pressure-tube blanket.

The configuration of the HTZ as shown in Fig. 6.2-4 cannot be accurately modeled in one-dimensional calculations, and is homogenized instead. It is necessary, however, to have more detailed information about the power distribution because only the energy deposited in the refractory rod bundle is recoverable at high temperatures. For this purpose constituent kerma edits are performed in order to calculate separately the energy disposition in each material in the HTZ. The results of these calculations show that 92% of the energy in the HTZ is deposited in the refractory rod bundle, approximately 8% in the pressure tube structure, and less than 1% in the insulation. The last column in Table 6.3-1 includes only the energy deposited in the refractory rod bundle.

In summary, none of the two-zone configurations analyzed can quite satisfy the simultaneous requirements for tritium breeding and high temperature energy production. For a tritium breeding ratio of 1.1, the maximum HTZ heating fraction is approximately 20%. This does not, however, totally defeat the idea of a two-zone design. It is possible to design the fusion reactor with the magnitude of the total power scaled to the high temperature requirements of the chemical plant. In this case, since the percentages do not quite match, the reactor will produce a surplus of low temperature energy. This energy may be used to produce electricity either for sale or to provide additional recirculating power.

Fig. 6.3-4 POWER DENSITY vs RADIUS, Li_2O - PRESSURE TUBE BLANKET
6-22A

6.4 BLANKET THERMAL-HYDRAULIC ANALYSIS

This discussion of the thermal-hydraulic analysis is divided into two parts: 1) low-temperature-zone, and 2) high-temperature-zone.

6.4.1 Low-Temperature-Zone

The low-temperature-zone blanket concepts presented in Section 6.2 are discussed here to define how thermal-hydraulic considerations affect their design.

Flowing Lithium-Oxide Microspheres

The thermal-hydraulic analysis of the flowing Li_2O microsphere concept has been presented previously for a higher temperature blanket⁽²⁾. This discussion presents an abbreviated and revised analysis for a lower temperature design that greatly reduces the possibility of sintering and the transport of Li_2O vapor.

Table 6.4-1 summarizes the average thermal-hydraulic results for the case HT4a2. The majority of the power (69.5%) is generated in the Li_2O region and imbedded structure. The high temperature zone generates 20.4%, the first wall 10.1% and the shield 1%. The total power is 90 MW which corresponds to a neutron multiplication of 1.20. The thickness of the Li_2O region is 0.36 m and is based on a neutronics compromise between tritium breeding and fraction of energy deposited in the high temperature zone. The inlet and outlet temperatures of the Li_2O and flow rate are defined by the requirements of the thermo-chemical process. The resulting average velocity of the Li_2O is 0.022 m/sec based on a solids fraction of 0.60.

Table 6.4-2 presents thermal-hydraulic results for selected Li_2O channels of a four channel blanket. The power for each channel is obtained from the power density and integral power results of Section 6.2. For the desired inlet and outlet temperatures, the channel flow is set in proportion to the channel power. The result is Li_2O velocity ranging from 0.031 m/sec for Channel 1 to 0.011 m/sec for Channel 4. The channel walls are sloped to the angle of sliding (55°) for free flowing spheres and a control orifice is placed at the bottom of each channel.

Table 6.4-2. Thermal-Hydraulic Summary for Selected Li₂O Channels.

	Channel 1	Channel 4
Power Fraction ^(a)	0.248	0.105
Power (MW)	22.32	9.45
Inner Radius (m)	1.51	1.79
Outer Radius (m)	1.59	1.88
Volume (m ³)	3.117	4.15
Power Density (MW/m ³)	7.16	2.28
T _{in} ^(b) (K)	584	584
T _{out} ^(b) (K)	870	870
Flow Rate (kg/sec)	27.0	11.4
Average Velocity (m/sec)	0.031	0.011
Temp Peaking Factor	1.34	1.18
Max T _{out} (K)	967	921
Max Temp Structure (K)	1083	950

(a) Neutronics calculation for case HT4a2.

(b) Based on 20 K mean temperature difference for heat exchanger.

The radial power variation through each channel also causes a nonuniform temperature distribution across the channel. From the power density shown and exit temperature distribution shown in Figure 6.4-1, the peak Li_2O temperature is about 967 K. This is below the sintering temperature estimate of 1200 K obtained from previous analytical estimates⁽²⁾ and as shown in Figure 6.4-1.

The division of the blanket into flow channels requires the use of the baffles that must operate at, or greater than, the temperature of the Li_2O . An estimate of the maximum baffle temperature was made for a radially oriented baffle. The baffles were assumed to have the thermal conductivity of stainless steel (18 W/m-K) and to be 2 mm thick. Heat transfer from the baffle to the Li_2O was calculated using a Nusselt number of 16 based on a University of Wisconsin experiment.⁽¹³⁾ A Nusselt number of 12 is typical for fully developed heat transfer between flat plates gives a heat transfer coefficient of $73 \text{ W/m}^2\text{-K}$ based on a particulate bed conductivity of 0.82 W/m-K and a hydraulic diameter of 0.18 m. The peak power density in the baffle is 17 MW/m^2 from the neutronics calculations. The resulting temperature rise is 116 K above the peak Li_2O temperature of 967 K and this gives a maximum structure temperature of 1083 K.

The material of construction for the Li_2O baffle structure is currently envisioned to be nickel rich alloy or nickel clad stainless steel. Section 6.5 has additional discussion of material considerations.

Conclusions regarding the thermal-hydraulic aspects of the flowing Li_2O concept are as follows:

1. Four channels are needed to keep temperature peaking factor along the channel in the range from 1.18 to 1.34.
2. The maximum Li_2O temperature of 967 K is about 230 K below the sintering temperature estimated by an analytical model.⁽²⁾
3. The maximum temperature of the baffle structure is estimated at 1083 K.

NECK GROWTH PREDICTIONS FOR SINTERING

Figure 6.4-1. Calculated Neck Growth Prediction for Sintering of Li_2O .

Heat Exchanger

A heat exchanger external to the blanket is required to transfer energy from the hot Li_2O microspheres to the helium working fluid. The concept selected for analysis is a vertical shell and tube heat exchanger where helium is on the tube side and the microspheres are on the shell side. The heat exchanger is essentially the same as that considered in the previous study.⁽²⁾ Other shell side concepts include gravity cross flow through a horizontal⁽²⁾ or inclined^(2,12) tube banks.

The heat exchanger considered here is sized to give a mean temperature difference of 20 K with process inlet and outlet temperatures of 564 and 850 K.

The heat exchange tube is fluted as shown in Figure 6.4-2 to provide enhanced heat transfer area to the flowing microspheres and to provide tube-to-tube support. The tube and flow channels are 0.01 m diameter with 0.001 m wall thickness.

A summary of results from a set of design calculations are given in Table 6.4-3. The shell side heat transfer uses a Nusselt number (hD/k) of 16 based on the University of Wisconsin studies⁽¹³⁾ of heat transfer from flowing particulates in a round tube. For an effective conductivity of 0.82 W/m-K, the shell side heat transfer coefficient is 1312 W/m²-K. The flow area is sized by selecting a bed velocity of 0.03 m/sec. This also defines the helium flow area. The resulting tube side heat transfer coefficient is 880 W/m²-K by using the Dittus-Boelter correlation⁽¹⁴⁾ for fully developed turbulent heat transfer. The tube wall heat transfer is quite high in comparison. The overall heat transfer coefficient is 631 W/m²-K. For a mean temperature difference of 20 K, this leads to a rather large heat exchanger (long and slender) with a vertical height of about 12 meters. This could be shortened by taking a larger mean temperature difference and associated higher blanket temperature. Reducing the particle velocity to give a larger overall diameter does offer much size reduction. Heat transfer to helium controls the size of the heat exchanger and helium heat exchangers tend to be quite long.⁽¹⁵⁾

Figure 6.4-2. Intermediate Li₂O to Helium Heat Exchanger.

Table 6.4-3. Li_2O to Helium Heat Exchanger Design Summary.

Overall Design

Type	Vertical Shell and Tube
Length	11.9 m
Diameter	2.5 m
Heat Transfer Area	4960 m^2
Heat Transfer Coefficient	630 $\text{W/m}^2\text{-K}$
Mean Temp Difference	20 K
Tube ID	0.0100 m (fluted)
Tube Patch	0.0191
Number of Tubes	13280

Shell Side

Working Fluid	Li_2O microspheres
Pressure	1 atm Helium Cover
Flow Rate	75.7 kg/sec
T_{in}	870 K
T_{out}	584 K
Heat Transfer Coeff.	1310 $\text{W/m}^2\text{-K}$

Tube Side

Working Fluid	Helium
Pressure	20 atm
Flow Rate	42.1 kg/sec
T_{in}	564 K
T_{out}	850 K
Heat Transfer Coeff.	880 $\text{W/m}^2\text{-K}$

Transport of Li_2O

The flow of Li_2O from the blanket and heat exchanger must be returned to the top of the blanket to complete the transport loop. This is a problem in the handling of bulk solids. While such technology is well developed for a variety of industries, there are some special considerations required for this application. The system must be entirely enclosed to contain tritium and the helium cover assumed to permeate the Li_2O and the blanket module system. It is also desirable to have a compact system.

Table 6.4-4 compares several of the options available to move bulk solids. The traditional bucket elevator or conveyor system provide low particle wear; however, they are mechanically complex systems that could require considerable maintenance. Containment of tritium in these systems would require complex design. A pneumatic system has been considered previously^(1,2) and is still judged to be unacceptable because of particle attrition and wear. Figure 6.4-3 shows an Archimedes lift. It operates on the principle of the particles flowing to the low point of the helix as it rotates. This results in a lifting of the particles up the incline. The Archimedes lift is a simple device that can easily contain tritium and has low particle attrition and low wear. Its primary drawback is its large size. The solids pump is an interesting device that has been used to "pump" rocks.⁽¹⁶⁾ The basic idea described in the presentation of Reference 16 is shown in Figure 6.4-4. Solids flow by gravity into the cylinder as the piston is withdrawn as shown in Figure 6.4-4a. The entire mechanism is then rotated and the piston pushes the solids up into the vertical pipe. The mechanism is rotated back to the original position to complete the cycle. The concept is simple; however, particle attrition could be a problem and it is not known over what vertical distance solids can be pushed.

At this time the Archimedes lift is judged to be the most viable option in spite of its large size. A set of design calculations were performed to estimate its size for a blanket module. A three stage design was selected and is summarized in Table 6.4-4a. The diameter is 1.6 m and the lift has 15% of its helix volume filled with Li_2O . A speed of 13 revolutions/minute gives the required flow of $0.063 \text{ m}^3/\text{sec}$. The centrifugal acceleration is quite low at 0.1 g. The angle of inclination is 45° .

Table 6.4-4. Comparison of Li_2O Microsphere
Return Option.

	Pro	Con
Bucket Elevator	Direct lift, Low Wear	Mechanically Complex Maintenance, Poor T Containment
Conveyor Belts	Low Wear	Mechanically Complex Maintenance, Poor T Containment, Large Size
Pneumatic	Simple, Enclosed	Erosion/Wear Particle Attrition
Archimedes Lift	Simple, Low Wear One Moving Part	Large Size
Solids Pump	Simple, Compact	Particle Attrition Operating Range

Table 6.4-4a. Design Summary for Archimedes Lift.

Stage	Radius		Vanes per Stage	Volume Flow per Revolution (m^3/rev)
	Inner(m)	Outer(m)		
1	0.2	0.4	4	0.035
2	0.4	0.6	6	0.089
3	0.6	0.8	8	<u>0.165</u>
				0.289

Figure 6.4-3. Archimedes Lift Tube. (18)

a) Suction Stroke

b) Discharge Stroke

Figure 6.4-4. Reciprocating Solid Particulate Pump ("Rock Pump").

Flowing Lithium-Lead

The use of liquid lithium-lead (Li-Pb) provides an alternate to the lithium-oxide microspheres. The primary advantage being that it is a liquid metal with corresponding ease of handling and high thermal conductivity. The primary thermal-hydraulic disadvantage is that it has a high electrical conductivity and a resultant magnetic pressure drop when flowing across magnetic field lines. Lithium-lead also has an upper temperature limit because of its corrosive behavior with materials of construction as discussed in Section 6.5. The lower temperature limit, important in start-up, is the melting temperature.

Figure 6.4-5 shows a phase diagram for lithium-lead. It has a eutectic of interest at an atom concentration of 17% lithium and 83% lead ($\text{Li}_{17}\text{Pb}_{83}$). The melting temperature is 508 K.

Table 6.4-5 summarizes the average thermal-hydraulic results for the blanket. The relative energy splits show the majority of the energy deposited in the Li-Pb region. The total power is 99.8 MW which corresponds to a neutron multiplication of 1.33. The thickness of the lithium lead region is 0.44 m and is based on a neutronics calculation compromise between tritium breeding and fraction of heat deposited in the high temperature zone.

The inlet and outlet temperatures and flow are based on the process heat requirements and a mean heat exchanger temperature difference of 20 K. The resulting average flow velocity is 0.053 m/sec. The blanket is assumed to consist of four rows of 0.13 m diameter nested flow tubes giving a total of 256 tubes per module.

Table 6.4-6 summarizes the results of the analysis. The channel flow rates are proportional to the radial power and result in velocities ranging from a maximum of 0.111 m/sec to a minimum of 0.014 m/sec. This would require a flow control orifice that also accounts for the magnetic component of pressure drop. The low velocity and rather large channel size minimizes the magnetic pressure drop. The pressure drop was calculated from the approximate relationship⁽⁵⁾

Figure 6.4-5 Phase Diagram for Lithium-Lead.

Table 6.4-5. Thermal-Hydraulic Summary for Flowing Lithium-Lead Blanket.

	First Wall	Li-Pb	High Temperature Zone
Power Fraction	0.092	0.713	0.195
Power (MW)	9.2	71.2	19.5
Inner Radius	1.500	1.510	1.950
Outer Radius	1.510	1.950	33.9
Volume	0.38	19.13	33.9
Power Density	24.3	3.72	0.58
Coolant	-	Li-Pb ^(a)	He
Pressure (atm)	-	(b)	7
T _{in} (K)	-	584	1060
T _{out} (K)	-	870	1125
Flow Rate (kg/sec)	-	1606	57.7
Flow Area (m ²)	-	3.14	-
Average Velocity (m/sec)	-	0.051	-

(a) Properties of Li-Pb: $\rho = 10700 \text{ kg/m}^3$, $C_p = 155 \text{ J/kg-K}$, $k = 15 \text{ W/m-K}$.

(b) 1 atm at top of blanket.

Table 6.4-6. Thermal-Hydraulic Results for Selected Tube Rows of the
Flowing Li-Pb Blanket.

	Tube Row 1	Tube Row 2	Tube Row 5
Power Fraction	0.304	0.114	0.035
Power (MW)	30.3	11.4	3.5
T _{in} (K)	584	584	584
T _{out} (K)	870	870	870
Flow Rate (kg/sec)	684	275	78.8
Average Velocity (m/sec)	0.11	0.04	0.012
Temp. Peaking Factor (a)	1.25	1.11	1.02
Max T _{out} (K)	942	901	876
Max T _{structure} (K)	942	901	876

$$\frac{dp}{dx} = - \frac{u B^2 \sigma_w t_w}{w_{II}}$$

where numerical values for the flow channels were taken to be: $B = 2$ Tesla, $\sigma_w = 10^6$ mho/m, $t_w = 0.003$ m, and $w_{II} = D/2 = 0.065$ m. For a 6 meter long channel and the maximum velocity (0.11 m/sec) tube, this is a pressure drop of 1.22 bar. The pressure drop through the inlet and outlet headers was calculated by using the same values except: $t_w = 0.004$ m, $w_{II} = 0.25$ m, and $u = 0.7$ m/sec. The header lengths were each taken at 1.0 m, giving a pressure drop of 0.90 bar for the headers. These pressure drops are reasonable for hydraulic circuits. The commonly held opinion of excessive magnetic pressure drop for liquid metals is not an issue for this design concept. The orificing of the channels to obtain the desired flow distribution requires consideration of magnetic field effects; however, this was not considered for this preliminary assessment. The hydrostatic head of lithium lead for a height of 5.1 meters is 5.1 bar. The maximum pressure is due to the hydrostatic head rather than the magnetic pressure drop.

The effect of the radial power distribution on the temperature peaking was estimated by a two-dimensional finite difference calculation of the temperature field. The flow was assumed to be uniform over the thickness of each tube row and the thermal conductivity was assumed to be isotropic and uniform. The results give a maximum temperature peaking of 1.25 for the first tube row. It is a minimum of 1.02 in the last tube row where the velocity is lowest. The peak-to-average power density in the first tube row is about 1.45 so the radial conduction provides significant flattening of the temperature profile. Temperature peaking could be reduced at the expense of more tube rows.

The maximum temperature (942 K) of the structure in contact with lithium-lead is very important because of the corrosive nature of liquid metals containing lead. Based upon the discussion in Section 6.5, the flow tubes would probably be built of a niobium alloy. Steel alloys do not have enough corrosion resistance at the temperatures required for this application.

Heat Exchanger

A heat exchanger external to the blanket is required to transfer energy from the hot Li-Pb to the helium working fluid. The heat exchanger is a shell and tube design with helium on the tube side and Li-Pb on the shell side. As for the Li_2O design, the heat exchanger is sized for a mean temperature difference of 20 K.

The 0.010 m ID tubes are close packed in a triangular array with a pitch-to-diameter ratio of 1.05. The wall thickness is 0.001 m. A summary of results from a set of design calculations is presented in Table 6.4-7. The heat transfer coefficients are calculated using standard correlations⁽¹⁴⁾ for gas and liquid metals. The overall heat transfer is about a factor of two higher than for the Li_2O heat exchanger because of the closer tube packing and this gives a factor of two size reduction in both overall length and diameter. The helium heat transfer is controlling.

Conclusions regarding the thermal-hydraulic aspects of the flowing Li-Pb concept are as follows:

1. The low velocity and relatively large tube diameter lead to acceptably low pressure drop in the presence of the magnetic field.
2. Temperature of the Li-Pb in contact with the tube walls is higher than desired to prevent corrosion of tube walls. Lower temperatures or corrosion resistant tube walls may be necessary for this concept to be feasible.
3. Flow control to obtain uniform channel average exit temperature would have to account for the effects of the magnetic field.
4. Startup requires preheating of the liquid Li-Pb to temperatures sufficiently high so that it does not freeze in the blanket tubes shown in Figure 6.2-2.
5. Non-programmed shutdowns with a consequent Li-Pb freezeup is a severe constraint on the applicability of flow Li-Pb in general.

Table 6.4-7. Li-Pb to Helium Heat Exchanger Design Summary.

Overall Design

Type	Shell and Tube
Length	5.3 m
Diameter	1.3 m
Heat Transfer Area	2640 m ²
Heat Transfer Coeff.	1200 W/m ² -K
Mean Temp Difference	20 K
Tube ID	0.0100 m
Tube OD	0.0120 m
Tube Pitch	0.0126 m
Number of Tubes	15900

Shell Side

Working Fluid	Li-Pb
Pressure	4-5 atm
Flow Rate	1694 kg/sec
Velocity	0.06 m/sec
T _{in}	870 K
T _{out}	584 K
Heat Transfer Coeff.	22430 W/m ² -K

Tube Side

Working Fluid	Helium
Pressure	20 atm
Flow Rate	47.9 kg/sec
Velocity	50 m/sec
T _{in}	564 K
T _{out}	850 K
Heat Transfer Coeff.	1350 W/m ² K

Helium-Cooled Lithium-Lead

The thermal-hydraulic issues of this concept are concerned with:

- 1) distribution of helium cooling tubes in a liquid pool of lithium-lead;
- 2) temperature at the tube wall;
- 3) maximum temperature of the lithium-lead;
- and, 4) pressure drop of helium flow.

As in the previous concepts, it is desired to have uniform exit temperature of the working fluid as it leaves the blanket. To achieve this with the helium cooled concept requires that the tube design acknowledge the nonuniform radial power density. The design objective is to have the same temperature at the exit of all tubes. This can be achieved by flow control and/or spacing of tubes with radius. The approach taken here is to use uniform tube power such that each tube would have the same thermal load. Figure 6.4-6 shows the integral power density divided into ten equal power increments. A tube passing through each increment removes one increment of power from the blanket. A uniform distribution of flow through all tubes, therefore, gives a uniform average coolant temperature from each tube. Since it is not quite possible to place tubes in one row to achieve this result, a compromise pattern such as that shown in Figure 6.4-7 would be needed for a practical design. It is necessary to stagger the tubes somewhat to have a more uniformly shared heat load. This choice requires 800 tubes per side for a total of 1600 tubes in the 4 m long module.

The tube diameter is selected to obtain a trade-off between pressure drop and heat transfer from the tube wall to the helium. The pressure drop is inversely proportional to the inside tube diameter to a power of 5. For fully developed turbulent flow, the heat transfer coefficient varies inversely with diameter to the power 1.8. Pressure drop therefore has the greatest sensitivity to diameter. For the pressure drop limited to 2% of the absolute pressure, or 0.4 bar, the required tube inside diameter is 0.017 m. This results in a heat transfer coefficient of $2096 \text{ W/m}^2\text{-K}$ and an average temperature rise to the tube wall of 37 K. Near the first wall the temperature rise is estimated at 141 K. The average temperature rise through the tube wall is estimated at 3 K for a wall thickness of 0.002 m and a thermal conductivity of 50 W/m-K which is typical of niobium.

6.4-6. Integral Power for Helium-Cooled Lithium-Lead Blanket.

Figure 6.4-7. Tube Pattern for Helium-Cooled Lithium-Lead Blanket.

The temperature rise in the Li-Pb is estimated by using a one-dimensional analysis where the region surrounding a tube is modelled as an internally cooled heat generating annulus. The outer radius of the annulus represents the distance to the adiabatic boundary between cooling tubes. The temperature rise is given by

$$\Delta T = \frac{q''' r_1^2}{4k} \left[1 - \left(\frac{r_2}{r_1} \right)^2 + \left(\frac{r_2}{r_1} \right)^2 \ln \left(\frac{r_2}{r_1} \right)^2 \right]$$

where r_1 is the tube outside radius and r_2 is the radius to the adiabatic boundary. For an average tube, $q''' = 3.7 \text{ MW/m}^3$, $k = 15 \text{ W/m-K}$, $r_1 = 0.005 \text{ m}$ and $r_2 = 0.025 \text{ m}$, the temperature rise is 49 K. Near the first wall for $q''' = 14.0 \text{ MW/m}^3$ and $r_2 = 0.015 \text{ m}$, the temperature rise is 141 K. The most limiting location is near the first wall. Taking the exit helium temperature as 850 K, the maximum structure temperature is 1002 K and the maximum Li-Pb temperature is 1028 K. The high Li-Pb temperature is not of practical concern because it is away from structural surfaces. The 1002 K at the tube wall next to the Li-Pb is of more concern because of material corrosion. Results for an average tube and the inner and outer tube rows are given in Table 6.4-9. The tube wall temperature could be reduced by increasing the heat transfer coefficient between the helium and tube wall. This would require an increase of velocity or reduction of tube diameter. To keep pressure drop within bounds the only acceptable choice would be a larger number of smaller diameter tubes. This is not necessarily a desirable solution because the added number of tubes could increase the probability of failure.

Heat Exchanger

No external heat exchanger is used for this design because of direct heat transfer to helium in the blanket.

Table 6.4-8. Thermal-Hydraulic Parameters for Helium-Cooled Li-Pb Blanket.

	First Wall	Li-Pb	High Temperature Zone
Power Fraction	0.091	0.713	0.195
Power ^(a) , (MW)	9.1	71.44	19.5
Inner Radius (m)	1.500	1.510	1.950
Outer Radius (m)	1.510	1.950	2.550
Volume (m ³)	0.37	19.1	33.92
Power Density (MW/m ³)	24.1	3.7	0.57
Coolant	-	He	He
Pressure (atm)	-	20	7
T _{in} (K)	-	564	1060
T _{out} (K)	-	850	1125
Flow Rate (kg/sec)	-	48.0	57.7
Flow Area ^(b)	-	0.36	-
Average Velocity (m/sec)	-	97.1	-

(a) M = 1.336, Total power is 100.2 MW.

(b) 1600, 0.0179 m ID tubes.

Table 6.4-9. Thermal-Hydraulic Results for Selected Tube Rows of the Helium-Cooled Lithium-Lead Blanket.

	Tube Row 1	Average	Tube Row 10
Tube OD (m)	.0105	.01051	.0105
Tube ID (m)	.0085	.0085	.0085
Eff. Adiabatic Radius (m)	.0150	.025	.0400
Power Density (MW/m^3)	14.0	3.7	1.2
Heat Flux (MW/m^2)	0.295	0.078 (a)	0.025
Heat Transfer Coefficient (W/mK)	2096	2096	2096
Temperature Rise to Wall (K)	141	37	12
Tube Wall Temperature Rise (K)	11	3	1
Max Tube Temp (K)	1002	890	863
Li-Pb Temp Rise (K)	26	49	69
Max Li-Pb Temp (K)	1028	939	932

(a) Based on a total heat transfer area of 913 m^2 .

Conclusions regarding the thermal-hydraulic aspects of the helium-cooled lithium-lead blanket are as follows:

1. The temperature rise from the helium to the tube wall is the most limiting heat transfer process. It leads to tube wall temperatures in excess of 1000 K near the first wall. This is a severe problem because of the corrosive nature of liquid lithium-lead (See Section 6.6)
2. Tube reliability must be very high for this concept because the design is not very tolerant of tube failure. A catastrophic failure is of concern because it could over-pressure the blanket. A small leak might be tolerable but would lead to decreased cooling for a tube and higher tube wall temperatures.
3. The design has the advantage of eliminating an external heat exchanger at the expense of blanket simplicity and reliability.
4. A strong advantage is that pre-heated helium could be used to liquify the in-situ Li-Pb for startup.

6.4.2 High-Temperature Zone

Figure 6.2-3 shows the conceptual layout of a high-temperature-zone surrounding the low-temperature-zone. A key component of the high-temperature-zone is a flow tube that contains helium at 7 atm and a heat generating material. This hot core is isolated from the pressure tube wall by carbon fiber insulation. The outer surface of the tube is cooled by ambient helium with a maximum temperature of 900 K. The intent of the design is to have a thin wall and low density insulation to reduce heat generation. The interior, however, is to have high neutron and gamma capture and high heat generation.

The heat generating interior could have several forms. An example considered here is illustrated in Figure 6.2-4 where a refractory rod bundle is used. The rods would be positioned relative to the radial power distribution to achieve uniform helium exit temperature. The thermal-hydraulic issues for this concept are concerned with: 1) selection of rod size to obtain a desired pressure drop; 2) flow distribution through the bundle; 3) temperature distributions; and 4) heat loss.

The design of the high-temperature-zone must really be done together with the design of the low-temperature inner zone because of the neutronic coupling between them. For purposes of this discussion the high-temperature-zone is taken to be of one design and corresponds in thickness and power to the flowing lithium-lead design. The overall design parameters are given in Table 6.4.10. The neutronics calculations define the power to be 19.5 MW and the nominal thickness of the high-temperature-zone to be 0.60 m. This thickness is accommodated by 4 rows of tubes (triangular packed) 0.160 m outside diameter. This results in 100 tubes per side for a total of 200 tubes per module. The tube wall is taken to be 0.003 m and the insulation thickness is 0.010 m. The choice of rod diameter affects the coolant velocity and pressure drop. With diameter being the independent parameter, the pressure drop was selected to be 0.14 bar which is 2% of the absolute pressure (7 bar). The resulting rod diameter is 0.0361 m for a friction factor of 0.02 and a channel length of 6.5 m. This rod diameter gives a nominal pitch-to-diameter ratio of 1.17.

The above results are for an average channel. They could be optimized by tube row and for radial rod positioning to account for the radial power gradient. This was not considered necessary at this preliminary stage of analysis.

The heat transfer from the rods to the helium was estimated by assuming fully developed turbulent flow. For the average conditions above, the Reynolds number is 22,000. The Nusselt number is calculated to be 60 with a resulting heat transfer coefficient of 660 W/m-K. The average heat flux is 17,400 W/m² giving a temperature rise to the rod surface of 26 K.

For a power density of 1.9 MW/m³ (high to account for power peaking) and a thermal conductivity of 20 W/m-K the temperature rise in a heat generating rod is 8 K. This low value is due to the relatively high thermal conductivity taken for the refractory material. An order of magnitude decrease to 2 W/m-K giving 80 K would not be a problem.

The heat loss through the tube wall is an important issue because it decreases the relative fraction of energy available from the high-temperature-zone. The heat loss is presently controlled by an insulated tube wall that must satisfy several requirements. From a thermal point-of-view the insulation should have low thermal conductivity and be thick. Neutronically, the insulation should be thin, have low density and have low heat generation. The insulation must also have structural strength. An estimate of insulation performance can be made by using a simple heat conduction model where the insulation is taken to be 1 cm thick with average inside and outside temperatures of 1100 K and 900 K. The heat flux and heat loss for a range of thermal conductivities is as follows:

k (W/mK)	q" (W/m ²)	q loss (% Module Power)
0.03	600	.86
0.06	1200	1.71
0.10	2000	2.84
0.30	6000	8.52
0.60	12000	17.05
1.00	20000	28.42

The above table indicates that the thermal conductivity must be less than about 0.06 W/mK to keep the heat loss to less than 1.7% of the total power. This is about 9% of the high-temperature-zone power when it is realized that it generates about 20% of the total power. Good insulators have values of thermal conductivity in the range from 0.04 to 0.06, so if one can be found to operate at the desired temperature, the heat loss could be manageable. Carbon fiber was assumed to be the insulation for the neutronics calculation. Some experimental thermal conductivities for carbon fiber insulation have been measured at LLNL⁽¹⁷⁾ at various densities and temperatures. For low density carbon insulation ($\rho = 0.157$ g/cc) in a vacuum typical values as a function of temperature are:

473 K	0.026 W/m-K
673 K	0.038 W/m-K
873 K	0.059 W/m-K

These values are in the range desired to maintain a moderate heat loss based on the previous conduction estimate. The presence of 1 atm nitrogen increases the above values by a factor of three or four. An evacuated insulation would therefore be necessary to maintain control of the heat loss. The tube wall could also be insulated by using thermal shields to reduce heat transport. As an example, the heat flux between two plane walls can be defined by⁽³⁾

$$q'' = \frac{\sigma (T_1^4 - T_2^4)}{(1/\epsilon_1 + 1/\epsilon_2 - 1)}$$

where $\sigma = 5.669 \times 10^{-8}$ W/m²-K⁴. Typical heat flux values are as follows:

E_1	E_2	q'' W/m ²
.05	.05	1174
.10	.05	1570
.10	.10	2411

Comparison with the thermal conduction values indicates emissivities on the order of 0.05 could produce acceptably low heat loss. This would require low emissivity surfaces (highly polished) and they would have to remain in that condition at high temperature.

Heat loss could also be reduced by reducing the surface area; however, that would require a larger flow tube and associated higher wall stresses to contain the helium at 7 atm.

Conclusions regarding the thermal-hydraulic analysis of this concept are as follows:

1. It is possible to devise a fuel form that can yield acceptable pressure drop. A rod bundle is one of several possible forms.
2. The heat generation results in modest temperature rise of the refractory above the coolant temperature. Maximum temperature for an average rod is 1160 K.
3. The heat loss from the tubes is an important issue. High performance insulations are necessary to retain a high fraction of thermal energy delivered at 1125 K. Carbon fiber insulations are in the range of being feasible.

Table 6.4-10. Thermal-Hydraulics Parameters for Helium-Cooled High-Temperature-Zone.

Power (MW)	18
Flow (kg/sec)	53.3
Pressure (atm)	7
Velocity (m/sec)	124
T _{in} (K)	1060
T _{out} (K)	1125
Number of Tubes	200
Tube Rows	4
Tube OD (m)	0.160
Insulation	Carbon Fiber
Insulation Thickness (m)	0.01
Heat Generation Material	Rod Bundle (Tantalum)
Rod Diameter (m)	0.036
Heat Transfer Coeff. (W/m^2-K)	660
Avg. Rod Temperature Rise (K)	33
Heat Loss ($k = .06 W/m-K$)	9%

6.5 MATERIALS CONSIDERATIONS

Materials selections for the Two-Zone Blanket concepts are needed mainly in the following areas:

- First wall
- Flow channels for low temperature zones
- Helium piping in the high temperature zone
- Heat exchanger - blanket outlet

The most critical materials problems are for the flow channel materials, and therefore this section will deal primarily with that area.

6.5.1 First Wall Materials

For both the flowing Li_2O microsphere and flowing Li-Pb blanket concepts, the first wall is isolated from contact with the blanket material (except in an accident situation), and the selection of first wall materials is therefore not subject to long-term corrosion constraints. For these two concepts, we select a V-10% Ti wall material on the plasma side, a beta-Ti alloy on the blanket side and a beta-Ti Feltmetal insulator in between the two, using the same design approach as for the Cauldron Blanket described last year.⁽²⁾ For the helium-cooled Li-Pb blanket concept, we retain the V-10% Ti material for the plasma side, but select D-43 niobium alloy for the Li-Pb liquid pool side and D-43 Feltmetal insulation in the wall. D-43 is required here for both corrosion resistance and long term creep strength as discussed for the Heat Pipe Blanket in Section 5.14.1.

6.5.2 Flow Channel Materials

For the flowing Li_2O microsphere concept, the maximum temperature in the flow channel materials is expected to be 1083 K. From the compatibility studies of metals in contact with Li_2O by Kurasawa et al.,⁽¹⁹⁾ we find that pure Ni is completely compatible with Li_2O under these conditions, and that Hastelloy-XR also shows fairly good compatibility. Ni-rich alloys (e.g., Inconels) generally show moderately good corrosion resistance, but cannot be considered for long term use without additional protection. Thus, we suggest that a Ni cladding can be applied to Inconel-617 or Inconel-625 to acquire the needed corrosion protection for long term use. Extrapolated creep strengths for 1% creep in 100,000 h at 1100 K for these two alloys are 14 MPa and 41 MPa, respectively.⁽²⁾ Hastelloy-XR (an improved form of Hastelloy X) should

provide adequate corrosion protection without a clad. Extrapolating the observed corrosion of 2 μm in 100 h ⁽¹⁹⁾ to 20 y, assuming a parabolic ($t^{1/2}$) rate law, we obtain a corrosion depth of 84 μm , which would be quite acceptable. The extrapolated creep strength for 1% creep in 100,000 h at 1100 K is 29 MPa. ⁽²⁰⁾ Thus, we conclude that although data on long term properties are not available, extrapolations would indicate that Ni-clad Inconel-617, Ni-clad Inconel-625, and Hastelloy-XR are acceptable alloys for the Li_2O microsphere channels.

The requirements for channel materials for the flowing Li-Pb concept and helium-cooled Li-Pb concept are similar, with the former having a maximum material temperature of 942 K, and the latter, 1002 K. These requirements are also similar to those for the Heat Pipe Blanket where corrosion by liquid $\text{Li}_{17}\text{Pb}_{83}$ is the main concern. As with the Heat Pipe Blanket, we select the niobium alloy, D-43, because of its good corrosion and long term creep resistance.

6.5.3 Helium Piping - High Temperature Zone

The design of the high pressure piping for helium in the high temperature zone utilizes a liner of carbon fiber insulation to permit a thinner and lower temperature pipe wall to be used (see Section 6.4.2). Thus, the tube wall attains a maximum temperature of ~ 900 K. The presence of a carbonaceous environment however presents problems for most Fe-Cr-Ni alloys, even at 900 K. According to the work of Perkins and Goldberg, ⁽²¹⁾ Aluminum is required as an alloy additive to reduce carbon attack. They found Incoloy-811 to be the most resistant of the alloys tested, and we therefore select it for the pressurized helium wall material.

6.5.4 Heat Exchanger - Blanket Outlet

Materials problems for the heat exchanger tubes from the outlets of the flowing Li_2O microsphere and flowing Li-Pb blanket regions are very similar to the respective flow channel problems. Maximum material temperatures are reduced to ~ 870 K, but stresses are higher because a 20 atm helium pressure is required on the tube side to carry heat from blanket to process. These are essentially compensating effects. We therefore select the same materials as given above for the flow channels. The helium-cooled Li-Pb blanket uses the outlet helium directly in the thermochemical process, and therefore does not utilize a separate heat exchanger.

6.6 TWO-TEMPERATURE-ZONE BLANKET CONCLUSIONS

The previous discussion has brought the design features, capabilities and issues regarding the two-temperature-zone concept. This section draws some comparisons and conclusions for the design concepts.

High-Temperature-Zone

1. The fraction of blanket energy available from the high-temperature-zone strongly depends on the neutronic performance of the blanket. There is a direct trade-off between tritium breeding and the fraction of energy deposited in the high-temperature-zone. While the current design configurations produce sufficient tritium breeding, they are marginal regarding the fraction of energy deposited in the high-temperature-zone. The process requires about 22% delivered to the process and the current design provides 24% generated in the high-temperature-zone.
2. The fraction of energy deposited in the high-temperature-zone is further degraded by that fraction deposited in the insulation and tube and the addition heat loss through the tube wall. Alternate design concepts need to be found if the high-temperature-zone is to meet process requirements.

Low-Temperature-Zone

1. While the low-temperature-zone is considerably cooler than the high temperature zone it is still a "hot" blanket that presents significant design challenges to maintain sufficiently low temperatures for good materials performance. The low-temperature-zone could also be operated as a stand-alone blanket (without a high-temperature-zone) and be used as the heat source for the Joule boosted thermo-chemical hydrogen production process.
2. Table 6.6-11 presents a performance summary and comparison of the low-temperature-zone concept:

a) Flowing Li_2O

The flowing Li_2O blanket has been considered previously ^(1,2) and the issue of sintering the microspheres has been removed as a problem because of the lower temperature in this application. The concept is simple and operates at low pressure. Its drawbacks are concerned

with: 1) particle attrition and wear; 2) large size of the intermediate heat exchanger (IHX); 3) large size of the particulate return system. It would have quite high operating reliability and would not suffer severe consequences from a IHX tube failure.

b) Flowing Li-Pb

This system is attractive because of its simplicity, compactness and relatively small size of the IHX. The magnetic pressure drop is moderate and does not appear to be a problem. The fundamental issue is concerned with the corrosion of the structure by Li-Pb at the upper temperature. The corrosive properties are well known at lower temperatures but are unknown for the candidate nickel alloys at these temperatures. Tube failures in the blanket would be of concern because of the consequences of a spill and it would also take the module out of service and have to be replaced. Heat exchanger tube failure would be a problem but could be repaired by tube plugging. Startup and shutdown is a serious drawback because of the freezing of Li-Pb.

c) Helium-Cooled Li-Pb

This concept is attractive because of its compactness and the elimination of the IHX. The heat transfer coefficient to helium is rather low and this leads to high tube wall temperatures. The temperatures are higher than for the flowing Li-Pb concept with the attendant higher concern for corrosion. The design is also intolerant to tube failure and could lead to overpressurization of the Li-Pb pool. Tube failure would require module replacement. Structural stresses due to unprogrammed reactor shutdowns and consequent freezing of the Li-Pb is a severe problem.

Based on this comparison the flowing Li_2O seems to offer the best alternative of these three concepts.

Table 6.6-1. Comparison of Low-Temperature-Zone Concepts.

Item	Low-Temperature Blanket Concept		
	<u>Flowing Li₂O</u>	<u>Flowing Li-Pb</u>	<u>Helium Cooled Li-Pb</u>
Neutronics:			
Breeding mat.	Li ₂ O	Li ₁₇ Pb ₈₃	Li ₁₇ Pb ₈₃
Trit. Breed. Ratio	1.041	1.13	1.17
LTZ/HTZ Heat Split	79.9/20.1	80.0/20.0	80.0/20.0
LTZ/HTZ Thick.(m)	0.36/0.90	0.44/0.60	0.44/0.60
LTZ/HTZ Out. Rad.(m)	1.87/2.78	1.95/2.55	1.95/2.55
Channel Thermal-Hyd:			
Channel Design	4 Rows of Rectangular Channels (0.090 x 0.400m)	4 Rows of Circular Tubes (0.13 m dia)	10 Rows of Circular Tubes (0.017 m dia)
Flowing Mat.	Li ₂ O microspheres (400 m dia)	Liquid Li-Pb	Helium
Avg. Flow Vel.(m/sec)	0.022	0.051	97.1
Pressure (atm)	-	5 (max)	20
Pres. Drop (atm)	-	2.12	0.4
Flow Exit Temp. max/avg (K)	967/870	942/870	850/850
Struct. Temp. max(K)	1083	942	1002
Intermed. Heat Exch:			
Type	Shell (Li ₂ O) & Tube (He)	Shell (Li-Pb) & Tube (He)	Penetrating Tubes (He) inside Blanket (Li-Pb)
Mean T (K)	20	20	-
Heat Trans. Coef. (W/m ² -K)	630	1200	-
Length/dia	11.9/2.5	5.3/1.3	-
Tube Side Pres. (atm)	20	20	-
Shell Side Temp. Tin/Tout (K)	870/584	870/584	850/584
Tube Side Temp. Tin/Tout (K)	564/850	564/850	-
Transport System:			
Primary	Li ₂ O Archimedes Lift	Li-Pb Pump	Stagnant Li-Pb
Secondary	He Circulator	He Circulator	He Circulator

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7.0

ENERGY TRANSPORT SYSTEM - REACTOR TO PROCESS

The two reactor blanket designs discussed in Sections 5 and 6 provide energy for the General Atomic Sulfur-Iodine Thermochemical Cycle in two different ways and in two different temperature ranges. The first, the Heat Pipe Blanket discussed in Section 5, produces 875 K process heat for the bulk (78%) of the thermochemical plant needs, while the SO_3 Decomposer, which is the high temperature demand part of the cycle, is driven electrically using the Joule-Boosted SO_3 Decomposer. The second, the Two-Radial-Zone Blanket described in Section 6, provides both high temperature thermal energy (1125 K) for the top end of the plant and 850 K process heat for the remainder of the plant.

This section discusses the options and alternatives available for transporting this energy to the Chemical Plant. System evaluations addressing both the problems of matching heat source to heat sink and designing for safe energy transport have been studied. The transport system selected for both blanket designs uses helium as the heat transport fluid.

7.1

Objectives and Safety Constraints

Both blanket concepts, the Heat Pipe blanket and the Two-Zone Blanket, must deliver heat at the appropriate amounts and temperatures to the Chemical Plant in order to achieve an overall efficient and economic process. The majority of the chemical engineering effort in this project has been devoted to developing techniques of matching the heat source to the heat demands of the process and then developing process unit designs that can effectively and economically make use of this heat profile.

Safety considerations have been an integral part of the chemical engineering. Attention to potential problems in liquid metal safety, helium coolant over-pressuring of process vessels, tritium permeation and contamination, and migration of neutron-activated corrosion products are some of these safety considerations.

7.2

Engineering Options

A very large number of engineering approaches for matching the heat source to the heat demands of the chemical process were examined in order to identify those which met the following desired characteristics:

- The majority of piping for long distance heat transport within the chemical plant shall use high quality steam.
- Liquid metal coolants shall not be used within the chemical plant.
- Piping distances of high pressure helium piping shall be minimized to reduce pumping power.
- Multiple barrier isolation shall be provided between the neutron-activated blanket materials and the hydrogen product.
- Highly redundant modularity shall be used whenever possible to increase plant availability and reduce the quantity of neutron-activated materials that could be accidentally released.

7.3

Case Studies

Space does not permit us to outline in detail the large number of case studies that were made in order to select and design a blanket process interface and then evaluate its performance. Some of these cases have been covered in earlier papers⁽¹⁻³⁾ and last year's report.⁽⁴⁾ Both He and Na were considered for heat transport in the process last year, and were coupled with steam-Rankine cycles for electrical power generation. Only He was used this year for heat transport out of the blanket, and various cycles were evaluated for electrical power generation, i.e., steam-Rankine, steam-Freon, steam-ammonia, and Brayton-steam.

The TMR Q was parametrically varied from 10 to 35. In the resultant cases, there were blanket temperatures or process

conditions that needed to be varied. The following approaches were investigated:

- Heat Pipe Blanket (He) 800 - 1150 K
- Heat Pipe Blanket (Na) 800 - 1150 K
- Two-Zone Blanket:
 - low temperature (He) 800 - 950 K
 - high temperature (He) 900 - 1150 K
- First wall (Na or He) 700 - 850 K
- Direct Converter Hx (He) 850 - 1050 K

System evaluations were made for these approaches by attempting to match the heat source to the process heat demand as follows: by developing a heat exchanger network train, by developing electric power generating systems on both topping and bottoming cycles, and by roughly optimizing the network and then evaluating the thermal efficiency. For cases involving helium-Brayton turbine cycles, each of these helium loops had to be optimized with a small computer program by varying temperatures, He flow rates and turbine wheel compression ratios. The loops were linked and the network system optimized factorily. This tedious process was required to produce the best set of temperatures, helium flows, and heat exchanger match-ups consistent with the highest efficiency. Thermal efficiency ranged from 32 to 58% for the thermochemical process depending mainly upon the particular selection of topping and bottoming cycles. As expected, an increase in Q from 10 to 35 also gave an increase in process efficiency (up to an incremental gain of 14%) because of the increased availability of electricity at a high conversion efficiency from the Direct Converter.

7.4 Process Steam Interface

In nearly all of the case studies, the need to produce process steam within the chemical plant created a serious debit in thermal efficiency and costs. The only case (which we will discuss in more

detail later) that produced high quality process steam in an acceptable manner was a helium-Brayton topping cycle generating electricity and bottoming into a 600 K, 5.8 MPa steam-Rankine cycle. This gave the added flexibility of absorbing assorted waste heat streams and generating both process steam and a sizable amount of shaft work for process pumps.

7.5 Cycle Efficiency and Plant Availability

We explored the variation of cycle efficiency with changes in the energy transport system and coupling of the blanket to the process. Generally, we found high cycle efficiency was obtained when liquid sodium was used to generate steam and drive process units. But we felt that liquid metal coolants also provided a serious fire and safety risk which would result in a reduction in total plant availability. The damage consequences from accidents could be severe.

High pressure helium was found to have the disadvantages of poor heat transfer (and thus large temperature gradients within the heat transfer equipment), significant pumping power, and the high pressure hazards of over-pressuring and bursting process unit vessels in an accident. Helium coupling generally produced lower thermal efficiencies, but we believe higher plant availability than with liquid metal heat transport. We conclude that the safety and availability advantages of helium make it the preferred heat transport fluid.

7.6 Helium Piping Optimization Studies

In carrying out the case studies, it became clear that although helium had clear safety and operational advantages, there were also clear economic debits in terms of pumping power and high pressure equipment capital costs. Therefore, a tradeoff study was done between capital costs and operational pumping power costs. The details of this rough optimization study were reported earlier.⁽⁴⁾ For heavy wall Incoloy-800H piping, using a perforated liner to

help keep the outer wall cooler, the minimum total operating and amortized capital cost was found for a 2 m diameter, 100 m long, 1.0 MPa pressure, 7 pipe transfer system feeding a helium-heated decomposer or boiler using blanket heat. The minimum cost was around \$35M - still somewhat high.

Since this earlier study, we developed a design using Kao-Wool insulation⁽⁵⁾ in place of the stagnant helium gap between the perforated liner and the heavy outer wall. This more effective insulation allowed us to drop the outer wall temperature to 755 K so that mild steel would be acceptable (i.e., 1% creep in ~20 yrs) and allow the helium pressures to be increased to 3.0 MPa. This improvement dropped the total operating and amortized capital cost to about \$10M -- a substantial improvement.

7.7

Conclusions on Energy Transport

In this section we have outlined the range of transport system alternatives that were examined in developing a near-optimum design concept that employs a helium-Brayton/steam-Rankine combined cycle approach. This satisfies our desire to achieve good radiation isolation of the Chemical Plant from radioactivity by using helium coolants, and to substitute for liquid sodium the more conventional process steam approach to driving the Chemical Plant.

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8.0 THERMOCHEMICAL HYDROGEN PROCESSES*

In this section we describe the basic principals of thermochemical cycles for water splitting to produce hydrogen, including the criteria that determine cycle efficiencies and influence process design. We also describe in a very introductory way the GA Sulfur-Iodine Cycle for hydrogen production that we are coupling to the TRM in our current design studies.

8.1 Basic Principles of Thermochemical Cycles

8.1.1 Introduction

A thermochemical cycle for hydrogen production is a process in which water is used as a feedstock along with a non-fossil high temperature heat source to produce H_2 and O_2 as product gases. (Fossil fuels can be converted directly to hydrogen by chemical processing in a more energy efficient and cost effective manner than by using them as a heat source for thermochemical cycles.) The water splitting process is accomplished through a closed loop sequence of chemical reaction steps in which the chemical reagents are continuously recycled and reused in the process with essentially no loss of material. Practical thermochemical cycles, as currently envisioned, require input temperatures of ~ 1200 K, for the highest temperature chemical step, and operate at a thermal efficiency of $\sim 50\%$. Here, the thermal efficiency is defined as the higher heating value of the H_2 produced, $286 \text{ kJ/mol } H_2$ (combustion enthalpy of the H_2 to give liquid water at 298.15 K), divided by the thermal heat per mole of H_2 delivered by the dedicated heat source.

Currently, there are some 30 thermochemical cycles world-wide that are under various stages of investigation and development. Energy sources that are being considered for these cycles are: (1) high-temperature, gas-cooled reactors providing a maximum temperature of ~ 1200 K, (2) solar central receivers providing a maximum

* This section is taken mainly from last year's report.⁽¹⁾

temperature somewhere in the 1200-2000 K range, and (3) magnetic fusion reactors anticipated to provide a maximum temperature in the 1200-2000 K range. Selection of an actual maximum operating temperature depends upon the process needs of the thermochemical cycle as well as on overall system design. Cycle efficiency, system economics, and materials needs are all important considerations. Electrical energy for process equipment is required in addition to high temperature heat for operation of thermochemical hydrogen plants.

Of the cycles under study, only three have thus far been developed to the stage where closed-loop, table-top or bench-scale models have been built and tested in the laboratory; these are the cycles that we are considering for magnetic fusion applications. They are illustrated in terms of their principal chemical steps and reaction temperatures in Figure 8.1-1. Note that in each case the sum of the chemical reaction steps is $H_2O = H_2 + 1/2 O_2$. The main efforts on the development of the Sulfur-Iodine Cycle are underway at the General Atomic Company,⁽²⁻⁴⁾ on the Sulfur Cycle at the Westinghouse Electric Corporation,^(5,6) and on the Sulfur-Bromine Cycle at the Joint Research Centre-Ispra Establishment.⁽⁷⁻⁹⁾

8.1.2 Basic Principles

The energetics of water decomposition are as follows:

Thus, the energy requirement for decomposing water to the elements is quite high; it can be further shown that if water were to be decomposed in a single step, temperatures of about 2500-3000 K would be required to obtain significant yields. Furthermore, there are a number of major problems associated with decomposing water in a single step, even if such high temperatures could be attained,

Figure 8.1-1 Thermochemical cycles whose chemistry and closed loop operation have been verified in the laboratory.

SULFUR-IODINE CYCLE

SULFUR CYCLE (PART ELECTROCHEMICAL)

SULFUR-BROMINE CYCLE (PART ELECTROCHEMICAL)

i.e., back reaction of H_2 and O_2 during cooldown, serious materials problems, and the need for considerable recycle because of low decomposition yields.

If the decomposition of water is carried out in two or more chemical reaction steps, a number of advantages result. For example, the production of H_2 and O_2 can be separated into different reaction steps, thus avoiding the back reaction problem. Also, by picking a proper combination of reactions, the maximum reaction temperature can be lowered with resultant benefits in reaction yields and availability of suitable materials. In actual practice, it is found that two-step cycles have shown only minimal lowering of the temperature compared to the single-step decomposition of water, unless one of the steps involves an electrochemical assist (e.g., see the Sulfur Cycle in Figure 8.1-1). A cycle that includes an electrochemical step is called a "hybrid" cycle. With three or more steps, cycles based only on thermal input for the chemical reactions become possible at reasonable temperatures (e.g., see the Sulfur-Iodine Cycle in Figure 8.1-1), and many will work efficiently at maximum temperatures of about 1200 K.

In order to illustrate some of the important considerations in developing and evaluating thermochemical cycles, we can use a generalized two-step cycle represented as follows:

In reaction (1), a reducing agent R is used to reduce water to produce H_2 at temperature T_1 , forming the oxide RO in the process. Reactions such as this are usually carried out near room temperature or at slightly elevated temperatures. In order to achieve a near zero or negative ΔG° to make the reaction go, the

enthalpy change is usually governing and ΔH_1 is negative (an exothermic reaction). Heat produced by reaction (1) is usually of limited value for reuse in the cycle because of the low temperatures involved. Reaction (2) is carried out at a high temperature, and is the decomposition step that regenerates the reducing agent R and O_2 . Reaction (2) is highly endothermic (ΔH_2 is positive) and requires a large change in entropy (ΔS°) to produce a near zero or negative ΔG° . This can be readily seen from the well-known second law expression $\Delta G^\circ = \Delta H^\circ - T\Delta S^\circ$ where the $T\Delta S^\circ$ term becomes increasingly important as the temperature is raised. Hence, one of the requirements in selecting RO is that it have as large a ΔS° of decomposition as possible, to minimize the maximum temperature required in the thermochemical cycle. As an example, the gaseous molecule SO_3 ($R = SO_2$) meets the requirement for reaction (2), but SO_2 falls short of the requirement for reducing water to produce H_2 in reaction (1). The fix for this problem is to use an assist for reaction (1) by either (a) forming a hydrogen compound of intermediate stability, such as HI or HBr, which can be subsequently decomposed to obtain the desired H_2 product, or (b) using an electrolysis assist at a voltage substantially less than for direct water electrolysis (e.g., the Sulfur Cycle in Figure 8.1-1). In the case of HBr decomposition, an electrolysis assist is also required (see Sulfur-Bromine Cycle in Figure 8.1-1), since the yield is low for thermal decomposition of HBr.

Two types of cycle efficiencies are used in evaluating thermochemical cycles. The work efficiency, i.e., the efficiency of converting heat into work, $E(w)$, is given by

$$E(w) = \frac{237.2}{\sum \Delta H_D} \leq \frac{T_D - 298}{T_D} \quad (4)$$

The thermal efficiency, or the efficiency of heat utilization, $E(H)$, is given by

$$E(H) = \frac{285.8}{\Sigma \Delta H_D} \leq 1.21 \frac{T_D - 298}{T_D} . \quad (5)$$

In expression (4), the number 237.2 (in kJ/mol) represents the maximum work (ΔG°) available from the hydrogen produced, (see expression (1), while $\Sigma \Delta H_D$ represents the total external heat requirements for the cycle, i.e., the heat provided by the non-fossil fuel heat source. For electricity required in the cycle, the $\Sigma \Delta H_D$ term includes the equivalent heat required to produce the electricity; or as in the case of the Tandem Mirror Reactor as an energy source, some of the energy is available as electrical energy from the Direct Converter, and we add this directly to the $\Sigma \Delta H_D$ term assuming it to be equivalent to thermal energy at 100% conversion. We see that $E(w)$ is limited by a Carnot expression for the maximum efficiency. The thermal efficiency, $E(H)$, compares the combustion energy of the hydrogen produced to form liquid water at room temperature as the final product, with $\Sigma \Delta H_D$. The thermal efficiency could conceivably exceed the Carnot efficiency up to a maximum value given by the ratio of $285.8/237.2 = 1.21$.

8.1.3 Some Important Criteria

A listing of the major reaction steps (such as in Figure 8.1-1) gives only an indication of the complexity of a thermochemical cycle system. Choosing the best cycle and optimizing it in terms of efficiency and economics are challenging research goals. The criteria that must be considered include:

- Favorable thermodynamics.
- Fast reactions.
- High reaction yields.
- Simple separations.

- Process design factors (such as efficient use of heat, avoidance of scarce or hazardous reagents, and simplicity of construction).

Favorable thermodynamics implies both a negative and a very small free energy change (ΔG°) for each reaction step. If the free energy change were positive, the equilibrium constant of the reaction would be unfavorable, and much energy would be wasted in separating or concentrating the reaction products. If the free energy change were too negative, on the other hand, one or more of the reaction products would be too stable and hard to regenerate into the original reactants.

Fast reactions are desirable, especially for reactions involving gaseous reactants, because they enable us to use smaller and less expensive reaction vessels. The requirement that the free energy change be nearly zero makes it difficult to achieve fast reactions, however, because it allows very little driving force for the reactions. Reactions need to be selected in which the energy barriers are low enough to permit a very small free energy change to drive them rapidly. In many instances, the reaction rates can be enhanced by the use of catalysts.

High reaction yields are important to minimize recycling of unreacted reagents or diversion of material into undesirable by-products.

By using simple separations, we can avoid another area in which hidden costs can be substantial. Operations such as boiling, compression, and filtering can involve expensive process equipment and energy losses, and they need to be minimized.

Finally, careful process design can do much to provide the optimum tradeoff between the efficient use of heat energy and the economies of construction and operation. Process design contributions include new and innovative process equipment, plant layout, and siting.

8.2 General Atomic Cycle

8.2.1 Selecting a Cycle

We have selected the GA Sulfur-Iodine Cycle for our current TMR/Thermochemical Cycle coupling studies because it is the closest of the most advanced cycles to a pure thermal cycle. This permits the economies of scale to apply for chemical process units in a large chemical plant of the type considered here. Scale-up does not permit a similar reduction in costs for electrolysis units which are modular in construction. The Sulfur-Iodine Cycle also involves the handling of only liquids and gases, which is advantageous from a chemical engineering standpoint.

8.2.2 Chemical Description of GA Cycle

The current Sulfur-Iodine Cycle can be described chemically as follows:

All reactions in this system have been verified in the laboratory and total recycle has been illustrated in a small closed-loop cycle experiment.⁽²⁾ Major parts of the process are associated with separation and purification of the reaction products. A critical aspect for the successful operation of the process is the separation of the aqueous reaction products in reaction (1) above. Workers at the General Atomic Company have solved this problem by using an excess of I_2 in reaction (1), which leads to separation of the products into a lower density phase, containing H_2SO_4 and H_2O , and a higher density phase, containing HI , I_2 and H_2O . Reaction (2) shows the catalytic decomposition of HI , which is carried out under pressure (~ 80 atm) with HI and I_2 in liquid forms, in order to enhance the decomposition yield. Laboratory

decompositions are around 30% per pass and, therefore, use a recycle step. Unreacted HI is condensed out of the H_2 product and distilled away from the I_2 product. Pure H_2 is obtained by scrubbing out the remaining I_2 with H_2O .

The equilibrium for reaction (3) lies to the right at temperatures above 1000 K, but up to temperatures of ~ 1150 K, catalysts are needed to attain sufficiently rapid decomposition rates. Catalysts are available for this process, but careful consideration needs to be given to cost versus effectiveness. The SO_3 Decomposition Reactor is a challenging unit to design, owing to the high temperatures and corrosive products involved. All of the H_2SO_4 based cycles currently under study need such an SO_3 decomposer. Each of the three laboratories involved has approached this problem from a different standpoint. The Joule-Boosted Decomposer introduced by LLNL in this year's study provides a breakthrough in this difficult area by overcoming the materials and catalyst problems, and giving increased decomposition yields.

Figure 8.2-1 is a simplified schematic flow diagram of the GA Sulfur-Iodine Cycle conceptually showing product flows and recycle streams.

A complete description of the GA Cycle is given later in Section 9 of this report. For purposes of flowsheeting and process design, the GA Cycle has been divided into the following process sections.

- Section I, H_2SO_4 -HI Production and Separation.
- Section II, H_2SO_4 Concentration and SO_3 Decomposition.
- Section III, HI Separation and Purification.
- Section IV, HI Decomposition and H_2 Purification.
- Section V, Energy Distribution and the TMR-Chemical Plant Interface.

Figure 8.2.1 An early version of a block diagram of the General Atomic Sulfur-Iodine Cycle.

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SECTION 9

INTERFACING THE GA CYCLE

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9.0

INTERFACING THE GA CYCLE

In this section, we initially present a flow diagram showing the plant layout of the TMR driven GA cycle. We then discuss in some detail a preliminary design for each of the process units in the five sections of the Chemical Plant. The design description includes considerations of heat and mass flow, equipment sizing, and safety; and is based on chemical processes that have been established by laboratory experimentation. Finally we present a plot plan for a TMR driven Synfuels Plant. We find the Chemical Plant design to be reasonably straightforward and the plot plan to be quite compact.

9.1

Process Chemistry Status

The developmental effort on the GA Sulfur-Iodine Cycle has proceeded since 1974 along three parallel and mutually supportive lines:

- Chemical investigations
- Engineering flowsheet development
- Process demonstration

A new engineering flowsheet, now tailored to the TMR heat source, provides the basis for a Chemical Plant design from which we have estimated the cost of hydrogen production.

Ultimately the process must be optimized on the basis of a minimum hydrogen production cost. During the early stages of process development at GA, insufficient chemical information was available upon which to base an optimization procedure. Moreover, since a true optimization requires a complete plant design and cost estimate at each flowsheet iteration, a rigorous optimization could not be justified. Instead, the first flowsheet development was directed towards maximization of the overall process efficiency. Now the process has evolved into the present more practical form where costs have been factored in and units have been reduced in complexity. Chemical investigations provide the basis for our flowsheet calculations. In turn our flowsheeting effort, in a

search for high efficiency and/or low cost hydrogen, indicates those areas in which additional chemical investigations are needed.

The plant design, as it now exists, is based upon fully demonstrable chemical phenomena. In those few cases where the flowsheet calculations indicate that the desirable operating conditions are outside the experimentally defined region, only limited extrapolations have been made. Hence, additional experimental investigations are very desirable in the chemistry of the following areas:

- An alternate chemical approach to the H_3PO_4 method needs to be developed for the purification of HI.
- The thermodynamics of aqueous and gaseous H_2SO_4 at high temperature and pressure need to be better established.
- Kinetic data are needed both on the decomposition of SO_3 into SO_2 and O_2 , and on the recombination of SO_2 and O_2 to form SO_3 .
- Chemical equilibrium data are needed above 368 K for the main solution reaction (reaction (4) in Section 9.4.1).
- Solubility data are needed for gaseous SO_2 in the liquid H_2SO_4 and HI_x phases produced by the main solution reaction.

Of these areas, the first one, an alternate chemical approach to the H_3PO_4 method for HI purification, is the most critically needed area, since the current H_3PO_4 method imposes major cost requirements on the current version of the GA Cycle. Theoretical calculations and preliminary experimentation at GA indicate that alternative methods may be possible that will significantly reduce these cost values. Further experimental work is needed to establish this potential and provide the parameters for an engineering analysis.

9.2 Plant Design Status

The present plant design is based on new process flowsheets tailored to the TMR heat source. The H_2SO_4 processing step is most closely associated with the heat source so this section has been retailored for each blanket design considered. The remainder of the process also has been reflowsheeted with the emphasis upon decreasing the capital cost while maintaining high thermal efficiency. The overall process flow diagram for the TMR driven Heat Pipe Blanket design coupled with the GA Cycle is shown in Figure 9.2-1.

At this level of preliminary design the size of the process equipment is determined to the extent that the equipment may be costed. The design calculations make use of standard chemical engineering correlations for the sizing calculations. When the required physical and transport properties have been unavailable they have been approximated.

9.3 Energy Demands

Only the theoretical energy demands are discussed here to orient the reader as to the main energy needs of the GA Cycle. The actual plant energy demands are covered later in the text, especially in Section 9.8 which deals with the power systems interface.

9.3.1 Theoretical Energy Demands

In order to determine the theoretical energy demands of the steps in the GA Sulfur-Iodine Cycle, we first show all reactants and products in their pure thermodynamic reference states at room temperature according to the following reaction steps:

The energetics of these reactions as written are readily available in standard thermodynamic tables, e.g., JANAF Tables.⁽¹⁾ Thus,

Figure 9.2-1 Overall Process Flow Diagram for the TMR/Synfuels Hydrogen Production Plant,

reaction (1) is found to be endothermic by 104 kJ/mol when it produces pure separated $\text{H}_2\text{SO}_4(\text{l})$ and $\text{HI}(\text{g})$. In actual practice, reaction (1) is exothermic by 42 kJ/mol because H_2SO_4 and HI form very stable and immiscible aqueous solutions. Taking into account the real chemical states for both reactants and products as encountered in practice, we find that 140 kJ/mol (a value very close to the difference between 42 kJ/mol exothermic and 104 kJ/mol endothermic) must be supplied to recover pure separated $\text{H}_2\text{SO}_4(\text{l})$ and $\text{HI}(\text{g})$ from the reaction (1) products. Of this, 56 kJ/mol is required for the H_2SO_4 purification process and 84 kJ/mol for the HI purification process. It should be noted that these calculated energies are ideal energies that represent reversible processes. Therefore, in an actual engineering process, which is not reversible, we expect to expend a larger amount of energy to accomplish the purification. Indeed, the amount of energy required can be a great deal larger than the ideal energy if we are not careful in the engineering design.

The processes of purifying these acids are complex. Water is distilled out of the H_2SO_4 in five flash evaporation stages to best use the heat energy. The HI processing is even more complex in that a slightly exothermic process is run in which H_3PO_4 is added to the HI solution containing excess I_2 at 393 K. The H_3PO_4 precipitates the I_2 as a second liquid phase and extracts the H_2O and HI. This process also breaks the H_2O -HI azeotrope such that the distillation of the HI is not particularly demanding, requiring only a minor fraction of the 84 kJ/mole to free the HI. A major portion of the 84 kJ/mole is needed to free H_2O from the H_3PO_4 , which is done in a vapor recompression process to conserve heat.

The final energy demands that are associated with equations (2) and (3) can readily be estimated from available thermodynamic sources. H_2SO_4 must be vaporized first, primarily as $\text{H}_2\text{SO}_4(\text{g})$, requiring 56 kJ/mole at 600 to 700 K. Some H_2O must be vaporized with it since it is made only as the azeotrope (98 wt% H_2SO_4). After vaporizing, H_2SO_4 is endothermically decomposed to SO_3 and H_2O at

700 to 900 K. This requires another 103 kJ/mole. Finally, SO_3 undergoes decomposition at higher temperatures (1050-1250 K) to SO_2 and O_2 . This step requires 92 kJ/mole. Heat capacity differences between reactants and products and heat exchange losses must be accounted for in the final evaluation.

The decomposition of HI is a nearly heat-neutral step. Nevertheless, there are requirements for purifying HI- I_2 solutions amounting to 13 kJ/mole.

In summary, equation (1) forming solutions of products is 42 kJ/mole exothermic, equation (2) is 251 kJ/mole endothermic and equation (3) is 13 kJ/mole endothermic. In addition, the reversible energies associated with H_2SO_4 concentration and HI separation are 56 and 84 kJ/mole, respectively. Using only these energetics, the thermal efficiency of the cycle for hydrogen production would be given by:

$$286 \div (251 + 13 + 56 + 84 - 42 \times 0.2) = 0.72 \text{ (or 72\%)},$$

where 286 kJ/mole is the higher heating value of the hydrogen produced, and 0.2 is an estimated recovery factor for utilizing the heat from the low-temperature exothermic reaction.

An efficiency of about 50% is obtained when all aspects of energy requirements are included. This indicates good engineering performance for this cycle, especially considering the complexity of the process and the large amounts of material that need to be processed.

9.4 Main Solution Reaction Step (Section I)

9.4.1 Design Considerations

The main solution reaction step, Section I of the process, comprises all equipment associated with the chemical reaction:

where gaseous sulfur dioxide reacts with an excess of molten iodine and water to produce two immiscible liquid phases. The designations ℓ_1 and ℓ_2 indicate that water is the major component in the H_2SO_4

product phase, and iodine is the major component in the HI product phase. The H_2SO_4 phase which is the lighter (lower density) phase, also contains a trace of iodine. The heavy phase, referred to as HI_x , because it contains excess iodine in a complexed form, also contains water. The main solution reaction is exothermic and Section I requires no heat input from the fusion reactor but exports a significant quantity of low grade heat to a power bottoming cycle. The Section I process is shown schematically in Figure 9.4-1.

Even though Section I requires no heat input from the fusion reactor, the design of the main solution reaction step has a major influence on the overall process efficiency and thus upon the reactor size and the hydrogen production cost. Although decisions on heat and power recovery within Section I have an effect on efficiency, the composition and temperature of the light phase flowing to process Section II (the H_2SO_4 processing step) and of the heavy phase flowing to Section III (the HI concentration step) strongly influence the overall efficiency of the process. In the interest of efficiency, both streams should leave Section I at as high a temperature as possible and at the highest acid concentration possible. Since the equilibrium of an exothermic reaction shifts towards the reactants as temperature is raised, a compromise must be made between high temperature and high acid concentrations in the product. In deciding the operating conditions for Section I some economic trade-off calculations were made but a full scale process optimization was not in the scope of this work. The basis for the resulting decisions, as well as those for the rest of this study, may best be termed "engineering judgment." When trade-off calculations were not definitive, the decision was usually made on the side of "high efficiency" not "low capital cost."

Consideration of two factors dominates the design of Section I: (1) the large quantity of heat produced by the main solution reaction must be removed, and (2) the combination of hydrogen iodide and iodine in the heavy aqueous phase is extremely corrosive to materials normally employed for heat transfer surfaces. The only

metals known to resist HI_x are refractory metals such as niobium, tantalum and molybdenum. The present design employs niobium for heat transfer surfaces contacting HI_x , which permits the reaction energy to be used in the power bottoming cycle.

Niobium is relatively expensive: therefore, to reduce the capital cost of Section I, a number of design techniques have been employed which improve heat transfer and decrease the amount of Nb required. Some of these are:

1. Cool feed streams to Section I. Prior to carrying out the main solution reaction, we transfer the heat from the Section I feed streams to the power bottoming cycle. This involves less expensive heat transfer materials than Nb and reduces the amount of heat transferred from the reaction products of the main solution reaction.
2. Operate adiabatically. Instead of cooling the reaction products from 393 to 368 K (to shift the equilibrium towards the products) and then reheating the separated products, we operate adiabatically, and maintain the desired production rate of the main solution reaction by increasing the iodine concentration to provide the needed shift in the equilibrium.
3. Use direct-contact heat exchange. Where possible, we employ direct contact heat exchange between immiscible liquid phases or gas and liquid phases.
4. Apply enhanced heat transfer techniques. Utilizing two phase gas-liquid flow through the heat exchanger gives higher than normal convective heat transfer coefficients. Spiral-fluted tubing provides enhanced heat transfer and, due to the wall stiffening effect, permits thinner tube walls.

9.4.2 Heat Exchanger Reactor

Almost 52% of the chemical reaction forming HI and H_2SO_4 takes place in the Heat Exchanger Reactor (R-101 in the flow diagram of Figure 9.2-1) and in the piping immediately preceding the reactor. The

reactor selected is a shell and tube heat exchanger with fixed tube sheets. The vessel, tube sheets and heads are fabricated from mild steel. The heads are lined with fluorocarbon and the spiral fluted niobium tubes are welded to a niobium tube sheet liner. The process fluid is on the tube side of the heat exchanger and the shell side is part of the power bottoming cycle.

Upstream of the heat exchanger, the SO_2/O_2 from Section II (sulfuric acid processing) is mixed with the predominantly iodine stream from the Boost Reactor (C-103) and aqueous streams from the scrubbing reactors (C-101 and C-104). The combined streams react exothermically as they flow through a fluorocarbon-lined pipe and into the heat exchanging reactor. The pressure drop through the heat exchanger reduces the system pressure from an initial 0.50 MPa to 0.44 MPa at which point the temperature reaches 393 K. At the exit of the heat exchanger the O_2 is removed from the stream in the first of two separators (S-101). In the second separator (S-102), operating at 0.11 MPa, three phases separate: the heavy liquid phase (HI_x), the light liquid phase ($\text{H}_2\text{O}/\text{H}_2\text{SO}_4$), and a gas phase consisting of SO_2 and steam. The vaporization of water and SO_2 results in the temperature decreasing from 393 K to 385 K.

9.4.3 Lower Phase SO_2 Stripper

The lower phase solution (HI_x) is saturated with SO_2 which when allowed to remain forms sulfur and H_2S via tramp reactions. An oxygen recycle stream strips most of the SO_2 in a packed bed stripper (C-102) thus minimizing the tramp reactions. A minimum amount of oxygen is employed for this operation as the evaporation of water into the oxygen cools the HI_x requiring more heat input in Section III (HI purification). With a 10% O_2 recycle the HI_x is cooled from 385 K to 381 K.

The stripper, sized for operation at 70% of flooding, is a standard packed column design. The fluorocarbon-lined mild steel vessel is packed with 50 mm ceramic Raschig rings. Since the operating pressure is near atmospheric, a glass-lined steel vessel is a

viable option but would require fluorocarbon packing to avoid liner damage.

9.4.4 Boost Reactor

In the boost reaction, the sulfuric acid concentration of the light phase is increased from 50% to 57% by contacting with molten iodine in the presence of sulfur dioxide. The increased H_2SO_4 concentration is realized through the action of the main solution reaction. Water is used up by reaction with SO_2 and I_2 to form H_2SO_4 and HI . Over 7% of the total chemical reaction of Section I occurs in the Boost Reactor. Since the contact is performed in a countercurrent manner, the reactor also acts as a direct contact heat exchanger, raising the temperature of the sulfuric acid stream from 383 K to 393 K.

Although the mechanical design of the Boost Reactor is straightforward, i.e., a fluorocarbon-lined mild steel vessel packed with 50 mm ceramic Raschig rings, the sizing calculations are not. Common packed columns operate with either a gas rising through a descending liquid phase, or a light liquid phase rising through a descending heavy liquid phase. The Boost Reactor has both a gas phase (SO_2 in O_2) and a light liquid phase (H_2SO_4 and water) rising through the descending heavy liquid phase (I_2). The present design is based on adding the cross sectional areas required if the gas and light liquid separately contacted the heavy liquid. This is a very conservative approach. We believe that a design based upon information gained in a pilot plant would result in a smaller Boost Reactor vessel.

9.4.5 Scrubbing Reactors

The oxygen is purified before discharge to the atmosphere in the Scrubbing Reactors. The packed column reactors operate in a titration mode in which sufficient iodine is added to the scrub water in the lower part of the column to react stoichiometrically with the sulfur dioxide present in the oxygen. In the upper part of the column the oxygen is washed with pure water. The primary

Scrubbing Reactor (C-101) operates at 0.44 MPa purifying the gaseous product of the Heat Exchanger Reactor (R-101). Over 19% of the Section I reaction takes place in the primary Scrubbing Reactor. Almost 22% of the total reaction takes place in the secondary Scrubbing Reactor at 0.10 MPa. The secondary scrubber cleans up the oxygen stream exiting the Boost Reactor as well as the steam/SO₂ stream produced during depressurization of the liquid reaction product of the Heat Exchanger Reactor.

Both scrubbing reactors perform a second function as direct contact heat exchangers. Oxygen leaving the process is cooled to near ambient conditions while preheating the water entering the process. The scrubber vessels are of fluorocarbon-lined mild steel and the packing is 50 mm ceramic Raschig rings. Pilot plant tests may demonstrate reduced vessel costs. The upper portions of the scrubber contain only water and oxygen so no lining should be required in this area. Depending upon the temperatures reached in the lower portions of the scrubber, less expensive linings may be possible.

9.4.6 Heat and Power Recovery

The heat transferred to the steam power bottoming cycle from Section I totals 782 MW_t. The majority of this is transferred via the Heat Exchanger Reactor but significant quantities of heat are also transferred from the hot water products of Sections II and III and from the SO₂/O₂ product of Section II. Since the heat transfer materials used for water and SO₂ are much less expensive than the niobium used in the Heat Exchanger Reactor, there is the potential for further cost reduction by performing more of the cooling on water and SO₂ streams. Ultimately an economic optimization must determine the split in heat transfer duties on the basis of minimum hydrogen production cost.

A total of 5.4 MW is recovered in Section I using turbines for pressure reduction. Preliminary indications are that the turbines are economic but a final determination must await an analysis based on the hydrogen production cost resulting from this study.

9.5 H₂SO₄ Processing Step (Section II)

9.5.1 Design Considerations

The sulfuric acid processing step, Section II of the process, contains the process units necessary to convert the input 57 wt% H₂SO₄ solution to SO₂ and O₂ as products. Process units for Section II (shown schematically in Figure 9.5-1), consist of multi-effect evaporators, an H₂SO₄ azeotrope boiler, a high temperature SO₃ decomposer and product separation units.

9.5.2 Multi-Effect Evaporator

Decomposition of H₂SO₄ in Section II produces an SO₂-rich product gas from a 57 wt% aqueous H₂SO₄ feed stream. The feed stream, which is obtained from the main solution reaction, must be concentrated by first evaporating the water from the aqueous H₂SO₄ at 680 K to form the 98 wt% azeotrope. Subsequently the azeotrope is vaporized, the product gases are heated and finally SO₃ is thermally decomposed to form SO₂ and O₂.

The H₂SO₄ concentration step must efficiently remove water from the 400 K aqueous H₂SO₄ feed as well as increase its temperature up to 680 K to feed the boiler. This unit design involves an extremely delicate chemical engineering balance between thermal efficiency and capital cost. Normally, such as in sea water desalination and food processing, some 7 to 11 large multi-effect evaporators can be justified since energy costs must be kept at an absolute minimum in order to produce a cost effective product.⁽¹⁾ These multi-effect evaporators accomplish the evaporation by expending energy for only the latent heat of vaporization, since the sensible heat change is recovered by heat exchanging the product with the feed in a complex "backward feed" arrangement.

For our aqueous H₂SO₄ evaporation duty the "backward feed multi-effect evaporator" appears ideally suited since the viscosity and pumping power are reduced and the vessel sizes can be minimized -- consistent with our need for a special, corrosion-resistant construction.

Figure 9.5-1 H₂SO₄ Processing Step (Section II).

The capital cost is very high in order to achieve a high thermal efficiency in evaporation. A balance between cost and efficiency was selected with 5 parallel trains, each train having five Multi-Effect Evaporators staged in series to cover the required temperature range as shown in Figure 9.5-2. Here, at the top of the figure, one of the Multi-Effect Evaporators is shown in more detail. A Durichlor-51 pump circulates the liquid through a silicon carbide U-tube heat exchanger.^(3, 4) The large, tangential entry disengaging vessel is about 7 m in height and is lined with 3 courses of silica brick and has a Teflon-coated steel wall.⁽⁵⁾ The vapor is removed through a vapor/liquid cyclone disengager that is designed to remove the fine, entrained mist that would be carried over in the vapor and contaminate the next Multi-Effect Evaporator. At the bottom of Figure 9.5-2 is shown a train of five Multi-Effect Evaporators that utilize a 57 wt% H_2SO_4 feed at 400 K and produce the 98 wt% H_2SO_4 azeotrope at 680 K.

The Multi-Effect Evaporator train is driven with the energy recovered from the hot SO_3 Decomposer product gases (at around 720 K) which feeds the right hand side of the SiC heat exchanger of Multi-Effect Evaporator #5 as shown in Figure 9.5-2. Multi-Effect Evaporator #4 is heated by a heat exchanger driven both by the hot vapor from Multi-Effect Evaporator #5 and also by the hot SO_3 Decomposer gases from the heat exchanger on Multi-Effect Evaporator #5. Similarly, heat recovery and utilization is continued down the Multi-Effect Evaporator stages. Additional heat at 550 K is supplied to Multi-Effect Evaporator #2 by process steam from the steam-Rankine bottoming cycle. In this way, all of the heat is exhausted from the SO_3 Decomposer product gases at the smallest practical ΔT and highest thermodynamic availability.

The heat exchangers are a critical part of the design. For stability and low mist entrainment, we operate them in the submerged liquid configuration, while still carrying out the nucleate boiling needed to achieve a high heat transfer coefficient. As discussed later for the H_2SO_4 Boiler which is similarly designed (see Section 9.5.3),

Figure 9.5-2 Design for the Multi-Effect Evaporators for Concentration of H_2SO_4 .

we design the nucleate boiling at a heat flux of $q/A = 0.606 \text{ MW/m}^2$, which is some 4 times lower than the critical heat flux. The heat duty for vaporization is 412 MW_t . Sensible heat added produces a total of 830 MW_t , which sets the heat exchanger size. The near-optimum solution for this complex multi-variate design problem was found using 5 parallel trains of 5 Multi-Effect Evaporators driven by 25 heat exchangers using a combined total of 34,000 SiC U-tubes of 0.02 m inside diameter. The internal tube Reynolds number is 13,094, Nusselt number 42.5, heat transfer coefficient $663 \text{ W/m}^2\text{-K}$, film drop around 100 K at a velocity of 3.49 m/s. The pressure drop is 4 kPa (0.04 atm), and pumping power 6.4 MW_e .

On the shell-side of the the heat exchanger, the forced-circulated liquid provides high heat transfer coefficients with very small ΔT_s , a pressure drop of only 10 Pa, and uses only 16 kW_e of pumping power.

A reduction in capital costs would be possible if the Multi-Effect Evaporators and heat exchangers could be significantly enlarged to reduce the number of parallel trains. Of course, we find that the equivalence of design for the units, and including some 5 spare units, allows us to carry out a more efficient preventative maintenance program with a complete redundancy of equipment.

An alternate idea was developed as this Multi-Effect Evaporator design was being completed. This new concept involved a packed tower filled with SiC beryl saddles. The dilute H_2SO_4 feed would enter the top of the column as liquid and the H_2O vapor would leave the top stage as a gas. The column bottoms would consist of the concentrated 98 wt% H_2SO_4 azeotrope. A SiC reboiler would be attached to the bottom of the column to serve as the last stage and a means of producing the vapor flow to drive the column. This concept has the advantage of efficiently using direct-contact heat transfer internal to the column and involves considerably smaller and simpler equipment. It is possible the evaporator cost would be reduced about 10-fold. This needs to be traded off against a higher energy requirement than for the Multi-Effect Evaporators. This concept should be explored in future work.

9.5.3 H₂SO₄ Boiler

The H₂SO₄ Boiler is a particularly challenging design problem since either ceramic materials or high-silicon metallic alloys need to be employed to simultaneously withstand the H₂SO₄ corrosion and the 3.0 MPa pressure from the helium heat transfer fluid.

Our best choice is to use siliconized silicon carbide U-tubes for the heat exchangers in the H₂SO₄ Boiler. This approach has been used on a developmental basis by the Norton Company⁽⁶⁾ in conjunction with AiResearch, Inc. for high pressure (~5 MPa) helium "heat exchanger applications." We developed a special geometric configuration that established a rough optimization between the heat transfer and pumping losses in the helium and the boiling instabilities that could occur in the liquid H₂SO₄. This configuration is shown in Figure 9.5-3. The heat transfer basis for this design can be briefly summarized as follows.

We selected nucleate boiling on the H₂SO₄ liquid side of the tubes and then used the Palen and Small correlation⁽²⁾ to obtain an estimate of maximum obtainable heat flux:

$$\left(\frac{q}{A}\right)_{\max} = 61.6 \frac{P}{D_o N^{1/2}} \rho_v \lambda \frac{\sigma_s (\rho_l - \rho_v)^{1/4}}{\rho_v^2},$$

where P = the pitch diameter for the tubes,

D_o = outside tube diameter,

N = number of tubes (twice the number of U-tubes),

λ = the heat of vaporization,

σ_s = liquid-vapor interfacial surface tension,

ρ_l = density of the liquid,

ρ_v = density of the vapor.

A σ_s value of 43 dynes/cm at 680K and 1 atm was obtained from Gmelin.⁽⁷⁾ Thus, a maximum heat flux limit of 2.42 MW/m² was calculated for a U-tube boiler unit with 133 tubes, each 5 cm O.D.

Figure 9.5-3 Design for the H_2SO_4 Azeotrope Boiler.

with 3 mm walls. Since we wish to operate well below this critical heat flux level, say at 25%, we select $q/A = 0.606 \text{ MW/m}^2$ as a shell-side value. We use the Eckert and Drake⁽⁸⁾ correlation to obtain an estimate for the boiling side film drop of 0.3 to 1.5 K and conclude that the boiling side is well in hand. A parallel train of 5 boilers is required for the plant.

Next we needed to obtain the tube side heat transfer necessary to support this high flux (25% of the critical maximum heat flux). First we tried supercritical steam and found it unacceptable. Even at a Mach number of 0.5 or 336 m/s, the film drop with steam is 110 K, which is too high. Following supercritical steam, we next considered helium, which also has greater safety advantages. A rough optimum was found for 2.5 MPa high pressure helium operating at a flow rate of 1074 kg/s or 30 m/s (0.05 Mach number), and $Re = 137,000$. Here the film drop was 1400 K. However, by using turbulence promoters, small finned tube fluting, and swirl vane inserts, this film drop was cut to 150 K. For this operating condition, we calculated a 5.5 KPa (0.05 atm) pressure drop and a pumping power of 1.5 MW_e. A number of case studies were done varying parameters to establish the near-optimum design given above. Further parametric studies may be able to reduce further this still large (150 K) film drop.

9.5.4

SO₃ Decomposer for the Heat Pipe Blanket

The SO₃ Decomposer unit requires the highest temperature to be supplied from the TMR; therefore, the design of this unit and its relationship with the whole G.A. Cycle dictates the TMR interface concept. We have selected approximately 7 atm and 1250 K as the process conditions for the decomposer with the aim of obtaining a high decomposition yield, while eliminating the need for catalysts. The fraction of SO₃ converted to SO₂ + O₂ (quenched) under these conditions is expected to be around 84%. We have elected to supply the required 1250-K temperature by means of electrical heating; thus, this unit is called a Joule-Boosted Decomposer.

Figure 9.5-4 shows the TMR interface using heat-pipe heated helium gas to generate electricity for the Joule-Boosted Decomposer and to supply heat to other process units.

The Joule-Boosted Decomposer is shown in Figure 9.5-5 as we have developed the design. The heating elements are commercially available SiC electric (AC power) furnace elements. The vessel and headers are simple in design and can be silica brick-lined, Teflon-coated steel. From a materials standpoint SiC and silica brick should offer an acceptable life under the 1250 K, 7 atm SO_3 , SO_2 , O_2 , H_2O environment. Eight units in parallel are needed to handle the required hydrogen production rate.

The Joule-Boosted Decomposer offers the best possible safety isolation of the O_2 -containing process gases from the hot, liquid metal-containing fusion blanket.

9.5.5 SO_3 Decomposer for the Two-Zone Blanket

For the Two-Zone Blanket, the decomposer is a catalytic fluidized bed reactor operating at 1050-1100 K. The unit contains internal heat exchanger tubes to provide the heat for the highly endothermic SO_3 decomposition. It can achieve up to a 55% conversion using a CuO catalyst, if sulfation of the catalyst does not become a problem around 1050-1100 K. If sulfation presents a serious problem, our alternative is to use a more expensive platinum catalyst on a titania support. The details of this Fluidized Bed Decomposer were given in last year's Annual Report,⁽⁹⁾ so only a brief overview is provided here.

Figure 9.5-6 shows the percentages of heat available at three temperature levels from the Two-Zone Blanket to drive the decomposer and other process units. It is difficult to drive the decomposer with any less heat than 25% of the blanket heat at the highest temperature (1125 K), and it has proved difficult for the Two-Zone Blanket to provide the required 25% high temperature heat due to neutronic limitations. Helium is used directly from the blanket to provide 1125 K heat to the decomposer.

Figure 9.5-4 Configuration of the Heat Pipe Blanket Interface driving the G.A. Cycle.

Figure 9.5-5 Design for the Joule-Boosted SO_3 Decomposer.

Figure C-6 Configuration of the Two-Zone Blanket Interface Driving the G.A. Cycle.

Figure 9.5-7 shows some of the details of the fluidized bed design, using 0.5 mm diameter catalyst spheres. The fluidized bed is on the shell side of the unit with the helium on the tube side. The ΔT s across the helium film within the tube are about 20 K, and the ΔT s on the outside of the tubes in the fluidized bed region are expected to be around 14 K. The helium ΔT presents a difficult design problem and the 20 K is a compromise between higher helium source temperatures and helium pressure.

The Fluidized Bed Decomposer requires seven units in parallel to handle the needed production rate. This approach of coupling the blanket to the process with helium offers good safety isolation for the process. More optimization work is needed to reduce the overall capital and operating costs further. This concept saves the costs of a large turbo-electric generator required in the Joule-Boosted Decomposer, at the expense of running a hotter fusion blanket and a more complex decomposer design.

9.5.6 Decomposer Recuperator

The Decomposer Recuperator is a heat exchanger unit that simultaneously preheats the feed to the decomposer using heat from the hot SO_2 , O_2 , H_2O , and SO_3 that leave the decomposer. This unit is particularly important, since without it, a large quantity of sensible heat in the temperature range of 680 K to 950 K would have to be supplied from the blanket. In the Joule-Boosted Decomposer design the feed is further heated from 950 K to 1250 K using electrical heat. In addition to the sensible heat requirement, there is, of course, additional heat required to supply the endothermic heat for decomposing SO_3 into SO_2 and O_2 .

This Decomposer Recuperator is a difficult design to accomplish since it is a gas-to-gas heat exchanger, and these types of units characteristically have very low heat transfer coefficients. The result is a high pressure drop, high pumping power, large and expensive heat transfer areas, and high alloy temperatures.

Figure 9.5-7 Design of the Catalytic Fluidized Bed SO_3 Decomposer for the Two-Zone Blanket.

The design we evolved was again a techno-economic compromise. We used a standard shell and tube configuration as shown in Figure 9.5-8 and held the temperature low enough for Incoloy-800H.

The heat transfer analysis involved varying fluid temperature, wall temperature, heat exchanger surface areas, and pumping power, in order to produce a near optimum design. We found that the hot decomposed gases could be dropped from 1170 K to 730 K, while the feed could be preheated from 680 K to 950 K. The 950 K was selected to hold the average wall temperature to 1050 K so that the Incoloy-800H wall thickness could be kept at 3 mm, with a maximum calculated creep rate of 1% in 20 years. The heat exchanger area was found to be 14,814 m² and the pumping power was 1.24 MW_e equally split between the shell and tube side.

The gas velocity within the tubes was 13.6 m/s with a Reynolds number of $Re = 79,000$ based on the tube diameter. A Nusselt number of 284 and heat transfer coefficient of 78.8 cal/m²-s were used to obtain a gas to wall ΔT of 150 K.

9.5.7 Decomposer Cooler

The Decomposer Cooler is a heat exchanger that is part of the Multi-Effect Evaporator and cools the decomposer gas products from the recuperator (described above) from 730 K to 418 K while heating the Multi-Effect Evaporator process gases from 400 K to 680 K. This unit is made of SiC tubing.

9.6 HI Concentration Step

9.6.1 Design Considerations

The hydrogen iodide concentration step, Section III of the process, entails separation of a mixture of hydrogen iodide, iodine and water (HI_x) into its component parts. A simplified flow diagram of Section III is presented in Figure 9.6-1. The iodine and water are separately returned to Section I, the main solution reaction step, and the hydrogen iodide is decomposed in Section IV of the

2 units, 4366 tubes per unit
 Total heat transfer area per unit = 7407 m²

Figure 9.5-8 Design for the SO₃ Decomposer Recuperator

Figure 9.6-1 Schematic Flow Diagram of Section III, Separation of Aqueous HI_x into HI , I_2 , and H_2O .

process. The use of phosphoric acid as an extractive distillation agent, first proposed in 1975, remains as the reference process for HI purification although laboratory investigations of alternative techniques are being pursued.

The system HI-H₂O forms a maximum boiling azeotrope at a composition of 57 w/o HI which is approximately the composition of HI_x on an iodine free basis. Iodine is held in the HI-H₂O solution through the formation of polyiodides such as HI₃, HI₅, HI₇ etc., which are formed only in the presence of water. Addition of H₃PO₄ lowers the activity of water which performs two functions; first, it destabilizes the polyiodide complexes permitting iodine to form a separate liquid phase; secondly, it breaks the azeotrope which allows the HI to be distilled from the mixture. The HI distillation is performed under pressure so that liquid HI is available for decomposition in Section IV. Water is removed from the phosphoric acid by evaporation, and the phosphoric acid is recycled back into the process.

9.6.2 Iodine Separation

Liquid iodine is separated from the HI_x in two steps. Both operational steps are performed in fluorocarbon-lined mild steel vessels packed with 20-mm ceramic saddles. In the iodine knock-out column (C-302), the HI_x is contacted counter-currently with 96 wt% H₃PO₄. The HI and H₂O are extracted into the H₃PO₄, leaving molten iodine saturated with H₃PO₄. H₃PO₄ is washed away from the iodine with water in the iodine wash column (C-301). The wash column is operated at 0.3 MPa and 393 K so as to maintain both iodine and water in the liquid state.

9.6.3 Hydrogen Iodide Distillation Column

HI is distilled from the H₃PO₄ solution in a plate column operating at 0.9 MPa. Operation at this pressure requires a higher temperature in the bottoms than would be required at low pressure, but an expensive HI liquefaction step is eliminated. Efficient use of the thermal energy, required for operation of the HI Distillation

Column, is made possible by producing the HI product as a liquid and thus eliminating the thermodynamic inefficiency associated with a liquefaction compressor and that associated with production of power to operate it. Additionally, an intermediate condenser allows part of the condensing heat to be withdrawn from the column at a useful temperature. Although a majority of the heat required to preheat the feed and to operate the column is obtained by heat recovery within Section III, 355 MW is required from Section V at 523 K.

Design of the HI Distillation Column entailed a trade-off between capital and operating costs. Use of an intermediate condenser and operation at a relatively low reflux ratio reduce thermal energy requirements, but require more trays in the column for adequate chemical separation. The final design included 50 Hastelloy-C trays in a Hastelloy-C clad mild steel tower. The trays selected were the trough type, being a reasonable compromise between the higher efficiency higher cost of bubble cap trays and the lower efficiency lower cost of sieve trays.

An additional small design complication arises because the feed to the HI Distillation Column is saturated with iodine. Liquid iodine must be removed continuously from the middle of the column to avoid buildup. A stream, containing two liquid phases consisting of phosphoric acid and liquid iodine, is withdrawn from the iodine buildup region. The phosphoric acid returns to the column from the top of a liquid-liquid separator and the bottom iodine phase passes to the iodine wash column for phosphoric acid removal before returning to Section I.

The small quantities of H_2SO_4 and SO_2 , remaining in the lower phase product from Section I, react chemically with HI in the feed preheater for the HI Distillation Column. The product of the reactions are H_2O , I_2 , H_2S and S. The quantities involved are so small that the relative amounts of S and H_2S have not been determined. For flowsheeting and cost estimating purposes, it has

been assumed that half the sulfur in H_2SO_4 and SO_2 ends up as H_2S and half as S. The H_2S will leave the column in the overhead product and the S is assumed to exit the column with the H_3PO_4 . The liquid S is separated from the H_3PO_4 and oxidized back to SO_2 in Section II.

9.6.4 Phosphoric Acid Concentration

Removal of water from the phosphoric acid is accomplished in three stages of vapor recompression-driven flash evaporation. Only the last stage requires any heat input from the fusion reactor but all three stages require significant quantities of power for vapor recompression. A total of 641 MW of shaft power is required to operate the compressors whereas only an additional 104 MW of thermal power at 484 K is required from Section V to heat the high temperature evaporator. Heat is recovered within each evaporation stage from interstage cooling of the six stage compressor, from the condensation of the compressed steam and from the concentrated phosphoric acid product.

The phosphoric acid concentration step is simple in concept but capital costs of the turbine compressors and heat exchangers are significant in the overall hydrogen production cost. Even though methods have been identified for making a considerable reduction in the cost of H_3PO_4 concentration, alternative chemical systems are under investigation with the goal of eliminating H_3PO_4 from the process.

9.7 HI Decomposition Step (Section IV)

9.7.1 Design Considerations

The HI decomposition step (Section IV of the process) requires the following operations:

- Decompose $\text{HI}(\ell)$ to $\text{H}_2(\text{g})$ and $\text{I}_2(\ell)$
- Separate HI from the I_2 and recycle to the decomposer
- Separate HI from the H_2 and recycle to the decomposer

- Scrub the H_2 product in preparation for distribution

A simplified flow diagram of Section IV is presented in Figure 9.7-1.

Three process variables dominate the design of Section IV and have a direct impact on the fusion reactor interface. The temperature and pressure of the HI Decomposition Reactor govern the equipment size and amount of recycle through the reactor whereas the HI- I_2 Distillation Column pressure determines the maximum process temperature required from the fusion reactor. Variables governing the cleanup of the hydrogen product have a smaller, but still significant, impact on the hydrogen production cost.

9.7.2 HI Decomposition

Hydrogen iodide decomposition is accomplished in the HI Decomposition Reactor (R-101). The extent of the decomposition reaction is limited by thermodynamic equilibrium. Therefore, to limit the amount of recycle of HI back through the reactor, process conditions must be chosen so as to give a high conversion per pass through the reactor. Previous studies at the General Atomic Company⁽¹⁰⁾ have demonstrated that when decomposition is carried out under high pressures so that HI and I_2 are present as liquids, a much higher decomposition yield is obtained than with the analogous gas phase decomposition. Selection of the temperature and pressure involve a number of tradeoffs. The critical temperature of HI places an upper limit upon the initial reaction temperature of an adiabatic flow reactor, while the required hydrogen delivery pressure places a lower bound upon the system pressure. The reactor finally selected for HI decomposition is an adiabatic flow reactor with a 4-minute residence time employing activated charcoal as a catalyst. The mild steel vessel is lined with fluorocarbon to protect it from the HI and iodine. The reactor operates at 8.3 MPa. The reaction temperature increases from 415 K at the inlet to 424 K at the outlet due to the slightly exothermic nature of the reaction under these conditions. The majority of the energy required to bring the HI to reaction

Figure 9.7-1 Schematic Flow Diagram of Section IV, The HI Decomposition Step.

temperature is supplied by heat reuse within Section IV. Only 6% of the energy (10.6 MW_t) must be supplied from the fusion reactor.

A design based upon a continuous stirred tank reactor deserves future consideration. It would require a considerably longer residence time than the adiabatic flow reactor, but the system pressure could be lowered to 5 MPa, which may lead to reduced capital costs.

9.7.3 HI-I₂ Distillation

The liquid product from the HI Decomposition Reactor passes through a pressure reducing turbine to the HI-I₂ distillation column (C-401). The pressure of the still (5.1 MPa) sets the temperature of the still bottoms at 713 K, which is the highest temperature required from the fusion reactor helium in the Joule-Boosted concept. The still pressure and temperature may be decreased further, thus decreasing the high temperature heat load on the fusion reactor but at the expense of increased low temperature heat requirements to reheat the still overhead product for recycle to the HI cracker. In addition, the heat now supplied from the overhead condenser to the evaporative refrigerator and from the bottoms product to the liquid HI heat exchangers, would instead have to be supplied from other sources.

In addition to the 41 MW required from the fusion reactor helium stream, an additional 36 MW is required at 522 K. This additional heat may be used at a lower temperature because the distillation column, is designed with intermediate reboilers. The use of intermediate reboilers increases the capital cost due to the cost of both the boilers and additional trays in the column but the overall thermal efficiency is improved significantly.

Each distillation column, constructed from Hastelloy-B clad mild steel, is 2.7 m in diameter for the bottom 12 m and expands to 4.7 m in diameter for the upper 3 m. There are 25 Hastelloy-B trough type trays in the bottom section and 6 in the top. The expanded top section is required by both the higher flow rates and lower density encountered in this section of the column.

9.7.4 Hydrogen Cleanup

The hydrogen from the decomposer is cleaned in three operations. First, the bulk of the hydrogen iodide is removed by condensation. Second, remaining HI is removed with a water wash. Finally, the trace of H_2S is removed by a combination of chemical reaction and water wash.

The gaseous product of the HI Decomposition Reactor is cooled in three stages. A heat exchanger removes the high temperature portion of the heat to the power bottoming cycle and dumps the low temperature heat to cooling water. The stream is further cooled from 303 K to 291 K via an absorption refrigeration system. The LiBr based refrigeration⁽¹¹⁾ is driven by waste heat from the condenser of the HI- I_2 Distillation Column. After condensate separation, only a small quantity of HI and possibly H_2S remain in the hydrogen.

The remaining HI is easily removed via a water wash (C-402). A minimum amount of water is employed because any water added at this point must be removed by distillation in Section III. In the absence of iodine, HI is not corrosive to nonmetallics, therefore, hydrocarbon-lined mild steel is sufficient for this application. Since traces of iodine could enter the HI scrub column due to process upsets in the HI condensation system, the lower portions of the column are lined with fluorocarbon. The column is packed with 25-mm ceramic Raschig rings.

Due to the high acidity in the lower portions of the column, H_2S cannot be absorbed. H_2S builds up slightly in the upper section of the column until at steady state, H_2S passes on to the H_2S removal column (C-403) at the same rate it enters the HI scrub column. Since H_2S has a relatively low solubility in water, a simple water scrub would require that an excessive amount of water be processed through Section II to reconvert the H_2S to SO_2 . The problem is solved by adding a small amount of SO_2 to the H_2S absorber. H_2S reacts with SO_2 in the presence of water to produce a sulfur slurry. The slurry is concentrated via a filter with back-

flush so only a small amount of water accompanies the S back to Section II where it is converted to SO_2 via reaction with SO_3 . The mild steel column, packed with 25-mm ceramic Raschig rings, is hydrocarbon lined to prevent corrosion.

The H_2 pressure is dropped to the distribution pressure of 5.1 MPa via a power recovery turbine and thus exits the process.

9.8 Power Systems Interface (Section V)

9.8.1 Total Systems Interface

A substantial portion of the effort on this project was spent in developing the best possible total systems interface, i.e., closely matching the fusion reactor electrical and thermal supplies to the Chemical Plant demands. Section 7.3 briefly introduced the case studies that were used in developing this interface. Although we did not initially recognize that this part of the plant design would be a dominant element in the overall TMR driven H_2 Plant, we have clearly concluded that indeed Section V is a very important element. This interface must be tailored to the details of the energy source, and the interface in turn dictates the details of the temperature gradients in the process units of the chemical plant. The Chemical Plant design and this interface are not interchangeable with different blanket designs and/or energy sources. the plant must be custom designed and individually developed for the blanket coolant type, pressure, temperature, heat distribution, availability, and cost all to be compatible.

9.8.2 Heat Pipe Blanket Interface

For the Heat Pipe Blanket, Figure 9.8-1 shows the power system interface (Section V) for linking the TMR to the H_2 Plant. This power system is only approximate since an exact energy balance has not been completed, although mass balances are complete. Energy balances are sufficiently complete however for purposes of sizing of equipment and costing of the plant. The power system interface, as presented here, is also useful for illustrating the main elements

$$\eta = 0.477, Q = 15, 84\% \text{ conversion, } 4875.9 \text{ mol H}_2/\text{s}.$$

Figure 9.B-1. Power Systems Interface (Section V) of the Heat Pipe Blanket to the G.A. Cycle.

elements of the complex plant with which we are dealing. The reader is also referred to Figure 4.2-1 in Section 4 for another illustration of the power-flow diagram.

Using the process demands for each of the GA Cycle process sections (I through IV) that have been discussed, we can prepare the right hand side of Figure 9.8-1. This figure is arranged roughly from the highest temperature level across the top of the diagram to the lowest across the bottom. Heat generally flows from left to right. The three critical quantities: heat flow, Q (in MW_t), shaft or electrical work, W (in MW_e), and temperature, T (in K), are shown on all critical streams. Major components in each major stream are also occasionally shown.

Electricity from of the TMR Direct Converter (dashed line) is converted from high voltage DC to 440 VAC, 3 phase, to provide some of the electrical energy to Joule-heat the decomposer. The next lower grade energy is the thermal output from the Direct Converter in the form of a 190 kg/s helium coolant stream at 950 K. The hot helium at 2.5 MPa drives a Brayton cycle turbine with a mechanical efficiency, ϵ , of 90% and compression ratio, $r_p = 3.33$, to produce 69 MW_e of electricity at an efficiency of 24%. The turbine is bottomed into a steam-Rankine cycle to produce additional electricity. The next lower grade energy is the thermal output of the blanket in the form of 1100 kg/s helium at 2.5 MPa and 875 K. It is first heat-exchanged from 875 K to 852 K with heat exchanger sections of the HI/I_2 Distillation Column at 713 K and then with the H_2SO_4 boiler at 680 K. This cooled helium then enters a Brayton cycle turbine at 862 K, operating at a mechanical efficiency of $\epsilon = 0.90$ and a compression ratio of $r_p = 3.33$, and generates 351 MW_e at an efficiency of 19%.

Helium exits the turbine at 574 K and is then bottomed into the 522 K section of the HI Distillation Column. The helium then exits at 567 K and is further bottomed into a steam-Rankine cycle where it generates additional electricity.

The 700 K helium from the first wall drives a turbine at 13% efficiency to make 21 MW_e of electricity for the decomposer. The cooled 507 K helium is then bottomed into a steam-Rankine cycle to produce additional electricity.

The steam-Rankine cycle uses all of the available waste heat, generates steam at 4.8 MPa and 618 K, some of which is used directly in the process and the rest of which is used in a steam turbine to generate 743 MW_e of shaft work (at 34% efficiency) for process compressors and pumps and 23 MW_e of electricity to help electrically heat the decomposer. The steam-Rankine turbine condenser operates at 303 K using water from a wet cooling tower at a wet bulb temperature of 293 K.

The thermal efficiency of this cycle is finally obtained at 47.7% (or 49.3% if we take credit for the high pressure of the H₂ product). This is slightly above GA's earlier value⁽¹²⁾ of 47% (48.6%); we believe mainly because the contribution of the high efficiency electricity generated by the Direct Converter. The two values may well be equivalent within the uncertainties of our calculations.

9.8.3 The Two-Zone Blanket

The Two-Zone Blanket also requires a power systems interface. Mass balances are only approximate and energy balances have not been fully closed at this time, however the information is sufficiently complete for making rough plant cost estimates. The reader is referred to Figure 4.3-1 in Section 4 for an illustration of the power-flow diagram.

It is useful to describe the approximate values for the basic elements of the Two-Zone Blanket interface so that they can be compared with the Heat Pipe Blanket interface. For the Two-Zone Blanket, the thermal output of the direct converter is combined with that from the low temperature section of the blanket to supply about 1965 MW_t of 3.0 MPa, 850 K helium at 1100 kg/s to heat exchange with the HI/I₂ Distillation Column and the H₂SO₄ Boiler.

The 740 K lower temperature, spent helium is then used to drive a part of the steam-Rankine cycle. The high temperature section of the blanket supplies 1125 K helium at 1600 kg/s to heat the SO_3 decomposer to 1100 K using about 611 MW_t of energy.

The steam-Rankine cycle uses steam at 720 K and 5.8 MPa to produce 704 MW_e of electricity at 35% efficiency. Brayton cycle electrical generation is not required in this system. As before, a portion of the Rankine cycle steam is fed out into the Chemical Plant in order to drive the lower temperature process units. The steam-Rankine condenser system operates down to 315 K using a dry-cooling tower exchanging to air at a dry bulb temperature of 300 K. This dry cooling tower is thought to be an improvement in that siting the unit will not have the environmental objections that a plant with a wet cooling tower might have. This design requires more pumping power (190 MW_e more) to circulate more than twice the amount of helium than was required in the Heat Pipe Blanket design. Although this added pumping power is a clear debit, the elimination of the need for electric generation for Joule-Boosted Decomposer is more than compensated for with a net estimated thermal efficiency for this plant of about 49% (51% with H_2 pressure credit), which is somewhat higher than the 47.7% (49.3%) in the Heat Pipe Blanket. The penalty of course is a requirement for higher temperatures in the blanket, heat transport system, and fluidized bed heat exchanger, all of which involve difficult materials and design problems, and which could be very expensive.

9.9

Materials Selections for the Thermochemical Plant

The most difficult materials problems in the Thermochemical Plant are associated with Section II of the GA Cycle, and involve the processing of liquid and gaseous sulfuric acid at high temperatures and high pressures (see Section 9.5). This section therefore will discuss the materials problems of Section II. Materials needs for the other Sections of the GA Cycle have been adequately met and are described in the text. Conventional approaches have generally been

used in making materials selections for these other Sections. Where corrosion has presented special problems, we have used linings of hydrocarbons or Teflon in many instances, and in case of the HI_x phase, we have used niobium for heat transfer surfaces.

9.9.1 Materials Selections for Section II

Materials selections need to be made for the following process units for Section II:

- Multi-Effect Evaporator
- H_2SO_4 Boiler
- Joule-Boosted Decomposer
- Fluidized Bed Decomposer
- Decomposer Recuperator
- Decomposer Cooler

Multi-Effect Evaporator and H_2SO_4 Boiler

Materials problems are similar for the Multi-Effect Evaporator and the H_2SO_4 Boiler. The main problem is to provide a heat exchanger that is tolerant of hot concentrated sulfuric acid. All known metallic heat exchanger materials (except for expensive noble metals such as Au and Pt), are severely corroded by these H_2SO_4 solutions above temperatures of ~ 500 K. A number of ceramic and inter-metallic materials have been tested in hot concentrated H_2SO_4 at 623-693 K in recent years at LLNL^(13, 14) and at Westinghouse.⁽¹⁵⁾ The top candidate materials from both corrosion resistance and heat exchanger design standpoints are the following:

- Siliconized SiC
- "CrSi₂" coated Incoloy-800
- Durichlor-51 (Fe-14% Si-4% Cr)

All of these materials depend upon the development of a corrosion resistant SiO_2 scale on the surface to provide protection against corrosion by H_2SO_4 . Similarly, it is well known that pure silica

glass (SiO_2), such as in the form of silica brick, is inert to corrosion by H_2SO_4 .

SiC currently presents the best prospect as a heat exchanger material. Siliconized SiC (a two-phase composition consisting of a mixture of SiC and Si) is produced at the Norton⁽¹⁶⁾ and Carborundum⁽¹⁷⁾ companies and is especially suited for this type of application. This type of material, which contains about a 10-15 wt% excess of silicon metal, is impervious to gases, has a high thermal conductivity, high strength, good thermal shock resistance, and can be fabricated in complex shapes and bonded together to form heat exchanger assemblies.⁽¹⁶⁾ Corrosion testing of SiC for 1121 h and Si for 592 h in 97 wt% H_2SO_4 at 673 K at the Lawrence Livermore National Laboratory showed no evidence of corrosion,⁽¹³⁾ thus confirming the corrosion resistance of both SiC and Si for this application.

Specimens of Incoloy-800 coated with chromium silicide of nominal composition CrSi_2 were tested for corrosion for 240 h at 673 K in 97 wt% H_2SO_4 at the Lawrence Livermore National Laboratory⁽¹⁴⁾ and found to show a very low rate of corrosion (extrapolated weight loss rate of $15 \text{ mg/cm}^2\text{-y}$). The coatings were about 120 μm thick, uniform in thickness, well-bonded to the substrate, and without evidence of fractures. The coatings were prepared by Dr. Charles M. Packer of Lockheed Missiles and Space Company of Palo Alto, California, using a slurry coat and rapid melt technique. Although the above results are preliminary in nature, they are encouraging in that a conventional heat exchanger material such as Incoloy-800 can be protected against corrosion by a coating process.

Durichlor-51 is a commercial high silicon cast iron material that is yet another candidate as a heat exchanger or container material. It is basically an intermetallic compound with a composition of approximately Fe_3Si and, as such, presents the disadvantages common to most intermetallics of poor ductility, low tensile strength, and poor machining and fabrication characteristics. Also, being a cast

material, the control of internal porosities and non-uniformities present production problems. Nonetheless, with proper quality control and with good engineering design and practices, these problems can be controlled. From a corrosion standpoint, tests of up to 524 h on Durichlor-51⁽¹³⁾ have shown only a moderate amount of attack (5 μm surface penetration); but very importantly, corrosion appears to proceed very uniformly over the surface of this material. It therefore seems reasonable to extrapolate to a lifetime of the order of 5 y for this application. In contrast to Durichlor-51, Duriron is another high silicon cast iron alloy that shows a rather irregular corrosion behavior with grain pullouts, corrosion pits, and spallation in similar H_2SO_4 corrosion tests at 673 K.⁽¹³⁾

In view of the developments thus far, our choice of material is siliconized SiC for the H_2SO_4 heat exchangers for both the Multi-Effect Evaporator and the H_2SO_4 Boiler. We select silica brick as a liner material for the vessels, both because of the inertness of silica to corrosive attack by sulfuric acid, and to provide thermal insulation so that more conventional materials (e.g., Teflon-lined steel) can be used for the vessel wall. We select Durichlor-51 for the bodies of the liquid H_2SO_4 circulation pumps.

Joule-Boosted Decomposer

For the Joule-Boosted Decomposer, we mainly need to select a material for the heating elements. We are in the fortunate position that Tiegs at Oak Ridge National Laboratory has recently exposed a number of materials to sulfuric acid vapors at 1 atm pressure for up to 1900 h at 1273 K and 2300 h at 1498 K.⁽¹⁸⁾ Of the materials tested, SiC proved to be the best and to give an extremely low corrosion rate, e.g., 0.5 μm depth of corrosion in 1000 h at 1273 K. Furthermore, corrosion appears to proceed according to a diffusion controlled mechanism through a protective silica (SiO_2) scale, which, by extrapolating by the indicated $t^{1/2}$ law, we obtain 7 μm corrosion in 20 years - which is negligible.

1

Increase of pressure of gaseous sulfuric acid vapors from the 1 atm conditions used by Tiegs to the 7 atm required in the Joule-Boosted Decomposer will increase the corrosion somewhat due to volatility of SiO_2 in steam as gaseous Si(OH)_4 , but the effect is not expected to be significant at 1250 K.

Two materials approaches are available for the vessel walls of the Joule-Boosted Decomposer. The wall can either be lined with silica brick backed by 316 stainless steel maintained above the condensation temperature of sulfuric acid (~680 K), or it can be constructed of Incoloy-800H which is limited to a maximum temperature of ~1050 K because of creep strength limitations (see further details on Incoloy-800H properties in the discussion of the Fluidized Bed Decomposer below). Selection of the materials approach to use will depend upon engineering considerations.

Fluidized Bed Decomposer

The main materials problem in the Fluidized Bed Decomposer is selection of a heat exchanger material to meet corrosion resistance and creep strength requirements.

Corrosion tests on a number of heat exchanger alloys for H_2SO_4 vapor decomposers have been carried out at JRC Ispra and at the General Atomic Company at temperatures in the 773-1173 K range. Their results⁽¹⁹⁾ indicate that as bare uncoated alloys, Incoloy-800H and Inconel-625 provide the best materials with anticipated lifetimes of a few years. A substantial gain in lifetime can be achieved by using an aluminide-coated Incoloy-800H on the surface exposed to the decomposing SO_3 gas. This gives an unusually stable coating-substrate combination that stabilizes in coating thickness and weight change after an exposure of 200-300 hours to the gaseous H_2SO_4 products.⁽¹⁵⁾ Corrosion protection is apparently afforded by an Al_2O_3 film, and imperfections in the coating are self-healing. The useful life of aluminide-coated Incoloy-800H for this application is believed to be of the order of 20 years. On the high pressure helium side, corrosion is not a problem.

From a consideration of creep strength, cost, and fabrication issues, Inconel-617, Inconel-625, and Incoloy-800 (or Incoloy-800H) appear to provide the best alternatives. Stress rupture and creep properties for Inconel-625 and Incoloy-800 are comparable.⁽²⁰⁾

Although creep strength data are not available for Inconel-617, it shows 1000-hour stress rupture strengths that are three times higher⁽²⁰⁾ than for the other two alloys. Thus, extrapolations of the available data for Inconel-625 and Incoloy-800 suggest a tensile creep strength for 1% creep in 100,000 hours of $2000 \pm 50\%$ psi (1.4×10^7 Pa) at 1100 K and $1200 \pm 30\%$ psi (8.2×10^6 Pa) at 1150 K. We estimate the corresponding values for Inconel-617 to be $6000 \pm 50\%$ psi (4.1×10^7 Pa) at 1100 K and $3600 \pm 30\%$ psi (2.4×10^7 Pa) at 1150 K based on the argument that the creep strengths scale as a fraction of the long term creep rupture data similar to Inconel-625 and Incoloy-800. Although no creep data are available for Incoloy-800H, it is a material that has been designed for higher strength than Incoloy-800 and should give a better performance.

From a consideration of both corrosion and creep strength we select Incoloy-800H as the heat exchanger material for the Fluidized Bed Decomposer. A minimum wall thickness 3 mm is required to maintain material integrity after allowances for 0.5 mm corrosion penetration. An aluminide protective coating is recommended on the SO_3 side of the heat exchanger to obtain a longer equipment life.

Selection of a vessel material for the Fluidized Bed Decomposer involves the same considerations as for the Joule-Boosted Decomposer. Either a silica brick-lined 316 stainless steel vessel or an Incoloy-800H vessel would be suitable.

Decomposer Recuperator

The main materials problem for the Decomposer Recuperator is for the Heat Exchanger, and as was the case for the Fluidized Bed Decomposer, corrosion and long-term creep strength are the main considerations. Temperatures are sufficiently lower here (~1050 K)

that the problems are less severe. Therefore, an aluminide coating should not be needed. We select Incoloy-800H at a wall thickness of 3 mm as the preferred material with Inconel-617 as a backup for the Decomposer Recuperator heat exchanger. Selections for the vessel walls are either silica brick-lined 316 stainless steel or Incoloy-800H.

Decomposer Cooler

Transport piping from the Decomposer Recuperator to the Decomposer Cooler would be made from Incoloy 800H. The Decomposer Cooler heat exchanger is an integral part of the Multi-Effect Evaporator and the heat exchanger tubes would be made from siliconized SiC.

9.9.2 Summary of Materials Selections for Section II

The selection of materials for Section II are summarized below in Table 9.9-1.

Table 9.9-1 Summary of Materials Selections for Heat Exchanger and Vessel Materials for Section II of the GA Thermochemical Cycle.

<u>PROCESS UNIT</u>	<u>Hx MATERIALS</u>	<u>VESSEL MATERIALS</u>
Multi-Effect Evaporator	Siliconized SiC tubing, Durichlor-51 pumps	Silica brick liner on Teflon-lined mild steel
H ₂ SO ₄ Boiler	Siliconized SiC tubing, Durichlor-51 pumps	Silica brick liner on Teflon-lined mild steel
Joule-Boosted Decomposer	SiC heating elements	Incoloy-800H, or silica brick liner on 316 S.S.
Fluidized Bed Decomposer	Aluminide-coated Incoloy-800H	Incoloy-800H, or silica brick liner on 316 S.S.
Decomposer Recuperator	Incoloy-800H	Incoloy-800H, or silica brick liner on 316 S.S.
Decomposer Cooler	Siliconized SiC tubing	Vessel is integral part of Multi-Effect Evaporator

9.10

Safety Considerations

The main safety issues involve the control, handling and containment of tritium, liquid lithium, and sulfuric acid. The safety issues regarding tritium and liquid lithium were reviewed and discussed in some detail in last year's report,⁽⁹⁾ and it was concluded that adequate controls were designed into the system to maintain proper safety. We have applied similar considerations in the current designs, including particular attention to both residual and accidental leakages of tritium into the environment or product, tritium inventories in the system, and potential problems of liquid lithium fires. We feel that the safety issues have been well met, and many of the details have been covered in this year's text. We refer the reader to last year's report for further details.

In regard to safety of sulfuric acid, it is important that conservative designs be maintained on the vessels and piping containing hot liquid and gaseous sulfuric acid and its decomposition products, since sulfuric acid introduced into the environment is a serious health hazard. We feel that we have taken adequate measures in our design studies, and recognize further that the sulfuric acid industry has been a large and well-established industry and one that we can draw on for future design information as needed for assuring a safe plant.

9.11

Plant Layout and Plot Plan

9.11.1

Plant Layout

A complete plant process layout, assembling all of the sections presented above, has been prepared and is given earlier in the report in Figure 9.2-1, complete with important process parameters and labels on key process sections and on process units. The legend describes the symbolic representation used. Generally we have attempted to arrange the highest temperature processes at the top of the figure and the lower temperature processes at the bottom. At the immediate left is the Tandem Mirror Fusion driver and the associated helium coolant streams operating turbo-generators and

process heat exchangers as a topping cycle and bottoming into a steam-Rankine cycle for additional electricity generation.

A larger version of this complete process flow diagram and flow-sheets are available upon request. However, this reduced-size figure has been included to help one grasp the overall picture of the Fusion/Synfuels Process concept.

9.11.2

Plot Plan

An artists' conceptual drawing of the plot plan is shown in Figure 9.11-1. The purpose of developing a rough plot plan is to get a rough idea of the approximate land area and relative sizes of the TMR nuclear island, turbine-generator, steam generator and process heat exchanger building, as well as and the rest of the chemical plant. Since the turbine-generators, heat exchanger and process plant units had to be designed and sized in order to arrive at capital costs, developing a plant plot layout required only a small additional effort. We conclude from the plot plan that this plant is quite compact, and raises no new issues regarding heat transport distances, safety, etc.

Figure 9.11-1 TMR-Synfuels Thermochemical Hydrogen Production Plant Plot Plan.

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SECTION 10
PRELIMINARY COST ESTIMATES

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10.0

PRELIMINARY COST ESTIMATES

In this section, we carry out preliminary cost estimates of capital costs and hydrogen production costs using established procedures for costing by the fusion community and chemical industry for the respective parts of the plant. Three approaches are presented for financing and operation of the plant: public utility (100% debt), private utility (50% debt, 50% equity), and chemical industry (100% equity). A hydrogen production cost of about \$10-12/GJ (for both Heat Pipe and Two-Zone Blankets) is obtained for the public utility in July 1980 dollars, with higher costs indicated for private utility or chemical industry approaches. These costs are very favorable and indicate that fusion syngas will be competitive in the timeframe for emergence of fusion in ~2030.

10.1

The Economic Basis

We assume the plant would be operated at a time when fusion electric plants have been in operation for at least a decade (~2030-2050). We present costs based on constant, July 1980 dollars, and use currently available chemical plant technology. Since the chemical portion of the plant will be manpower intensive and cannot be operated with as few people or as little supplies or maintenance as in an electric power plant, we have used chemical plant experience⁽¹⁾ in estimating all labor costs.

Ultimately, a Fusion-Syngas Plant will most likely produce a combination of pipeline quality synthetic natural gas (e.g., 10% H_2 , 80% CH_4 , and 10% C_2H_6 and higher hydrocarbons and other gases) together with liquid transportable fuel (e.g., CH_3OH) and chemical feedstocks for plastics, fertilizers, and solvents (e.g., C_2H_4 , C_2H_3O , acetic anhydride, NH_3 , etc). If the production is mostly synthetic natural gas for pipelines, a utility is likely to finance, build, and operate the plant; whereas if the products are fuels and chemicals, a chemical company will be involved. So the method of financing could be by either utility (public or private)

or chemical industry. We will present costs for these three kinds of financing.

10.2 Costing Procedures

Both the fusion community and chemical industry have developed considerable expertise in estimating the capital cost as well as the projected operating costs of their respective plants. As explained in 10.3.6 below, the TMR capital costs are derived directly from those previously presented by Henning⁽²⁾ whereas the TMR operating costs as well as utility financing are calculated in the manner specified by Battelle⁽³⁾ as the standard for fusion reactor conceptual designs.

Costs for the Chemical Plant and the power systems interface are obtained by standard chemical engineering costing techniques. These costing techniques are based upon using actual construction experience from many chemical plants to predict capital and operating costs from FOB equipment costs. Although the power systems interface (Section V) and H_2SO_4 processing step (Section II) were costed at LLNL while the remainder of the Chemical Plant (Sections I, III & IV) was being costed at GA, the same reference works were used at both locations. The new reference work by Peters and Timmerhaus⁽¹⁾ was used to augment the older Guthrie methods^(4,5) both for estimating the FOB costs of equipment, as well as deriving from the FOB cost estimates of the installed direct capital cost, the total plant investment and the operating cost. Where the standard works could not provide capital costs, as in most of Section II and the power recovery systems, vendor estimates or other special sources of FOB cost data were used. When only pre-1980 costs were available, the industry standard, Marshall and Swift (M&S), equipment cost index was used to estimate 1980 costs.

It is recognized that some of the best costing techniques are maintained as proprietary by A&E, chemical and oil companies. A proprietary costing method, available at GA was used to spot check

costs for a representative number of items. No overall bias could be observed although variations between different types of equipment were noted.

We believe that our cost estimates are well within our goal of $\pm 30\%$ accuracy.

10.3 Preliminary Capital Cost

10.3.1 Main Solution Reaction Step (Section I)

The simplified schematic of Section I was presented in Figure 9.4-1. Table 10.3-1 presents a detailed list of the equipment required for performing the main solution reaction step, together with size and cost data. With the exception of the Heat Exchanger Reactor (R-101), all items have been costed by the Guthrie method.^(4,5)

The structural material used throughout Section I is mild steel. If other than dry SO_2 , oxygen and or water are present, the steel must be protected by an appropriate coating or lining. Although spray-on hydrocarbon based coatings are adequate when moist SO_3 is present, liners of bulk fluorocarbon are specified when H_2SO_4 , HI or I_2 are present. Installed cost of fluorocarbon linings is estimated to be $\$1100/\text{m}^2$.

The Heat Exchanger Reactor is unique because of the material involved, niobium. The $\$220/\text{kg}$ cost of the niobium tubing dominates the reactor cost. Added to the $\$15.8\text{M}$ cost of the tubing is an appropriate amount for installation of the tubing ($\$3.2\text{M}$), plus the FOB cost of the equivalent mild steel heat exchangers ($\$1.2\text{M}$), giving an FOB cost of $\$20.2\text{M}$. Installation cost was estimated from the cost of the equivalent mild steel heat exchanger since niobium is required only for the heat transfer surfaces.

The Guthrie method allows specification of the piping materials separate from the specification of the materials of construction of the equipment. Either mild steel or standard fluorocarbon lined

Table 10.3-1 Preliminary Capital Costs for Section I - M\$, July 1980

Item No.		Parallel Units	Dia. (m)	Lgth. (m)	Equiv. Mild Stl. FOB Cost	FOB Cost + Adders	Install. Dir. Cost	Total Plant Invest.
C101	Primary scrubbing reactor	6	3.8	9.0	0.326	1.353 ^{ab}	2.211	2.934
C102	Lower phase SO ₂ scrubber	6	5.1	19.5	0.847	3.868 ^{ab}	6.096	8.027
C103	Boost reactor	6	5.1	19.5	0.847	3.868 ^{ab}	6.096	8.027
C104	Secondary scrubbing reactor	6	4.5	8.6	0.381	1.651 ^{ab}	2.653	3.508
S101	High pressure flash drum	6	3.6	13.5	0.489	1.743 ^b	2.929	3.956
S102	Low pressure flash drum	6	4.2	12.0	0.452	1.692 ^b	2.875	3.849
S103	SO ₂ -H ₂ O knockout drum	6	4.7	3.9	0.184	0.192 ^b	0.564	0.868
S104	Primary water knockout drum	6	3.0	3.9	0.113	0.113	0.337	0.505
S105	Second. water knockout drum	1	3.0	3.9	0.019	0.019	0.056	0.084
R101	Hx reactor	6	1.8	7.5	1.175	20.171	22.206	26.518
E101	SO ₂ Hx	6	1.7	12.0	0.713	1.084	2.023	2.945
E102	Sec. III water Hx	2	1.1	12.0	0.174	0.174	0.403	0.615
E103	Sec. II water Hx	3	1.2	12.0	0.275	0.275	0.638	0.972
P101	Water feed pump	6+1	-	-	0.002	0.002	0.008	0.012
P103	Reactor feed pump from C101	6+1	-	-	0.009	0.017	0.031	0.042
P104	Iodine feed pump	6+1	-	-	0.018	0.036	0.065	0.090
P105	Reactor feed pump from C103	6+1	-	-	0.033	0.066	0.118	0.163
P106	Reactor feed pump from C104	6+1	-	-	0.046	0.092	0.169	0.233
P107	Reactor feed pump from S103	6+1	-	-	0.007	0.007	0.016	0.024
TE101	O ₂ power recovery turbine	1	-	-	0.971	0.971	2.095	3.157
TE103	Iodine power recovery turbine	1	-	-	0.057	0.165	0.244	0.321
TC101	SO ₂ turbine compressor	6	-	-	7.059	7.059	15.233	22.960
TOTAL CAPITAL COST					14.197	44.618	67.066	89.810

^a Adders include field installation of packing.

^b Adders include field installation of liner.

mild steel piping was specified as appropriate. Pumps are either cast iron for water, or cast iron with a molded fluorocarbon liner for corrosive solutions.

Since Section I operates essentially as six systems in parallel, 83% of production capacity may be maintained if any one system is down for repairs. Pumps are cross-connected such that one installed spare backs up the active six. The SO_2 compressors have good reliability and a high cost; so there is no in-line spare. The power recovery turbines are not spared except by a bypass valve.

The column labeled "Total Plant Investment Basis" in Table 10.3-1 indicates that the major unit costs of Section I are associated with the Heat Exchanger Reactor (R-101) and the SO_2 turbine compressor (TC-101). The cost of R-101 may be decreased if more of the heat load is shifted to water-based heat exchangers. The compressor could be completely eliminated if the pressure in Section II were increased sufficiently, although an overall cost savings may not result.

10.3.2 H_2SO_4 Processing Step (Section II)

The schematic of the H_2SO_4 processing step (Section II) that was presented in Figure 9.5-1 consists of the following units to be costed:

1. Multi-Effect Evaporators and supporting hardware
2. Section II drive pumps
3. H_2SO_4 Boiler
4. Joule-Boosted Decomposer (Heat Pipe Blanket)
5. Fluidized Bed Decomposer (Two-Zone Blanket)
6. Decomposer Recuperator
7. Decomposer Cooler
8. Vapor/Liquid Separator
9. Redundant process units

Costing of these units is discussed below and summarized in Table 10.3-2. Details are given mainly for the Heat Pipe Blanket approach, although cost values are summarized for the Two-Zone Blanket as well in Table 10.3-2.

1. Multi-Effect Evaporators

The basis of the cost of the Multi-Effect Evaporator is mostly dependent on the sheer size of the evaporator units to handle the total vapor flow, following the design guidelines of Peters and Timmerhaus⁽¹⁾. Here the body of the evaporator is sized according to the heat exchanger heat transfer area that is required to drive the unit. In our design, 25 units of 136 m² of area per unit were required (5 trains with 5 temperature-staged units per train). The unit size is limited by the physical size of the heat exchanger (3 m dia x 10.67 m long). This represents a capital cost of \$1.3M per unit, or \$32.5M for all 25 units, including fabrication and installation. The totals are in Table 10.3-2.

The 25 liquid circulation pumps, manufactured with Durichlor-51 bodies, can handle a total flow of 13,500 kg/s, requiring shaft power or electric motors rated at 50 kW per unit. These pumps are estimated to cost \$22,000 each.⁽¹⁾

2. Section II Drive Pumps

The Section II drive pumps are sized to drive the liquid flow across the shell side of the heat exchangers in each of the process units, i.e., the Multi-Effect Evaporator, the H₂SO₄ Boiler, the Decomposer Recuperator, and the Decomposer Cooler. The pressure drop is only 0.04 MPa (0.4 atm), and the pumping power about 26 MW_e, with most of it (23 MW_e) occurring across the shell side of the Joule-booster Decomposer. For the Two-Zone Blanket, the pumping power needed to fluidize the catalyst is about 28 MW_e.

Table 10.3-2 Preliminary Capital Costs for Section II -M\$, July 1980

	Heat Pipe Blanket		Two-Zone Blanket		
	Fabricated, not Installed each unit	Total	Fabricated and Installed	Fabricated, not Installed	Fabricated and Installed
<u>Multi-Effect Evaporators</u>					
SiC Tubes	3.18				
Forced Circulation Evaporators (25)	23.21				
Pumps and Meters	1.21				
Brick & Teflon linings	1.79	29.39	41.15	29.39	41.15
<u>Section II Drive Pumps(3)</u>	2.64	2.64	3.00	2.64	3.00
<u>H₂SO₄ Boiler</u>					
SiC U-Tubes	0.43				
Teflon and Brick Lined Vessels(5)	0.61	1.04	1.24	1.04	1.24
<u>Joule-Boosted Decomposer</u>					
SiC Heating Elements	14.40				
Incoloy-800H Vessels(8)	5.60	20.00	21.00	0	0
<u>Fluidized Bed Decomposer</u>					
Incoloy-800H Tubes	30.50				
Catalyst (Pt)	9.60				
Incoloy-800H Vessels(7)	4.90	0	0	45.00	50.00
<u>Decomposer Recuperator</u>					
Hx and Vessels(2)	6.00	6.00	6.40	6.00	6.40
<u>Decomposer Cooler</u>					
Hx and Vessels(2)	3.00	3.00	3.40	3.00	3.40
<u>Vapor/Liquid Separator</u>					
Vessels(5)	0.55				
Teflon Lining	1.18	1.73	1.93	1.73	1.93
<u>Redundant Process Units</u>					
1 Pump	0.34				
1 Boiler Unit	0.31				
1 Decomposer	3.00	3.65	3.95	7.58	8.20
TOTAL		67.45	82.07	96.38	115.32
TOTAL DIRECT + INDIRECT CAPITAL COST ^a			94.38		132.62

^a This includes a 15% added contingency.

3. H₂SO₄ Boiler

The H₂SO₄ Boiler costing basis is simply the cost of 133 SiC U-tubes at \$32.7/m⁽⁶⁾ for 5 boiler units for a total SiC cost of \$430K. Each of the required 5 boiler vessels has a 491 MW_t heat load rating and is costed at \$40K, or \$200K total cost. The Teflon and brick lining for 62.8 m² of lining for each vessel gives a total lining cost of \$410K. The total cost is therefore \$1.04M total for 5 vessels (see Table 10.3-2).

4. Joule-Boosted Decomposer (Heat Pipe Blanket)

The cost is dominated by the \$850 cost for each SiC heating element. For the 17,000 heating elements required, this cost alone is \$14.4M. The eight vessels into which these elements are placed would be fabricated out of Incoloy-800H at \$5.6M (see Table 10.3-2).

5. Fluidized Bed Decomposer (Two-Zone Blanket)

We carried out parametric studies on a platinum-catalyzed, fluidized bed decomposer, and found that 1050 K is a rough optimum temperature. For this use, the decomposer installed cost would be \$50M for a total of 7 units. The catalyst alone would cost \$9.6M, \$4.9M for the bare vessels, and \$30.5M for Incoloy-800H tubes (aluminide-coated on the SO₃ side) for the helium heat supply. Thus, the total cost (fabrication and installation) is about \$50M.

6. Decomposer Recuperator

These costs are controlled by the required heat transfer area of 14,814 m², since the recuperator involves gas-to-gas heat exchange with low heat transfer coefficients. Physical size limitations constrain these units to 4,366 tubes per unit so that two units would be required, fabricated out of Incoloy-800H at a total cost of \$3.00M. If the tube wall temperature of 1050 K appears to be too high in order to meet the condition of <1% creep in 20 years, then the tubes could be SiC U-tubes as used in the evaporator or boiler, approximately doubling the cost to \$6.00M. The higher cost has been used in Table 10.3-2.

7. Decomposer Cooler

This unit is actually the tube side of the Multi-Effect Evaporator heat exchangers and accepts the sensible heat provided by the Decomposer Recuperator. The temperature of this unit is sufficiently low (418-730 K) that condensation of H_2SO_4 will occur. Therefore we select SiC tubing for the heat exchanger and obtain a cost for the Decomposer Cooler of \$3.00M.

8. Vapor/Liquid Separator

The cost of this separator is controlled by the required cross sectional area of 181 m^2 to achieve an acceptably low aerosol sedimentation velocity of 0.2 m/s. We use 5 vessels, 6.8 m in diameter, with a 5 mm wall fabricated out of Incoloy-800H. The vessel heights are set by the 300 second residence time required for water holdup; thus, a height of 11 m is obtained. The fabricated cost is about \$0.55M and a Teflon lining adds about \$1.18M for a total cost of \$1.73M.

9. Redundant Process Units

We elected to place within the process three process units in a redundant mode:

- 1 - Pump (Durichlor-51)
- 1 - H_2SO_4 Boiler
- 1 - SO_3 Decomposer

These would be manifolded into the rest of the process unit train, so that in case of unexpected failure or a planned unit shutdown for preventative maintenance, a unit could be taken out of service with a minimum of downtime.

10.3.3 H1 Concentration Step (Section III)

Capital costs for Section III are presented in Table 10.3-3. The simplified flow diagram for this section was presented in Figure 9.6-1. Materials of construction and costing techniques are similar to those of Section I. Where, because of thermal or

mechanical limitations, fluorocarbon linings are not acceptable, we have used Hastelloy-C.

The hydrogen iodide concentration step is the most capital intensive portion of the chemical plant. Although significant costs are associated with iodine knockout and hydrogen iodide distillation, the largest costs are associated with phosphoric acid concentration.

The high cost components of the phosphoric acid concentration system are steam compressors and heat exchangers. The compressor costs appear to be fixed unless future developments bring down the relative cost of turbine compressors. Flowsheet modifications have the potential for significantly reducing the heat transfer costs.

The large amount of rotating machinery in the phosphoric acid concentration system makes this part of the process a potential source of downtime. The compressors in the third evaporation stage were deliberately oversized to make them identical to the first and second stage units. If any one of the 15 units is down, intermediate pressures may be shifted to permit operation at 93% of capacity with only a slight overall efficiency loss.

10.3.4 HI Decomposition Step (Section IV)

The HI decomposition step has the lowest flow rates and lowest costs of the four chemical process steps. The costs are as high as given in Table 10.3-4, only because of the high pressures involved. The simplified flowsheet for Section IV was given in Figure 9.7-1.

10.3.5 Power Systems Interface (Section V)

The cost basis for Section V includes all of the helium coolant pumping, heat exchange, heat recovery, and shaft or electric power generation, except for the blanket heat exchangers (covered in Section 10.3.6) and the process side (tube side) of the heat exchangers which are located within the Chemical Plant. We cost,

Table 10.3-3 Preliminary Capital Costs for Section III M\$, July 1980

Item No.		Parallel Units	Dia. (m)	Lgth. (m)	Equiv. Mild Stl. FOB Cost	Cost + Adders	Install. Dir. Cost	Total Plant Invest.
C301	Iodine wash column	3	4.5	18.0	0.339	2.700 ^{ab}	3.263	4.036
C302	Iodine knockout column	10	7.7	24.0	3.059	28.537 ^{ab}	33.618	41.220
C303	HI distillation	3	6.9	21.6	0.889	21.642 ^a	24.394	28.745
S301	Surge drum C303 reflux	3	2.7	10.2	0.125	0.413 ^a	0.737	0.945
S303	Flash drum 1st H ₃ PO ₄ stage	6	6.0	9.0	0.487	1.619 ^b	2.876	3.686
S304	Flash drum 2nd H ₃ PO ₄ stage	5	3.6	16.2	0.348	1.162 ^b	2.061	2.641
S305	Flash drum 3rd H ₃ PO ₄ stage	4	6.6	10.7	0.451	3.156	5.653	6.852
S306	S-H ₃ PO ₄ separator	3	5.9	22.2	0.544	3.810	6.620	8.037
E301	Dilute H ₃ PO ₄ cooler	48	1.5	12.0	5.098	51.486	58.025	70.859
E302	Intermediate condenser on C303	3	1.6	12.0	0.144	1.459	2.029	2.450
E303	Reboiler on C303	6	1.1	12.0	0.881	3.853	4.983	6.444
E304	Condenser on C303	6	1.6	12.0	0.800	3.962	5.344	6.794
E305	Iodine cooler	1	1.4	12.0	0.099	0.998	1.388	1.676
E306	Heater 1st H ₃ PO ₄ stage	48	1.5	12.0	4.964	46.083	63.000	77.019
E307	Heater 2nd H ₃ PO ₄ stage	28	1.5	12.0	3.124	24.758	33.644	41.249
E308	Heater 3rd H ₃ PO ₄ stage	22	1.5	12.0	2.203	16.713	22.697	27.874
E309	Concentrated H ₃ PO ₄ cooler	6	1.8	12.0	0.794	8.023	11.154	13.471
E310	Water cooler 1st H ₃ PO ₄ stage	1	1.7	12.0	0.125	0.923	1.250	1.539
E311	Water cooler 2nd H ₃ PO ₄ stage	1	1.8	12.0	0.134	0.987	1.337	1.646
E312	Water cooler 3rd H ₃ PO ₄ stage	1	1.7	12.0	0.139	1.027	1.391	1.713
P301	Lower phase feed pump	10+1	-	-	0.207	0.414	0.794	1.084
P302	Iodine wash water pump	3+1	-	-	0.006	0.006	0.013	0.020
P304	Feed pump C303	10+1	-	-	0.724	1.449	2.653	3.656
P305	Concentrated H ₃ PO ₄ pump	10+1	-	-	0.202	0.404	0.739	1.017
TE301	Dilute H ₃ PO ₄ PR turbine	1	-	-	0.684	1.368	2.314	3.139
TE302	1st H ₃ PO ₄ stage PR turbine	1	-	-	0.618	0.618	1.322	1.946
TE303	2nd H ₃ PO ₄ stage PR turbine	1	-	-	0.618	0.618	1.322	1.946
TE304	3rd H ₃ PO ₄ stage PR turbine	1	-	-	0.548	0.548	1.173	1.727
TE305	Iodine power recovery turbine	1	-	-	0.597	0.995	1.821	2.505
TC301	1st H ₃ PO ₄ stage steam comp.	6	-	-	25.791	25.791	55.657	83.891
TE303	2nd H ₃ PO ₄ stage steam comp.	5	-	-	21.492	21.492	46.381	69.907
TE304	3rd H ₃ PO ₄ stage steam comp.	4	-	-	17.194	17.194	37.105	55.927
TOTAL CAPITAL COST					93.428	294.208	436.759	575.663

^a Adders include field installation of packing.^b Adders include field installation of liner.

Table 10.3-4 Preliminary Capital Costs for Section IV - M\$, July 1980

Item No.		Parallel Units	Dia. (m)	Lgth. (m)	Equiv. Mild Stl. FOB Cost	FOB Cost + Adders	Install. Dir. Cost	Total Plant Invest.
C401	HI-I ₂ distillation column	1	4.7 ^a	15.0 ^a	0.170	1.749 ^b	2.143	2.597
C402	HI absorber	6	4.1	9.6	1.050	1.309 ^{bc}	4.301	5.704
C403	H ₂ S scrubber	2	3.8	27.3	0.700	1.023 ^{bc}	2.828	3.981
S401	Reactor effluent vapor liquid separator	1	2.9	13.2	0.162	0.293 ^c	0.710	0.984
S402	H ₂ -HI vapor liquid separator	1	2.4	9.6	0.121	0.201 ^c	0.514	0.717
D401	Reflux surge drum - C401	1	3.3	15.0	0.075	0.248 ^c	0.364	0.497
R401	HI decomposition reactor	4	4.5	27.0	1.750	3.688 ^{bc}	8.202	11.245
E400	Misc. integrated Hx	13	-	-	1.300	6.461	9.222	11.799
E4XX ^d	Absorption refrigeration unit	1	-	-	1.689	1.689	2.819	4.562
P401	Reactor feed pump	4+1	-	-	0.638	1.845	2.906	3.876
P402	HI recycle feed pump	1+1	-	-	0.287	1.406	2.215	2.954
P403	Reflux pump - C401	1+1	-	-	0.013	0.037	0.058	0.075
P404	Make-up water feed pump	6+1	-	-	0.020	0.020	0.048	0.072
P405	Recycle pump - C402	6+1	-	-	0.024	0.069	0.109	0.145
P406	SO ₂ -H ₂ O pump to C403	6+1	-	-	0.018	0.018	0.042	0.063
P407	Recycle pump - C403	2+1	-	-	0.015	0.015	0.035	0.052
TE401	HI-I ₂ power recovery turbine	1	-	-	0.348	1.007	1.483	1.997
TE402	Hydrogen power recovery turbine	1	-	-	1.625	1.625	3.507	5.286
TOTAL CAPITAL COST					10.005	22.695	41.506	56.606

- ^a The HI-I₂ distillation column is 2.7 m dia. for the bottom 12 m, and 4.7 m dia. for the remaining 3 m.
- ^b Adders include field installation of packing.
- ^c Adders include field installation of liner.
- ^d The absorption refrigerator includes heat exchangers E408, E410 and E411.

in Section V, the shell side of all of the in-process heat exchangers.

The results are shown in Table 10.3-5. The most significant costs are those for the helium gas turbine generator sets which are costed at $\$189/\text{kW}_e$ or $\$54.10/\text{kW}_t$.⁽⁷⁾ This cost compares closely with those quoted by Pratt and Whitney Co.⁽⁸⁾ at $\$137.81/\text{kW}_e$ ($\$49.34/\text{kW}_t$) for an installed bare turbine generator set, and $\$174.61/\text{kW}_e$ ($\$62.51/\text{kW}_t$) for installed units in a complete power station with land, buildings, accessory electric and power plant equipment and other minor expenses. Since we are not including land and power plant buildings, etc., the GA cost of $\$54.10/\text{kW}_t$ appears a very reasonable compromise for our July 1980 dollar cost. We also discussed our application with Westinghouse⁽⁹⁾ and Brown-Boveri⁽¹⁰⁾ and they confirm our choice of $\$54/\text{kW}_t$ as a good estimate for July 1980 dollar costs.

The next most significant item is the steam generator. Using a quotation to GA by United Engineers and Constructors,⁽⁷⁾ we take a value of $\$16.80/\text{kW}_t$ in July 1980 dollars for a fabricated and installed steam generator unit including some indirect costs. Thus, a 2293 MW_t unit would cost $\$38.56\text{M}$ for the Heat Pipe Blanket, and a 1853 MW_t unit would cost $\$31.13\text{M}$ for the Two-Zone Blanket.

Helium piping to the H_2SO_4 Boiler in both blanket designs, and to the Fluidized Bed Decomposer in the Two-Zone Blanket design, is simple and inexpensive. We use a mild steel outer pipe which is from 100 to 200 K cooler than the helium. A 580 K stainless steel inner liner is perforated and spaced away from the cool, mild steel outer wall by 5 cm, and the space is insulated with Babcock and Wilcox "Kao-Wool" spun mineral insulation.

A water treatment plant has been added to recondition the recirculated process water and to a minor extent precondition the make-up feed water. Water treatment consists of softening with

Table 10.3-5 Power Systems Section V Capital Costs
(Fabricated, Installed and Indirect Costs) - M\$, July 1980

	Heat Pipe Blanket	Two-Zone Blanket
Steam Rankine Bottoming System:		
First wall Brayton bottoming	0.41	0.41
Steam generators (2295/1853 MW _t) ^a	44.27	35.80
Turbine generator set (646/725 MW _e) ^a	139.10	157.58
Cooling tower	10.48	7.96
Shell-side of all process Hx-bottoming	2.16	2.16
First Wall Coolant Turbine Loop:		
Turbine generator set	9.33	9.33
Direct Converter Thermal Output:		
Turbine generator set (286 MW _t or 76 MW _e)	17.79	0
Main Blanket Heat Loop:		
Turbine generator set (1847 MW _t)	114.91	0
Direct Converter DC to AC:		
Main HV transformer station	3.44	3.44
Inverter banks	1.72	1.72
Secondary transformer station	4.24	4.24
Electrical distribution system	0.39	0.39
Helium Piping:		
Main blanket:		
Mild steel outer pipe	2.61	2.61
Perforated 304-S.S. liner	0.52	0.52
Kao-Wool insulation (installed)	0.46	0.46
Direct Converter heat recovery		
Total (similar to main blanket)	0.61	0.61
To Fluidized Bed Decomposer	0	3.62
Water Treatment Plant	5.75	5.75
TOTAL DIRECT PLUS INDIRECT CAPITAL COST	358.19	236.60

^a The first number refers to the Heat Pipe Blanket and the second number to the Two-Zone Blanket.

ion-exchange and demineralization with reverse osmosis units, organic removal by carbon adsorption, and other standard waste treatment operations. The plant is sized for a total recirculated water flowrate of 2.0×10^4 mol/s H_2O/s (8 million gallons per day), and costed at \$5.00M.

10.3.6 Fusion-Driven Nuclear Island:

For the Tandem Mirror Reactor we have adopted the capital costs of Henning,⁽²⁾ which cover a range depending on the complexity of the physics and the number of extra coils required on ECRH units for additional plasma heating. We have added \$80M to the blanket costs that Henning originally listed,⁽²⁾ owing to the additional blanket complexity required to provide the high temperatures necessary to drive the thermochemical cycle. These TMR costs are included as part of the capital cost summary in Table 10.3-6. We have not included any turbo-generator and electrical distribution equipment as part of the TMR costs, since they are included in Section V.

10.3.7 Summary of Capital Costs

Table 10.3-6 summarizes the capital costs developed above for the Thermochemical Plant and for the TMR Fusion Driver. The Thermochemical Plant estimates include the appropriate installation and indirect charges plus a contingency (usually 15%) to cover items not specified at this level of design effort. A 3% contractors fee has been added, and a 5%/y interest charge has been carried over a 3 year construction period over which the amount of investment increases uniformly. We have assumed a 20 year chemical plant life.

The TMR Fusion Driver covers a range of costs that include the degree of physics complexity from low to high technology as explained in Section 10.3.6. We have added a 35% direct charge and contingency as expected for utility industries⁽³⁾ to construct the TMR Fusion Driver. We have carried a 5%/y interest charge over

Table 10.3-6 Summary of Capital Costs for the Thermochemical Plant
and TMR Fusion Driver - M\$, July 1980

PLANT COMPONENT	BLANKET	
<u>Thermochemical Plant</u>	<u>Heat Pipe</u>	<u>Two-Zone</u>
Direct plus Indirect Capital Costs: ^a		
Section I	89.8	89.8
Section II	94.4	132.6
Section III	575.7	575.7
Section IV	56.6	56.6
Section V	358.2	236.6
TOTAL	<u>1174.7</u>	<u>1091.3</u>
Total Capital Cost: ^b		
Public Utility (100% Debt)	1278.0	1187.3
Private Utility (50% Debt, 50% Equity)	1244.0	1155.7
Chemical Industry (100% Equity)	1202.9	1124.0
<u>TMR Fusion Driver</u>		
Direct Capital Cost: ^c	756.3- 989.6	756.3- 989.6
Direct plus Indirect Capital Cost: ^d	1021.0-1336.0	1021.0-1336.0
Total Capital Cost: ^e		
Public Utility (100% Debt)	1194.6-1563.1	1194.6-1563.1
Private Utility (50% Debt, 50% Equity)	1107.8-1449.6	1107.8-1449.6
Chemical Industry (100% Equity)	1021.0-1336.0	1021.0-1336.0

^a Includes installation, indirect charges, and contingencies.

^b Adds contractor's fee of 3%, and interest during construction for 3 y investment at 5%/y interest on debt capital only.

^c Derived from Henning⁽²⁾ by assuming the TMR Synfuels Driver is equivalent to a 1032 MW_e TMR in cost. An additional 80M\$ is also added to take into account the higher cost of a Synfuel's Blanket.

^d Adds indirect costs of 15% for construction facilities, 15% for engineering, and 5% for owner's costs for a total of 35% indirects costs according to Battelle.⁽³⁾

^e Adds interest during construction for 8 y investment at 5%/y interest on debt capital only.⁽³⁾

an 8 year period where the amount of investment increases uniformly over the period of the construction. These interest charges vary depending on whether the utility is public (100% debt) or private (50% debt, 50% equity), and for chemical industry there is no interest charge since at 100% equity the investment is carried by the chemical firm. We have assumed a 30 year fusion plant life as recommended by Battelle⁽³⁾.

10.4 Hydrogen Production Costs

10.4.1 Plant Availability

We assume the operating personnel of the Thermochemical Plant to consist of a day crew of three chemical engineers, one electrical engineer, five plant operators, one electronics technician, one computer systems-programmer, and one analytical process chemist; and four shift crews with 16 plant operators with the professional staff on call for four hour shifts or longer if required. Staffing of the TMR follows the guidelines set up by Battelle for costing purposes.⁽³⁾

We have taken a 90% stream factor for the Chemical Plant, which takes into account 5% unscheduled and 5% scheduled downtimes. We have assumed that owing to the process unit redundancies that we have designed into the Thermochemical Plant that plant failures can only reduce the stream factor to 80%. This results from the fact that there are at least 5 parallel process trains that are configured in a redundant fashion. One of the redundant trains could be shutdown 50% of the time, without reducing availability below the stated 90% level.

We have taken a 77% TMR fusion plant availability as given by the Battelle report,⁽³⁾ and assumed that this availability results from an 11% fraction of unscheduled shutdowns and 12% of scheduled shutdowns.

The combined overall system availability, therefore, can be calculated from the above information as follows: The unscheduled

availability is 1.00-0.11 or 0.89, for the TMR Driver and 1.00-0.05, or 0.95, for the Chemical Plant, producing a combined availability arising from random shutdowns as the product, which is $0.89 \times 0.95 = 0.846$. The scheduled fraction shutdown must be the larger of that for the TMR Driver or the Chemical Plant; thus, 0.12 for the TMR Driver. The total availability is therefore the product of the scheduled availability times the unscheduled availability, i.e.:

$$A = (1.00-0.12) \times 0.846 = 0.744 \text{ (or 74.4\%).}$$

10.4.2 Estimates of Hydrogen Production Costs

The purpose of this section is to obtain the hydrogen production costs (\$/GJ) from the estimated capital costs in Table 10.3-6, the Chemical Plant operating costs in Table 10.4-1, and the Battelle methods⁽³⁾ for the TMR and electrical power systems. This will involve three different analyses: one based on public utility financing, one on private utility financing, and one on chemical industry financing. Table 10.4-2 summarizes for the Heat Pipe Blanket the computation of H_2 production costs utilizing the three different financing methods, while Table 10.4-3 summarizes the same information for the Two-Zone Blanket.

The computational procedure for both tables is identical. First, we need to separate the capital costs from Table 10.3-6 into the appropriate debt capital and equity capital portions depending upon how these capital cost expenditures are financed. Since the public utility is all debt financed either through prepayments of the rate payers under the justification of "future expansion" or under a utility bond, the capital costs are 100% debt financed. The private utility divides its capital between debt and equity capital (we assume 50% debt and 50% equity), while our model chemical industry takes it as entirely internally (100% equity) financed.

This interest rate is based on constant dollars and is taken from the recommendations of the Battelle report.⁽³⁾ In an inflated

Table 10.4-1 Estimated Operating Cost of the Chemical Plant Sections I-IV
Operating Costs are based on the Capital Cost of an Equivalent Chemical
Plant Constructed of Mild Steel, - M\$, July 1980

	BLANKET	
	Heat Pipe	Two-Zone
<u>Direct Capital Cost (Mild Steel)^a</u>		
Section I	14.2	14.2
Section II	39.3	39.3
Section III	93.4	93.4
Section IV	10.0	10.0
TOTAL	156.9	156.9
<u>Direct plus Indirect Capital Cost</u> <u>for Sections I-IV (Guthrie, 248.2% of above)</u>	546.3	546.3
<u>Annual Operating Cost for Sections I-IV</u> <u>Guthrie, 20%/y of Direct + Indirect</u> <u>Capital Cost)</u>	109.3	109.3

^a The mild steel capital costs for Sections I, II, and IV are from Tables 10.3-1, 10.3-3, and 10.3-4. For Section II, the main mild steel capital cost is for the Multi-Effect Evaporator at \$36.18M. Other Section II costs including redundant units are Drive Pumps \$0.55M, H₂SO₄ Boiler, \$0.14M, Decomposer \$1.20M, Decomposer Recuperator \$0.60M, Decomposer Cooler \$0.30M, and Vapor/Liquid Separator \$0.31M.

Table 10.4-2 Estimates of Operating Costs and Hydrogen Production Costs of a TMR Driven Thermochemical Plant based on the Heat Pipe Blanket - M\$, July 1980

FINANCING METHOD ----->	PUBLIC UTILITY		PRIVATE UTILITY (50% debt, 50% equity)		CHEMICAL INDUSTRY	
	(100% debt)		(100% equity)		(100% equity)	
	Low	High	Low	High	Low	High
TMR DRIVER TECHNOLOGY LEVEL						
<u>TOTAL CAPITAL COST^a</u>						
T/C PLANT	1278.0	1278.0	1244.0	1244.0	1209.9	1209.9
TMR	1194.6	1563.1	1107.8	1449.6	1021.0	1336.0
<u>ANNUAL OPERATING COST</u>						
Financing Cost:						
T/C Plant (5%/y, 20y)	101.1	101.1	50.5	50.5	0	0
TMR (5%/y, 30y)	76.9	100.6	38.4	50.3	0	0
Production Cost: ^b						
T/C Sec. I-IV	109.3	109.3	109.3	109.3	109.3	109.3
T/C Sec. V	7.2	7.2	7.2	7.2	7.2	7.2
TMR	20.4	26.7	20.4	26.7	20.4	26.7
Other Fixed Costs:						
T/C Sec V and TMR ^c	49.7	61.5	96.8	119.3	91.0	111.8
Gross Profit (before taxes):						
10% Profit	0	0	111.5	127.3	223.1	254.6
20% Profit	0	0	223.1	254.6	446.2	509.2
Total Annual Operating Cost:						
10% Profit	364.6	406.4	434.1	490.6	451.0	509.6
20% Profit	364.6	406.4	545.7	617.9	674.1	764.2
<u>HYDROGEN PRODUCTION COST^d</u>						
\$/GJ, 10% Profit	10.72	11.95	12.77	14.43	13.26	14.99
\$/GJ, 20% Profit	10.72	11.95	16.05	18.17	19.83	22.48

^a Total capital costs are taken from Table 10.3-6.

^b Annual production costs for the chemical part of the thermochemical plant (Sections I-IV) are taken from Table 10.4-1. For the TMR and power systems part (Section V), only the operating and maintenance costs are given here using the Battelle estimate of a factor of 2%/y of the direct plus indirect capital costs,⁽³⁾ which are taken from Table 10.3-6. The balance of the TMR and Section V annual costs are given under Other Fixed Costs in this table.

^c Following the Battelle method,⁽³⁾ the Other Fixed Costs for private utility and chemical industry options are estimated as the following percentages of the Total Capital Cost (given in Table 10.3-6): depreciation 1.3%, interim replacement 0.3%, property insurance 0.2%, Federal income tax 2.0%, and State and local taxes 2.8%, for a total factor of 6.6%. For the public utility, we leave out Federal and State income taxes, and estimate local property taxes to be 1.4%, for a total factor of 3.2%.

^d These cost are based on 74.4% plant availability, and 34.0×10^6 GJ of H₂/y.

Table 10.4-3 Estimates of Operating Costs and Hydrogen Production Costs of a TMR Driven Thermochemical Plant based on the Two-Zone Blanket - M\$, July 1980

FINANCING METHOD ----->	PUBLIC UTILITY		PRIVATE UTILITY (50% debt, 50% equity)		CHEMICAL INDUSTRY (100% equity)	
	(100% debt)		(50% debt, 50% equity)		(100% equity)	
	Low	High	Low	High	Low	High
TMR DRIVER TECHNOLOGY LEVEL						
TOTAL CAPITAL COST^a						
T/C PLANT	1187.3	1187.3	1155.7	1155.7	1124.0	1124.0
TMR	1194.6	1563.1	1107.8	1449.6	1021.0	1336.0
ANNUAL OPERATING COST						
Financing Cost:						
T/C Plant (5%/y, 20y)	93.9	93.9	47.0	47.0	0	0
TMR (5%/y, 30y)	76.9	100.6	38.4	50.3	0	0
Production Cost: ^b						
T/C Sec. I-IV	109.3	109.3	109.3	109.3	109.3	109.3
T/C Sec. V	4.7	4.7	4.7	4.7	4.7	4.7
TMR	20.4	26.7	20.4	26.7	20.4	26.7
Other Fixed Costs:						
T/C Sec. V and TMR ^c	45.8	57.6	88.7	111.3	83.0	103.8
Gross Profit (before taxes):						
10% Profit	0	0	107.2	123.0	214.5	246.0
20% Profit	0	0	214.5	246.0	429.0	492.0
Total Annual Operating Cost:						
10% Profit	351.0	392.0	415.7	472.3	431.9	490.5
20% Profit	351.0	392.0	523.0	595.3	646.4	736.5
HYDROGEN PRODUCTION COST^d						
\$/GJ, 10% Profit	10.32	11.53	12.23	13.89	12.70	14.43
\$/GJ, 20% Profit	10.32	11.53	15.38	17.51	19.01	21.66

^a Total capital costs are taken from Table 10.3-6.

^b Annual production costs for the chemical part of the thermochemical plant (Sections I-IV) are taken from Table 10.4-1. For the TMR and power systems part (Section V), only the operating and maintenance costs are given here using the Battelle estimate of a factor of 2%/y of the direct plus indirect capital costs,⁽³⁾ which are taken from Table 10.3-6. The balance of the TMR and Section V annual costs are given under Other Fixed Costs in this table.

^c Following the Battelle method,⁽³⁾ the Other Fixed Costs for private utility and chemical industry options are estimated as the following percentages of the Total Capital Cost (given in Table 10.3-6): depreciation 1.3%, interim replacement 0.3%, property insurance 0.2%, Federal income tax 2.0%, and State and local taxes 2.8%, for a total factor of 6.6%. For the public utility, we leave out Federal and State income taxes, and estimate local property taxes to be 1.4%, for a total factor of 3.2%.

^d These cost are based on 74.4% plant availability, and 34.0×10^6 GJ of H₂/y.

economy as we have today the true interest rate is the 5%/y constant dollar interest rate over and above the current inflation rate.

The annual production cost for the Chemical Plant (Sections I-IV) is obtained directly from Table 10.4-1 based on a 20% fraction of the mild steel equivalent capital cost of the chemical plant. For the power systems (Section V) of the chemical plant and for the TMR, the methods outlined by Battelle⁽³⁾ are followed as detailed in the footnotes to Table 10.8-8. The gross profit is obtained as a simple percentage of the total equity capital.

From the above analyses, we can now obtain the hydrogen production cost. We use as our basis a plant production rate of 4895.9 mol/s H_2 . At a total combined availability of 74.4% (from Section 10.4-1) for the TMR/Thermochemical Plant, we obtain an energy production rate of 34.0×10^6 GJ/y. This energy equivalence of the H_2 is obtained by using 285.77 kJ/mol H_2 as the higher heating value of H_2 plus an added compressive value of energy of RT ln P (9.70 kJ/mol H_2) associated with the 50 atm pressure of the hydrogen product, to obtain a total energy value of 295.47 kJ/mol H_2 . The hydrogen product cost in \$/GJ is obtained by dividing the total annual operating costs (\$/y) by 34.0×10^6 GJ/y.

10.5

CONCLUSIONS ON PRELIMINARY COST ESTIMATES

1. Using conventional costing methods established by the fusion community and chemical industry, we obtain a hydrogen production cost of \$10-12/GJ in July 1980 dollars for both the Heat Pipe and Two-Zone Blanket approaches assuming public utility financing. Private utility and chemical industry financing give higher costs.
2. The \$10-12/GJ hydrogen cost obtained here is very favorable, and indicates that fusion synfuels will be competitive in the timeframe for emergence of fusion in ~2030.

3. The total direct plus indirect capital cost in July 1980 dollars are \$1175M and \$1091M for the Heat Pipe Blanket and Two-Zone Blanket based plants, respectively, and in the range of \$1021M-1336M for the TMR Fusion Driver.
4. Significant reductions in capital costs are anticipated in the future in Section III of the Thermochemical Plant, i.e., in the H1 purification step. Currently, this step is the most capital intensive, accounting for \$576M (~50%) of the Plant cost. A new approach under study at GA for purification of H1 should reduce capital costs and improve cycle efficiency as well for this part of the process.

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