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**LIQUID METAL ENGINEERING ASPECTS OF A COMMERCIAL-SIZED
POWER PLANT BASED ON THE HY LIFE CONVERTER CONCEPT**

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P.O. Box 1449, Canoga Park, CA 91304

LIQUID METAL ENGINEERING ASPECTS
OF
A COMMERCIAL-SIZED POWER PLANT
BASED ON THE HYLIFE CONVERTER CONCEPT

BY

M. J. Hoffman and M. W. McDowell

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I. INTRODUCTION

The Energy Technology Engineering Center (ETEC) is part of a team set up by Lawrence Livermore Laboratories (LLL) to furnish a conceptual design for a commercial-size (~ 1000 MWe) power plant based on inertial confinement fusion. This ETEC report concentrates on the engineering required in the non-fusion portions of such a plant. Perhaps the most promising approach for producing electrical power from thermonuclear fusion is the High Yield Lithium Injection Fusion Energy (HYLIFE) converter. In this concept, a massive fall of molten lithium isolates structural walls from the deleterious effects of a deuterium-tritium pellet undergoing thermonuclear burn. In addition, the lithium fall serves as the tritium breeding source and as a circulating thermal pool from which a small stream flows into the heat transfer circuits. The heat extracted from these circuits eventually boils water to make electricity.

This report begins with a description of the plant energy balance followed by a description of the protective lithium fall circuit and the heat transfer system. The report concludes with discussions on two areas of critical importance to commercial fusion plants: compatibility of structural alloys with lithium, and tritium separation.

II. SUMMARY

A study of a commercial fusion plant based on the High Yield Lithium Injection Fusion Energy (HYLIFE) converter has been performed. A net efficiency of 33.3% was derived for a plant using 2-1/4 Cr - 1 Mo ferritic steel as structural alloy. Use of a thick lithium fall to protect structural materials from the deleterious effects of pellet thermonuclear burn allows the structure to last the life of the plant without replacement. Both mechanical pumps and EM pumps are analyzed for this application. The power requirement for the lithium fall mechanical pumps is ~ 20 MWe. This is a relatively insignificant 1.6% of the gross electrical power output of the plant of ~ 1250 MWe. An EM pump has a greater electrical requirement but the lesser head (NPSH) requirement of an EM pump appears to be a marked advantage since this affects the size of the lithium inventory.

The preferred tritium separation method appears to be that developed by Argonne National Laboratory which involves mixing lithium into an immiscible liquid having a greater affinity for hydrogen isotopes, with subsequent electrolytic separation. The immiscible liquid under consideration is a lithium bromide-lithium fluoride-lithium chloride mixture.

III. ENERGY BALANCE

A. DIAGRAMS

Some of the electricity generated by any power plant is used internally to pump fluids or in other ways that support plant operation. Fusion power plants will use a higher proportion of their generated electricity internally than do existing types of power plants. Cataloging and assessing these in-plant power uses become an important part of the design and evaluation of a fusion power plant concept. This was done for the HYLIFE converter concept (Figure 1). This initial plant energy balance assumes the use of mechanical centrifugal pumps.

A variation from our current base design includes electromagnetic (EM) pumps for lithium fall recirculation pumping rather than mechanical centrifugal pumps. The EM pumps have a disadvantage in their lower efficiency. An energy balance for the HYLIFE converter plant using a type of EM pump is shown in Figure 2. This energy balance shows the effect of the lower efficiency of this pump on the plant net output. The general relationship between the fall recirculation pump efficiency and the net HYLIFE converter plant efficiency is illustrated by the plot in Figure 3.

The bases for the HYLIFE converter plant energy balance are explained in the following paragraphs. For the most part, the design represents a conservative extrapolation of present technology.

B. ASSUMPTIONS ON DRIVER SYSTEM AND HYLIFE CONVERTER CHAMBER

In line with their objectives of investigating the design of a chamber operating with high yield pellets driven by an advanced driver of moderate efficiency, the Livermore group selected the laser and pellet parameters listed in Table 1. These parameters were used as the starting point for our system energy balance. The pellet yield of 2700 MF is distributed between neutrons (68%) and pellet debris plus helium nuclei plus x-ray energy (32%). The neutrons interact with the blanket to give a blanket gain of 1.26. The system operates at a pulse repetition rate of 1 Hz resulting in a fusion chamber output around 3175 MWt.

Figure 1. HYLIFE Converter Fusion Power Plant Energy Balance

Figure 2. HYLIFE Converter Fusion Power Plant Using EM Pumps - Energy Balance

Figure 3. HYLIFE Power Plant Dependence of Net Electrical Efficiency on Fall Recirculation Pump Efficiency

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TABLE 1
LASER AND PELLETT PARAMETERS

Laser Energy	3 MJ
Laser Efficiency	3%
Pellet Gain	900
Pellet Yield	2700 MJ

C. ASSUMPTIONS ON STEAM CYCLE

An essentially state-of-the-art LMFBR heat transfer and electrical generation system has been incorporated into the HYLIFE plant design. The heat transfer system has been divided into four identical quadrants, each having one lithium primary to sodium secondary intermediate heat exchanger (IHx) and three parallel steam generators. Thus, ~3200 MWt of energy transferred from the sodium secondary IHx to the steam system is accomplished in twelve units of 267 MWt each. The steam generators operate in a once-through super-heated steam cycle. They are

assumed to be similar to the hockey-stick modules developed by the Atomic International Division of the Energy Systems Group of Rockwell International for CRBRP. Sodium enters the steam generators at 482°C (900°F) producing steam at the turbine inlet with a temperature of 454°C (850°F) and a pressure of 15.2 MPa (2200 psig). The turbine-generator converts the thermal energy in the steam into electricity with an efficiency of 39%.

D. ASSUMPTIONS ON AUXILIARY POWER REQUIREMENTS

The auxiliary power requirements are the in-plant electricity uses with the specific exceptions of the power required to operate the laser system and that required to pump the protective lithium fall and the lithium primary heat transfer system. To estimate these demands, we have relied on a study of auxiliary power requirements in a commercial LMFBR.⁽¹⁾ The auxiliary power requirements from that study have been ratioed as a fraction of plant gross thermal output and applied to the HYLIFE plant. In addition, an allowance of 0.8 MWe for the tritium removal system has been included. The three largest auxiliary consumers of in-plant power deserve special mention. The largest is the heat removal system. Here power is used to operate pumps that circulate low temperature water through the condenser and then through cooling towers where the heat, picked up in condensing the steam output from the turbine, is rejected to the environment. The second largest is the boiler feed and condensate system. Again, this power is used for pumping. In this case, the use is pumping the very low pressure water in the condenser through the feedwater system piping and feedwater heaters and up to the pressure of the steam generators. The third largest is the pumping power used to circulate sodium around the secondary heat transfer loops.

In most cases, the energy used within the plant is not recovered but most of the energy that goes into pumping the lithium fall, the lithium primary heat transfer fluid, the sodium secondary and the boiler feed and condensate ends up as heat within the fluid and is just as convertible into electricity as the thermal energy generated by the nuclear reaction. We have used a figure of 76.9% as the amount of pumping energy that results in pump heating. This is based on the results of testing the FFTF pump in the Sodium Pump Test Facility (SPTF) at ETEC.

E. ASSUMPTIONS ON LITHIUM PUMPING POWER REQUIREMENTS

The power required to pump the large flow rates of the lithium fall and the much smaller flow rates of the primary heat transfer system have been derived from mathematical models of those systems. These models were used in efforts to optimize the design of the fall recirculation system in terms of lithium volume and power requirements. The models included assumed reaction vessel geometry; piping configuration and friction head loss, pump NPSH requirements and pump inefficiencies.

F. EFFICIENCIES

Table 2 summarizes the HYLIFE plant energy balance in the form of various defined efficiencies. The net electrical efficiency is an economic figure of merit since the cost of electricity generated by the power plant is inversely proportional to this ratio. The electrical conversion efficiency is an environmental figure of merit since one minus this ratio is the fraction of the system energy that must be dissipated to the environment. Comparing the liquid metal system efficiency to the net electrical efficiency gives an indication of the effect of the laser power requirements on plant economics.

TABLE 2
LASER FUSION POWER PLANT -
ENERGY CONVERSION EFFICIENCY

Efficiency Type	Definition	% Efficiency
Net Electrical Efficiency	$\frac{\text{Net Electrical}}{\text{Fusion Chamber Output}}$	33.3
Electrical Conversion Efficiency	$\frac{\text{Gross Electrical Output}}{\text{Gross Thermal Output}}$	39.0
Liquid Metal System Efficiency	$\frac{\text{Net Electrical} + \text{Laser Input}}{\text{Fusion Chamber Output}}$	36.5

G. EFFECTS OF EM PUMPS FOR FALL RECIRCULATION PUMPING

To evaluate the effect on the plant energy balance of using EM pumps for fall recirculation pumping, an energy balance was prepared for a plant using EM pumps having an efficiency of 39.7%. It was assumed that 80% of the energy used

by the EM pumps is eventually transformed to heat in the lithium. This energy balance is presented as Figure 2. The general effect of lower EM pump efficiencies on plant net efficiency is shown by the plot on Figure 3.

At this time the possible efficiency of a very large EM pump is somewhat unsure. We have performed a brief design evaluation, of the helical rotor type of EM pump, which indicates that it is likely to operate with the 39.7% efficiency used in the energy balance on Figure 2. The scaleup from existing pump technology to the sizes required by the HYLIFE converter plant is much larger for an EM pump than for a mechanical-centrifugal liquid metal pump. This makes their use in a plant design more speculative. They may, however, have significant advantages in reducing system lithium volume and the complexities of moving parts and penetrations into the lithium piping.

IV. PROTECTIVE LITHIUM FALL CIRCUIT

The essence of the HYLIFE converter concept is a large volume flow of liquid lithium protecting the solid structure of the converter chamber walls. The considerations associated with providing this flow are presented in this section. The first subsection discusses flow of a protective fall in a jet array geometry within the chamber. The second subsection discusses pump head requirements. The third subsection covers pump hardware and pipe sizes while the final subsection outlines the geometry of the protective fall circuit.

A. VOLUME FLOW, FALL HEAD AND PRESSURE PULSE SUPPRESSION

As presently conceived, the liquid lithium fall is an array of jets with initial diameters of 0.30 meters. A mid-elevation plane showing the jet cross-sections is depicted in Figure 4. The total volume flow of lithium, including the coolant flow required for the chamber structures, is $129 \text{ m}^3/\text{s}$. The height of the fall is 8 m and the inlet velocity is 4.4 m/s which is fast enough to assure that each jet has time to reform and fall between successive fusion energy pulses. The inlet velocity is provided by a static head of lithium in a pool over the nozzle plate as shown in Figure 5. A physical vertical thickness of lithium is used to provide the head rather than either an enclosed pumped circuit or gas pressurization. The enclosed pumped circuit would transmit pressure pulses to the mechanical pumps which could well be excessive. Inert gas pressurization would cause pressure buildup within the vacuum chamber of the HYLIFE converter, eventually interfering with laser beam transmission.

The height of lithium pool required to provide the planned inlet velocity is ~1 meter. This pool is fed by a cascade of many small jets of lithium from an upper pool. The inlet to the chamber from the pumps is above the level of the upper pool in order to prevent the lithium from draining backward through a circuit in the event of a pump failure. Separating the lithium inlets from the upper lithium pool minimizes transmission of pressure pulses from the fusion chamber into the line leading from the discharge side of each pump. The use of a highly porous splash baffle with small jets issuing into a pool at the bottom of the chamber minimizes pressure pulses to the suction side of each pump.

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Figure 4. Plan View of Jet Fall at Vessel Midplane

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Figure 5. Laser Fusion Target Chamber

B. PUMP HEAD REQUIREMENTS AND ELEVATIONS

The pumps must provide the $129 \text{ m}^3/\text{s}$ of lithium to the level at which the liquid enters the HYLIFE vessel. The distance up from the top surface of the pool at the bottom of the chamber below the splash baffle to the center line of the pipes bringing the lithium in at the top of the chamber is 16.15 m (53.0 ft). The pump must supply this head plus friction losses in the lines and exit losses around the protective fall circuit from the surface of the lower pool through the pump and up to the lithium inlets. The pump head requirement in this design is $\sim 23.5 \text{ m}$ (77 ft). The suction side of the pumps require a minimum head of lithium which, since gas pressurization is not deemed feasible, must be provided by an actual height of lithium. Losses on the suction side require an even greater height of lithium than the theoretical value needed to prevent cavitation. In the design discussed here, a total of 5.49 m (18 ft) of lithium is required down from the top surface of the pool at the bottom of the chamber to the centerline of the pipes bringing the lithium into the suction side of the mechanical pumps. Thus, the centerline-to-centerline distance from pump inlet up to converter chamber inlet is 21.64 m (71 ft).

C. FALL RECIRCULATION MECHANICAL-CENTRIFUGAL PUMPS

In the previous subsection, we reported the height of lithium required above the suction side of the pump and discussed the pumps output, $129 \text{ m}^3/\text{s}$ of lithium at a developed head of $\sim 23.5 \text{ m}$ of lithium. In this subsection, we will elaborate on the derivation of these numbers and other pump design parameters.

The primary objective of our investigation of the fall recirculation pump design was to minimize the plant lithium volume. This is important as it reduces the plant tritium inventory, and since the fall recirculation system accounts for about two thirds of the total plant lithium inventory, the most significant volume reductions can be accomplished here. A secondary objective was to reduce the pump power requirements. We restricted ourselves to pump designs that were essentially conventional in hydraulic design and fabricability. This meant limiting the suction specific speed to 12,000 and below, and limiting the impeller diameter to 2.6 m (8.5 ft). Suction specific speeds in excess of 12,000 may be possible with the development of more advanced combined axial-centrifugal pumps

but this is yet to be demonstrated. A limit on the impeller diameter was imposed based on a requirement that the impeller be a casting and that the expected casting size capabilities for ferrous impeller castings within the United States to the year 2000 will be ~ 2.6 m (8.5 ft).

The fall recirculation system pump design effort focused on selecting the specific speed (N_2) and the suction specific speed (S_2) for the fall pumps. The former term describes the shape of the pumps rotating hydraulic elements and the latter term defines the suction requirements that result from the pump geometry. Their proper selection produces the minimum system lithium volume and a reasonable requirement for pumping power, within the limitations imposed.

Three terms that describe the pump characteristics probably need better definition before explaining the selection of a pump design. These terms are specific speed (N_2), suction specific speed (S_2) and net positive suction head (NPSH).

1. Specific Speed (N_s)

The term "specific speed" is used to describe a mechanical pump in terms of impeller shape. An often used diagram relating a numerical value to an impeller shape is shown in Figure 6. Numerically, the specific speed relates to the pump revolutions per minute (N), flow rate in gallons per minute (Q) and developed head in feet (H) by the formula $N_s = N \sqrt{Q}/H^{3/4}$. The highest efficiency impeller shape occurs at an N_s of ~ 2500 . Higher N_s values mean smaller diameter impellers for a given head and capacity. Depending on the suction specific speed, there is a lower limit on N_s before impeller diameter becomes larger than could be cast by a steel foundry in this century. The power used by the pumps increases markedly as N_s increases above 2500.

2. Suction Specific Speed (S_s)

The pump suction specific speed is given by the formula $S_s = N \sqrt{Q}/\text{NPSH}^{3/4}$. This formula is very similar to the one for specific speed, N_s , except that it relates the pump revolutions per minute and flow rate to NPSH rather than to the developed head. Suction specific speed, like specific speed, is dependent on the geometry of the pump's rotating elements. Centrifugal pumps commonly have suction

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Figure 6. Approximate Relative Impeller Shapes and Efficiencies as Related to Specific Speed

specific speeds between 8,000 and 12,000. An axial pump may have a suction specific speed up to 20,000 or 30,000. The higher the suction specific speed, the lower the required NPSH.

3. NPSH

NPSH stands for net positive suction head or the absolute pressure, including dynamic head, in units of feet, at the pump inlet. An NPSH that is too low will cause cavitation to occur within the pump leading to damage and eventual failure. The plant must therefore be able to supply an NPSH to the fall recirculation pumps which will allow safe, cavitation free, long-term pump operation. With a fixed head and capacity, the NPSH required is determined by specific speed (N_s) and suction specific speed (S_s).

The pump hydraulic design was determined with the aid of a mathematical model of the fall recirculation system. This model allowed us to vary pump parameters (in particular, N_s and S_s) in order to examine their effect on the

fall recirculation system volume, pump impeller size and pumping power. Model results are presented in plots as Figures 7 through 14. Figure 7 summarizes much of the results by showing the dependence of recirculation system lithium volume on S_s and N_s and the way in which the 2.6 m (8.5 ft) limit on impeller diameter is a part of the problem. Using this figure, we have selected a N_x of 2725 and an S_s of 11000 as the optimum values for minimizing the system volume while remaining close to the maximum pump efficiency. But as can be seen on the figure, only very slight penalties are incurred by small variations from the optimum point.

D. GEOMETRY OF THE FALL RECIRCULATION SYSTEM

The fall recirculation system consists of 16 individual loops which bring lithium from the bottom of the reaction chamber to the top. Each loop consists of one pump and 1.22 m (48 in.) internal diameter piping with bellows to mitigate the thermal expansion stresses in the piping. The 16 loops are grouped into four cells of four loops each. A small flow from each of the four fall recirculation loops within a cell is diverted to a primary transfer loop. The number of pumps was chosen to (1) result in pumps of manufacturable size, yet retain the cost advantages of scale, (2) allow the pumps to be clustered closely around the reaction vessel, and (3) be divisible by four to correspond to the four heat transfer loops. Piping size was chosen to provide a flow velocity close to 6 m/s (20 ft/s), and it will actually be 6.9 m/s (23 ft/s). Six meters per second is postulated to be the optimum liquid metal velocity that will result in minimal erosion over a long plant life. The configuration of the lithium fall recirculation system is best seen in Figure 15.

Figure 7. Suction Specific Speed as a Function of Specific Speed

Figure 8. Fall Circuit Piping Volume as a Function of Specific Speed

Figure 9. Pump Impeller Diameter as a Function of Specific Speed

Figure 10. Fall Pumping Power as a Function of Specific Speed

Figure 11. Net Positive Suction Head (NPSH) as a Function of Specific Speed

Figure 12. Developed Pump Head as a Function of Specific Speed

Figure 13. Distance Between Free Surface at Bottom of Chamber Down to Pump Inlets as a Function of Specific Speed

Figure 14. Pump Revolutions per Minute (rpm) as a Function of Specific Speed

Figure 15. Primary Steam Supply System Plot Plan

V. HEAT TRANSFER SYSTEM

The heat transfer system consists of three elements: (1) the primary system which transfers heat from the reaction vessel-fall recirculation system to the secondary system and uses lithium as the transport fluid, (2) the secondary system which transfers heat from the primary system to the steam generators and uses sodium as the transport fluid, (3) the steam and feedwater system which functions as the actual power cycle (a superheated steam rankine cycle) for the production of electricity with, of course, H_2O as the working fluid. The secondary sodium loop is necessary because it provides a barrier to the consequences of a light metal - water reaction with a radioactive liquid metal and facilitates hydrogen removal from sodium through cold trapping. Tritium can be easily cold-trapped from sodium to a concentration of <1 ppm by weight in the circulating liquid, whereas tritium in cold-trapped lithium is over two orders of magnitude higher in parts per million (ppm) weight in the circulating liquid. The diffusion of tritium into the water is not the issue here since the partial pressure of tritium above these cold-trapped liquids is about the same. The important consideration is the amount of tritium that would be within the alkali metal exposed to water if a steam generator failure occurred. Another point in favor of a sodium secondary system is that the technology of sodium system modular steam generators is quite advanced, including construction and operation of large facilities for studying methods of handling sodium-water reactions. If similar facilities had to be built to study lithium-water reactions, many years and millions of dollars would have to be expended on a problem that is quite avoidable if a sodium secondary system is used.

The thermohydraulic characteristics and some pipe diameters of the heat transfer system are shown in Table 3. All pipe elbows within the plant heat transfer system will be standard long-radius elbows which have a turn radius to pipe centerline equal to 1.5 pipe diameters.

TABLE 3
LASER FUSION POWER PLANT
HEAT TRANSFER AND ELECTRICAL CONVERSION SYSTEM

Lithium Primary — Four Parallel Loops (each loop from four pumps)		
Hot Leg Temperature	500°C	(932°F)
Cold Leg Temperature	343°C	(650°F)
Flow Rate per Loop (Total)	2.6/10.2 m ³ /s	(40,500/162,000 gpm)
Piping ID	0.71	(28 in.)
Pumped by Full Recirculation Pumps	0.71 m	
Sodium Secondary — Four Parallel Loops		
Hot Leg Temperature	482°C	(900°F)
Cold Leg Temperature	316°C	(600°F)
Flow Rate per Loop (Total)	5.0/20.1 m ³ /s	(79,500/318,000 gpm)
Piping ID	0.91 m	(36 in.)
Intermediate Pump Head	119 m	(390 ft)
Steam System — Superheated		
Twelve Once-Through Steam Generators		
Turbine Inlet Steam Pressure	15.2 MPa	(2200 psig)
Turbine Inlet Steam Temperature	454°C	(850°F)
One Turbine-Generator	1200 MWe	

VI. COMPATIBILITY OF STRUCTURAL ALLOYS WITH LITHIUM

Two alloy types of interest to laser fusion liquid metal systems are austenitic stainless steels and low-chromium ferritic steels. Austenitic stainless steels are made up of crystals with a face-centered-cubic crystal structure with carbides scattered around the crystals and often along crystallographic planes within the crystals. The face-centered-cubic structure implies a ductile alloy, a low diffusion rate for hydrogen isotopes, and relatively good creep resistance. The existence of intergranular carbide can mean severe concentration gradients of the carbide-forming elements within the crystals near the carbides. The structure of ferritic steels is very much a function of the heat treatment that the alloy experienced. One typical structure is a mixture of body-centered-cubic crystals with masses of alternating lamellae of iron carbide and body-centered-cubic plates. Another structure is composed of body-centered-cubic crystals interspersed with spheres of carbides. Each ferritic steel structure may behave differently with respect to lithium compatibility and hydrogen isotope permeability. In general, the low-chromium ferritic steels have relatively high short-time strength properties at moderate temperatures, relatively low creep resistance, and high hydrogen permeability.

A. AUSTENITIC STAINLESS STEELS

Experiments at Oak Ridge and Colorado School of Mines have shown that austenitic stainless steels were dissolved at a greater rate than was a stabilized grade of 2-1/4 Cr - 1 Mo steel.^(2,3,4) Austenitic stainless contains roughly 18% Cr and 8% Ni. Chromium is an element that may be difficult to obtain in quantity in the U.S. by the turn of the century and any nickel dissolved in lithium and flowing through a high neutron flux can form undesirable radioactive products. Furthermore, austenitic stainless steels can be attacked intergranularly under certain conditions of nitrogen contamination and temperature. Work at the Colorado School of Mines has indicated the following relationship is useful in estimating intergranular penetration, P , of Type 304L stainless steel in the temperature range of interest (Figures 16 and 17).

$$P \text{ (in mm)} = \left[4000 (N) e^{-(31000/RT)} t \right]^{1/2},$$

ETEC-39-451

Figure 16. Grain Boundary Penetration
of Type 304 L Stainless Steel

ETEC-39-452

Figure 17. Grain Boundary Penetration at 500°C
as a Function of Nitrogen Content

for 21 years of continuous operation at 500°C,

$$P \text{ (in mm)} = 1.08 (N)^{1/2},$$

where

(N) = wt % nitrogen in the lithium

R = 1.98

T = °K

t = h

If a tube wall is to end up 0.65 mm thick after 21 years (30 years at 70% operation of flowing lithium), and the nitrogen content is kept down by periodic hot trapping to 0.0080 wt % (~80 ppm) then the original wall thickness, from grain boundary penetration considerations alone, would have to have been 0.75 mm.

The Colorado School of Mines also presents a formula for weight loss, W, of Type 304L stainless steel in lithium (Figures 18 and 19)

$$W \text{ (in gm/mm}^2\text{)} = \left[22.3 \times 10^{-8} (N)^{2.5} e^{-(17000/RT)_t} \right]^{1/2},$$

or, under the same conditions as above

$$W \text{ (in gm/mm}^2\text{)} = 7.84 \times 10^{-4} (N)^{1.25} = 0.188 \times 10^{-5} \text{ gm/mm}^2 \text{ when } (N) = 0.008\%.$$

Since the area of the lithium side of an IHX for a HYLIFE power plant will be 10,000 m² (10¹⁰ mm²) the calculated weight of dissolved elements from Type 304L stainless steel would be 18.8 kg. Since the lithium is assumed to be continuously cold-trapped, the liquid metal will not saturate with time. Since nickel is the main element leached out by lithium, we have calculated the weight of nickel in a square millimeter per micron of depth.

$$W_{Ni} = (0.08)(8)(10^{-1})(10^{-4}) = 0.64 \times 10^{-6} \text{ gm Ni/micron.}$$

This indicates that a fairly thick layer (several microns) of nickel-free material would be created on a Type 304L stainless steel surface in lithium with

Figure 18. Weight Loss of Type 304L Stainless Steel in Lithium

Figure 19. Weight Loss of Type 304L Stainless Steel in Lithium at 500°C as a Function of Nitrogen Content

80 ppm N. This nickel depletion would cause the surface to transform to ferrite, a body-centered-cubic structure. The conversion of the surface to a nickel-free ferrite would probably slow down the corrosion rate of the stainless steel after extended periods in lithium. On the other hand, experimental data are from static tests and flowing lithium should dissolve stainless steel at a significantly higher rate than does static lithium. Theoretical corrosion rates for stainless steel plotted in Figures 17 and 18 are for static systems.

B. LOW CHROMIUM FERRITIC STEELS

Experiments on a stabilized grade of 1-1/4 Cr - 1 Mo have indicated that the corrosion is negligible on wrought 2-1/4 Cr - 1 Mo alloy stabilized with columbium (niobium), both in the bulk alloy and in the heat affected zones by welds.

The columbium stabilized 2-1/4 Cr - 1 Mo alloy usually has approximately 0.5% Ni added to improve the weldability of the metal. Unfortunately, this nickel is bad for the induced radioactivity characteristics of the alloy.

Recent experiments have also shown that wrought unstabilized 2-1/4 Cr - 1 Mo alloy has a very low corrosion rate. Heat affected zones with unstabilized 2-1/4 Cr - 1 Mo steel can, however, suffer from marked intergranular attack within a narrow region surrounding the fusion zone of a weld. These heat-affected zones are characterized by high residual stresses, by distinctive carbide configurations, and by unique intergranular phases in addition to carbides. Experiments are in progress to define which of these characteristics or combination of characteristics can result in intergranular attack. A post-weld stress relief heat treatment of 10 h at 760°C has eliminated any significant intergranular attack in heat affected zones in lithium corrosion experiments performed to date at the Colorado School of Mines. Such a heat treatment not only relieves high residual stresses but also changes carbide stoichiometry from the carbides that are unstable in lithium (M_3C and M_2C) to the stable carbides M_7C_3 , $M_{23}C_6$ and M_6C . There is even a possibility that this heat treatment can affect subsequent formation of noncarbide intergranular phases after prolonged exposure at 500°C. At present, we believe the unstabilized grade of 2-1/4 Cr - 1 Mo steel is the optimum choice for the bulk of the HYLIFE converter liquid metal system if creep-inducing stresses can be kept sufficiently low in the design.

C. LIQUID METAL EMBRITTLEMENT

When corrosion is insignificant, liquid metal embrittlement becomes a consideration. This phenomenon is easily avoided when the circumstances giving rise to such severe liquid metal effects are known. A quick review of severe liquid metal effects is presented in Tables 4, 5, and 6. A search for the appearance of this phenomenon is being performed at Colorado School of Mines on unstabilized 2-1/4 Cr - 1 Mo alloy. Stress state, heat treatment, lithium interstitial element content and residual impurities in the ferritic steel are the main considerations in evaluating liquid metal embrittlement for 2-1/4 CR - 1 Mo in lithium. We will evaluate this phenomenon in future work.

TABLE 4
SEVERE LIQUID METAL EFFECTS

Grain Boundary Penetration (Diffusion Process)
Rate Predictable by Equation
(Li with high N content on stainless)
Liquid Metal Embrittlement
Under Highly Specific Conditions (can be catastrophic)
(Li on 4130 low alloy steel)

TABLE 5
REASONS FOR LIQUID METAL EMBRITTLEMENT

<p>Surface Tension Characteristics Force Liquid Between Grains</p> $\gamma_{SS} > 2\gamma_{SL}$ <p>θ is angle such that $\gamma_{SS} = 2\gamma_{SL} \cos \theta$</p>			
Stress Concentration Site Cannot be handled

Plastic Deformation Relieves Stress	Dissolution Blunts Crack	Neither Plastic Deformation nor Dissolution Occur	

TABLE 6
REASONS WHY LIQUID METAL INHIBITS PLASTIC DEFORMATION

Injects atomic hydrogen or nitrogen into metal which concentrates in highly stressed area at tip of crack.

Attacks carbides to give extremely narrow radius stress raiser.

VII. TRITIUM SEPARATION

A. INVESTIGATION OF CONCEPTS

We are investigating the following concepts for their applicability to laser fusion plants:

- 1) Permeation through the 2-1/4 Cr - 1 Mo primary heat transfer loop tubes into a sodium or NaK secondary system, with subsequent cold trapping.
- 2) Permeation through columbium alloy tubing into a vacuum or inert gas.
- 3) Mixing the lithium into an immiscible liquid having greater affinity for hydrogen isotopes, with subsequent electrolytic separation.

The first two of the above methods have very serious shortcomings. A discussion of each of these concepts is presented here. The third method is being studied by a group of people at Argonne under the direction of V. Maroni.

1. Permeation Through the 2-1/4 Cr - 1 Mo Primary Heat Transfer Loop Tubes

A liquid metal heat exchanger requires ~ 3 to 8.0 m^2 of area per thermal megawatt in the temperature range we are considering. For 1060-MWe net electrical output, our thermal balance shows that the heat transfer surface must accommodate $\sim 3200 \text{ MWt}$. Thus, our available area is, at minimum, $\sim 10,600 \text{ m}^2$. Wall thicknesses between 0.5 and 1.5 mm are typical for this application. We will assume the lithium must absorb 1.6 kg per day of 33 at.% deuterium plus 67% tritium mixture (400 gm deuterium and 1200 gm tritium). This amount corresponds to $4.48 \times 10^6 \text{ cm}^3 \text{ (STP)}$ of tritium and $2.24 \times 10^6 \text{ cm}^3 \text{ (STP)}$ of deuterium of a total of $\sim 6.7 \times 10^6 \text{ cm}^3 \text{ (STP)}$ of hydrogen isotope each day. The flux of diffusing gas through a metal at a specific temperature is given by the following formula, providing that the gas pressure is in a range such that the $\sqrt{P}$ relationship is valid:

$$\text{Flux } [\text{cm}^3 \text{ (STP)/h}] = \frac{C A (\text{in cm}^2) (\Delta \sqrt{P} \text{ (in atmos)})^{1/2}}{t (\text{mm})},$$

where the permeability constant C is in units of $\text{cm}^3 \text{ (STP)} \cdot \text{mm/h} \cdot \text{cm}^2 \cdot \text{atm}^{1/2}$.

We need the tritium flux to be $4.8 \times 10^6/24$ or $\sim 1.9 \times 10^5 \text{ cm}^3 \text{ (STP)/h}$. For a 4000 Mwt plant, the intermediate heat exchanger area will lie between 10.6×10^7 and $25.6 \times 10^7 \text{ cm}^2$. We will assume our 2-1/4 Cr - 1 Mo tubes have a thickness between 0.65 and 1.30 mm.

We can solve for $C(\Delta\sqrt{P})$:

$$C(\Delta\sqrt{P}) \approx \text{flux (thickness/area):}$$

$$0.0005 < C(\Delta\sqrt{P}) < 0.0023.$$

There is an equation that gives us an order of magnitude value for C appropriate to tritium diffusing through 2-1/4 Cr - 1 Mo: ⁽²⁾

$$C = 88.1 e^{-9350/RT}.$$

$$\text{At } 500^\circ\text{C}, C = 0.20 \text{ cm}^3 \text{ (STP)} \cdot \text{mm/h} \cdot \text{cm}^2 \cdot \text{atm}^{1/2}.$$

Plugging such a value into the $C(\Delta\sqrt{P})$ relationship shown above, we obtain the following:

$$0.0025 < \Delta\sqrt{P} < 0.0115 \text{ in (atmospheres)}^{1/2}.$$

If the low pressure side is considered to have essentially a zero tritium pressure, we obtain

$$0.005 < P \text{ tritium} < 0.101 \text{ torr.}$$

If we assume that the relationship between tritium pressure and parts per million shown in Figure 20 is accurate, such a pressure of tritium corresponds to between 1000 and 10000 ppm tritium in lithium. At first look, then, this huge concentration would be required to diffuse away the incoming tritium at a rate that would result in a steady-state tritium concentration. Such a solution would require that a system be designed to allow a huge tritium burden in the primary lithium. Cold trapping of the alkali metal in the secondary system would minimize

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Figure 20. Relationship Assumed Between Tritium Pressure and Parts per Million (ppm) Tritium in 500°C Lithium

tritium diffusion into the water in the steam generator, while an evacuated vessel around the whole chamber and primary system would keep tritium from escaping into the environment. Tritium would be recycled from both the secondary system cold traps and the evacuated outer vessel.

Even though very high levels of tritium are being discussed in the above analysis, cold trapping of the lithium itself cannot be used to help control the tritium concentration to ~ 1 ppm in the primary lithium system. If all the hydrogen being trapped were tritium, 500°C lithium could be cold trapped to a pressure of ~ 0.113 torr.* If all the hydrogen being trapped were protonium, the lithium could be cold trapped to a pressure of 5×10^{-3} torr (Figure 8).⁽⁶⁾ Thus, cold trapping of lithium appears less advantageous than diffusion through an intermediate heat exchanger followed by cold trapping of the liquid in the secondary system. If the secondary system is sodium, the tritium pressure can be cold trapped to $\sim 3 \times 10^{-5}$ torr⁶ on the low pressure side which can be considered essentially the zero assumed in the calculation shown above. Since we are seeking a steady-state value between tritium coming into lithium and leaving via the IHX walls rather than the equilibrium value achievable if no tritium were entering the lithium, we see no great advantage in using NaK as the secondary fluid even though NaK can be cold trapped to $\sim 2 \times 10^{-10}$ torr of tritium pressure.

This leads to consideration of a scheme where a portion of lithium is removed from any spot where tritium can be bred or injected into it and allowed to equilibrate across a wall with sodium or NaK at 500°C . In these cases, the tritium contents could approach a value associated with 3×10^{-5} torr using sodium; namely, ~ 400 ppm, or 2×10^{-10} torr using NaK, which is ~ 1 ppm tritium.

Finally, a concept based on supplying sufficient area of 2-1/4 Cr - 1 Mo tubing to allow a steady-state concentration of 10 ppm tritium in the lithium with NaK on the low tritium pressure side is of interest. In this case, the tritium pressure in the lithium is 10^{-7} torr or 1.3×10^{-10} atmospheres, while

*The phase diagram relationships between lithium and hydrogen isotopes are those used in the classic paper by Natesan and Smith.⁽⁶⁾ Recent papers by Katsuta, et al, and Veleckis,⁽⁸⁾ et al, indicate the solubility of H in lithium to be ~ 60 ppm at cold trap temperatures rather than the ~ 1800 ppm calculated by Natesan and Smith (Figure 21).

Figure 21. Lithium-Hydrogen Phase Diagram

the tritium pressure in the cold trapped NaK is 2×10^{-10} torr or 26×10^{-14} atmospheres. Thus,

$$\Delta\sqrt{P} = \sqrt{1.3 \times 10^{-10}} - \sqrt{26 \times 10^{-14}} = 109 \times 10^{-7} \text{ (atm)}^{1/2}$$

$$\text{thickness/area} = \frac{(0.20)(109 \times 10^{-7})}{1.9 \times 10^5} = 11.47 \times 10^{-12} \text{ mm/cm}^2$$

Assuming a thickness between 0.65 and 1.4 mm,

$$57 \times 10^9 < \text{area} < 113 \times 10^9 \text{ in cm}^2$$

An area of $5.7 \times 10^6 \text{ m}^2$ is over 100 times the normal area of an IHX. This huge area is awkwardly large for a plant, but not totally out of the question.* Further, a thorough investigation of hydrogen isotope permeability through various heat treatments of 2-1/4 Cr - 1 Mo immersed in alkali metal on both sides may result in a significantly higher permeability constant. The permeability constant utilized here was derived from experiments with alkali metal on the high pressure side only.^(5,6)

2. Permeation Through Columbium Alloy Tubing Into Vacuum or Inert Gas

Data from Webb,⁽⁹⁾ et al, (Figure 22) allowed us to construct Figure 23 which compares the required area for columbium (niobium) in terms of the area required for a BCC iron base alloy. One serious drawback in utilizing this comparison is the assumption that both alloys have permeabilities that are a function of the difference in the square root of the pressure between the high and low tritium surfaces of a wall. In fact, both alloys may pass through a regime where the permeability becomes a function of pressure to the first power. Another problem is that data are very sensitive to oxides on the low pressure face. An additional confusing factor occurs when the rate-controlling factor for tritium transport becomes the rate at which tritium atoms skating about on the low pressure surface find another atom and leave the surface as a gaseous molecule.

The figure showing the area ratio for columbium versus iron is based on the curve for columbium, also based on the Webb report. Note the abrupt change in permeability around 475°C.

Based on the area calculated for 2-1/4 Cr - 1 Mo alloy tubing of 0.65 mm thickness at 500°C, a similar wall of columbium alloy would be:

$$(5.7 \times 10^6 \text{ m}^2)(0.0022) = 12,540 \text{ m}^2 \text{ at } 1000^\circ\text{C}$$

and

$$(5.7 \times 10^6 \text{ m}^2)(0.0036) = 20,520 \text{ m}^2 \text{ at } 500^\circ\text{C}.$$

*For example for a given total flow rate, tubes could be one-tenth the radius and coiled to give 10 times the length of a linear segment.

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Figure 22. Permeability of Tritium Through Columium

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Figure 23. Comparison of Required Areas for a Columium Alloy System vs a Cooler Iron Alloy System

Work by Alire at Los Alamos and Livermore indicates⁽¹⁰⁾ that the temperature sensitivity of hydrogen isotope permeation through columbium may be more temperature sensitive than this treatment shows. The formula that corresponds to data read off curves from the Webb paper for columbium is

$$C = 408 e^{-(1018/RT)} \text{ cm}^3 (\text{STP}) \cdot \text{mm/h} \cdot \text{cm}^2 \cdot \text{atm}^{1/2},$$

whereas the data of Alire has a far higher activation energy than the 2018 calories/gm-mil indicated here. Natesan and Smith,⁽⁶⁾ however, extract from the literature a low activation energy:

$$C = 1.05 \times 10^{-2} e^{-(3430/RT)} \text{ in cm}^3 (\text{STP}) \cdot \text{mm/s} \cdot \text{cm}^2 \cdot \text{torr}^{1/2},$$

which we convert to our units:

$$C = 1042 e^{-(3430/RT)} \text{ cm}^3 (\text{STP}) \cdot \text{mm/h} \cdot \text{cm}^2 \cdot \text{atm}^{1/2}.$$

A low activation energy implies that lithium does not have to be heated to high temperatures for diffusion through the columbium walls.

Although the relative areas involved indicate that a columbium-alloy tritium diffuser might be cheaper than 100 times the areas of typical IHX units built of 1-1/4 Cr - 1 Mo, the scarcity of columbium might call this approach into question. The weight of columbium alloy involved is in the range of 56,000 kg per 1000 MWe net electric plant with a 0.65 mm thickness of tube. Halving this thickness means halving the area, or a one-fourth reduction in weight.

If this weight is too great a consumption of columbium, vanadium alloys begin to appear to be attractive. In particular, a vanadium alloy with an internal oxygen getter such as hafnium or zirconium may have to be developed for this application.

3. Immiscible Liquids

Mixing lithium into an immiscible liquid having greater affinity for hydrogen isotopes with subsequent electrolytic separation is being studied intensively by Argonne. This project is being funded by Magnetic Confinement Fusion interests.

A loop has been constructed by Argonne specifically to establish data required to evaluate this concept. The energy consumption and space requirements of this concept are very small. At present, this concept appears the most promising.

VIII. CONCLUSIONS

ETEC has, at LLL request, evaluated the liquid metal engineering problems peculiar to the HYLIFE converter concept, i.e., a concept involving a very large lithium flow compared to other fusion power plant schemes. We found that this concept does not engender any particularly difficult liquid metal problems. Energy requirements for providing the thick protective wall of falling molten lithium are a rather minor fraction of plant output. Components for pumping the large flow of lithium will take years of development, but similar liquid metal experience in the LMFBR program should allow confidence that estimates of costs and development times will be accurate.

A secondary heat transfer circuit has been included in this study, specifically a sodium secondary system. We realize the temptation to eliminate the secondary system for economic reasons, but believe safety considerations require a non-lithium secondary system be employed. The reactions of steam jetting from a ruptured tube into molten sodium in the shell side of a steam generator have been well characterized in the LMFBR program and found to be quite mild. The reaction between steam and molten lithium is not expected to be mild. Another safety consideration is the tritium at an ~ 1 ppm level in lithium. A spill of lithium-water reaction would release the dissolved tritium.

This study indicated that certain development efforts should be initiated. A major pump development program should be started soon. The hydraulics of fall stability needs to be studied in a water flow facility capable of duplicating the Reynolds Number and geometry of the actual flow planned for the HYLIFE converter. Lithium interactions with ferritic steel weldments are an area that needs intensive and continuing research. Tritium recovery from lithium is another area where intensive effort is required. Although excellent work involving molten lithium halide contact followed by electrolysis is underway by Maroni et al, additional work in the area of hydrogen isotope diffusion through a body-centered-cubic alloy should be undertaken.

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