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**ORNL Fusion Power Demonstration Study:
Lithium as a Blanket Coolant**

W. M. Wells

OAK RIDGE NATIONAL LABORATORY
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FUSION ENERGY DIVISION

ORNL FUSION POWER DEMONSTRATION STUDY:
LITHIUM AS A BLANKET COOLANT

W. M. Wells

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CONTENTS

	<u>Page</u>
LIST OF SYMBOLS	v
ABSTRACT	vii
1. INTRODUCTION	1
2. THE PREFERRED APPROACH — THE TUBE BUNDLE BLANKET	2
2.1 Calculational Methods for Liquid Metal MHD Flow in Uniform Magnetic Fields	2
2.2 Estimation Methods Involving Perturbations of Flow and Magnetic Field	3
2.3 Basic Quantities and Duct Routing Considerations	4
2.4 The Tube Bundle Blanket	5
2.5 Variations on the Tube Bundle Blanket	9
3. RELATED MATTERS REGARDING LIQUID METALS	11
3.1 Methods for Mitigation of MHD-Induced Pressure Stresses	11
3.2 Heat Transfer Considerations	12
3.2.1 Heat Transfer in Laminar Flow	12
3.2.2 Nusselt Number for Use with Helium in First Wall Tubes	12
3.2.3 Entry Length Considerations	13
3.2.4 Twisted Tape in First Wall Tubes	13
3.2.5 Reduced Cyclic Thermal Stress in Inner (6-cm) Lithium Tubes	13
3.3 Comparison of Lithium and Other Coolants	13
3.3.1 Boiling Potassium or Cesium as a Coolant	13
3.3.2 Comparison with Sodium and with Helium	13
3.4 Stagnant Regions and their Consequences	14
4. CONCLUSIONS	17
APPENDIXES	19
A.1 Other Blanket Designs	19
A.1.1 Cell Construction	19
A.1.2 Lithium in Immersed Round Tubes	20
A.1.3 Gravity-Induced Lithium Flow	22
A.2 Duct Routing for the Tube Bundle Design	22
A.3 Derivation Relative to the Advantage of Tapering the Wall Thickness in Transverse MHD Flow	23
A.4 Determination of Diametral ΔT for Laminar Flow	25
A.4.1 Method for Determining Temperature Distribution	25
A.4.2 Nusselt Number Determination	25
A.5 ASME Code Treatment and Outline of Detailed Calculations	27
A.5.1 Tube Bundle Blanket with First Wall Composed of 1.5-cm-diam Tubes	28
A.5.2 Helium-Cooled First Wall Composed of 3-cm Tubes with 1-mm Wall	29
A.5.3 The Case Where the First Wall Consists of Single Tubes 3 cm in Diameter with Flowing Lithium	30
A.5.4 Justification for Eq. (A.5.6) for the First Wall Tubes	30
REFERENCES	31

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LIST OF SYMBOLS

a	radius of round tube or half-width of flat passage
A	area
b	dimensionless constant
B	magnetic field strength
$C \equiv \sigma_w t / \sigma a$	conductance ratio
c_p	specific heat
d	hydraulic diameter
e	channel width (A.4)
D	drag force per unit length
j	electrical current density
k	thermal conductivity
L	length
$M \equiv Ba\sqrt{\sigma/\eta}$	Hartmann number
$N \equiv B^2 \sigma a / \rho v$	interaction parameter
$Nu \equiv hd/k$	Nusselt number
p	pressure
$Pr \equiv c_p / \eta k$	Prandtl number
$\dot{q}''$	heat flux through a unit area
$\dot{q}'''$	heating rate per unit volume
$Re \equiv v d \rho / \eta$	Reynolds number
s	stress
t	wall thickness
v	fluid velocity
w	dimensionless velocity
$\left. \begin{matrix} x \\ y \\ z \end{matrix} \right\}$	position coordinates; z is the flow direction
α	thermal diffusivity
β	ratio of plasma pressure to magnetic pressure
η	viscosity
ρ	density
σ	electrical conductivity
θ	angle

Differential quantities are obvious except for these:

ΔT_d	temperature difference across a tube diameter
ΔT_f	temperature difference above mixed mean temperature
ΔT_r	temperature rise of a flowing stream
$\Delta \phi$	electrical potential difference
Δh_e	enthalpy change for evaporation

Subscripted quantities:

$B_\perp$	component of magnetic field normal to the flow direction
s_d	stress due to temperature difference across a tube diameter

s_p	stress due to pressure
s_w	stress due to temperature difference across a tube wall
σ_w	electrical conductivity of wall
T_m	mixed mean temperature

ABSTRACT

Recent reassessment of tokamak reactors which move towards smaller size and lower required field strength (higher β)¹ change the picture regarding the magnitude of MHD effects on flow resistance for lithium coolant. Perhaps the most important consequence of this as regards use of this coolant is that of clear acceptability of such effects when the flow is predominantly transverse to the magnetic field. This permits defining a blanket that consists entirely of round tubes containing the circulated lithium with voids between the tubes. Required thermal-hydraulic calculations are then on bases which are well established, especially in view of recent results dealing with perturbations of ducts and magnetic fields. Mitigation of MHD effects is feasible through tapering of tube wall thickness or use of insulated layers, but neither scheme was mandatory for the assumed conditions. Blanket configurations utilizing flowing lithium in round tubes immersed in static lithium may be suitable, but calculational methods do not now exist for this situation. Use of boiling potassium or cesium appears to be prohibitive in terms of vapor flow area when temperature levels are consistent with the use of stainless steel. Liquid sodium, in addition to not being a breeding material, requires higher velocity than lithium for the same heat removal. Consequences of stagnant regions, which are sometimes found where flow perturbations exist, are discussed.

1. INTRODUCTION

The recent years have witnessed a variety of blanket design concepts, including several choices of structural material and blanket fluids. There is a growing trend to the effect that, at least for a demonstration power plant, near-term technologies need to be emphasized, and, in particular, that the designs should be based on minor modification of existing stainless steel technology. This constrains the thermodynamic cycle to have peak temperatures which are somewhat higher than the current commercial light water reactors. From the early power reactor studies, it has generally been accepted that wall loading values of $\sim 1 \text{ MW/m}^2$ would likely lead to commercially unacceptable power plants. Thus, following the *ORNL Fusion Power Demonstration Study: Interim Report*,¹ the reference wall loading has been taken here to be $\sim 3.1 \text{ MW/m}^2$ with peak material temperature of $\sim 500^\circ\text{C}$.

The acceptability or popularity of blanket breeding and coolant fluids has fluctuated substantially, with lithium having been replaced by helium as the favorite coolant. The shift away from lithium as a coolant seems to have arisen substantially out of despair at realizing that the MHD effects are indeed strong and not well understood. Recently, there has been a resurgence of interest in lithium as a coolant coupled with an excellent beginning in dealing specifically with some of the necessary details of ducting in and around blankets.² There are three obvious potential advantages which can be considered major:

- (1) The heat is generated largely in the lithium, and if stagnant regions are avoided, the lithium can be withdrawn at very nearly the peak temperature in the blanket. Compared to helium-cooled blankets, this can result in an increase in the peak cycle temperature of 60°C or greater, which can raise cycle efficiency 7% or so.
- (2) Related to the situs of heat generation and disregarding the region constituting the first wall, there is the likelihood that cyclic thermal stress can be substantially eliminated by stopping the flow during the power-off portion of the cycle.

- (3) Compared to stagnant-lithium-based blankets, tritium removal is rendered easier and more certain because all of the lithium is circulated.

The organization of this paper is principally that of providing descriptions of design concepts and details aimed at the technical feasibility question with supporting sections and appendixes. The dominant theme is to address the technical feasibility issues directly related to the peculiar nature of liquid metal MHD and consequences thereof — in particular:

- (1) pressure drops and consequent pressure stresses;
- (2) basic heat transfer considerations, especially those arising from the laminar flow constraints and stagnant region effects; and
- (3) sufficient stress analysis, following the ASME Code, to deal with feasibility.

Use of Section III of the ASME Code for stress analysis (intended for nuclear power plant components) provides a widely accepted, conservative approach which deals with the combined effects of pressure (primary) stresses and cyclic thermal (secondary) stresses including explicit treatment of ratcheting due to these combined effects. Treatment of cyclic thermal stress fatigue is mandatory because, in terms of material properties, the strain range as a function of cycles to failure for type 316 stainless steel varies by two orders of magnitude over the range of 10 to 10^6 cycles.

In a broader sense, it is the writer's view that satisfaction of the above three criteria is a mandatory prelude to calling any concept a "design." Given a configuration and basic heating criteria, if these conditions are not met, there is no fix. Once these criteria are satisfied, one can apply ingenuity to fabrication and maintenance problems. The reverse is not sensible.

In addition, attention is given to aspects of liquid metal application technology which are not directly related to the design concepts described.

2. THE PREFERRED APPROACH — THE TUBE BUNDLE BLANKET

2.1 CALCULATIONAL METHODS FOR LIQUID METAL MHD FLOW IN UNIFORM MAGNETIC FIELDS

An excellent review of the methods for calculating pressure gradients is found in Ref. 3. The dominant pressure loss associated with the pumping of liquid metal in projected thermonuclear reactors is that due to flow transverse to a component of a magnetic field that is substantially uniform. The pressure gradient $\partial p / \partial z$ in the flow direction is found to be controlled by the Hartmann number $M = B_1 a \sqrt{\sigma / \eta}$ where

- B_1 = magnetic field component normal to flow direction,
- a = characteristic dimension (half-width a square channel, radius for a circular duct)
- σ = electrical conductivity, and
- η = viscosity.

The Hartmann number is typically greater than 10^4 for reactor conditions. As reviewed in Ref. 3, flow is found to be laminar when $Re/M < 500$ where Re is the traditional Reynolds number $\rho v d / \mu$ and ρ is fluid density, v is average velocity, and d is channel hydraulic diameter. This criterion appears invariably to indicate laminar flow for reactor coolant conditions. Thus, the analyst receives some assistance from nature; i.e., the flow, being laminar, is much more amenable to an analytical approach than the turbulent situation typical of most non-MHD flows.

Equation (12) of Ref. 3 is recommended there as being a conservative estimate of pressure gradient in a round duct with a conducting wall. This equation is:

$$\frac{\partial p}{\partial z} = \frac{\eta v}{a^2} \left[1.3 \left(\frac{M^2 \tanh M}{M - \tanh M} - 3 + \frac{M^2 C}{1 + C} \right) + 8 \right]. \quad (1)$$

The factor 1.3 is added to ensure conservatism, especially in the low M range. Reference 4 indicates that 1.0 is a better value for high M ; this is confirmed by Ref. 5. When it is allowed to degenerate to its simplest form for high values of M and $C \ll 1$, it may be expressed as:

$$\frac{\partial p}{\partial z} = C v B_1^2 \sigma, \quad (2)$$

where

$$C = \frac{\sigma_w t}{\sigma a} \quad (3)$$

is the conductance ratio with t representing wall thickness and the subscript w indicates that the conductivity is that of the wall. Upon substitution of Eq. (3) into Eq. (2), we find

$$\frac{\partial p}{\partial z} = \sigma_w \frac{t}{a} v B_1^2. \quad (4)$$

Thus, we have the result that the pressure gradient as determined by this degenerate expression is completely independent of the properties of the fluid. This situation results from the fact that the dominant contribution to the pressure gradient is that of the $\vec{J} \times \vec{B}$ effect where $\vec{J}$ is the current induced in the moving fluid. Its magnitude is limited by the resistance through the current return path, which is the (implicitly thin) duct wall. The corresponding degenerate expression for a rectangular duct [Eq. (9) of Ref. 5] may be derived by treating a bar (slug flow) moving through frictionless brushes where a return path for the current is provided which has a resistance equal to that of the duct wall. The approach is similar for a circular duct, but not quite so simple because the velocity profile is an elliptical cylinder rather than uniform.

It is of passing interest to examine the question of whether, in a uniform magnetic field, there is benefit to be gained in terms of pressure drop by having the flow proceed in a direction other than perpendicular to the magnetic field. It is obvious from Eq. (4) that the pressure gradient will vary as $\cos^2 \theta$ with θ representing the deviation from perpendicular flow. However, the criterion for a uniform field will almost surely be the pressure drop Δp required to cross a given number of field lines. With the field being uniform, the flow passage length will

vary as $1/\cos \theta$. The result will be that Δp varies as $\cos \theta$. Thus, there is a pressure drop or pumping power advantage to proceeding in a direction other than perpendicular. The following factors would appear to lead to a choice of perpendicular flow:

- (1) long ducts are required, and
- (2) because of the cosine distribution, the angle θ must be rather large to gain significant advantage.

It will be assumed that the perpendicular direction is preferred.

For a uniform magnetic field and a flow distance ΔL , we have the pressure drop Δp from Eq. (4) as:

$$\Delta p = \frac{\partial p}{\partial z} \Delta L = \sigma_w \frac{t}{a} v B_{\perp}^2 \Delta L. \quad (5)$$

From elementary strength of materials texts, we have an expression for the circumferential stress in a cylindrical pipe subjected to an internal pressure stress as

$$s = \Delta p \frac{a}{t}. \quad (6)$$

We may combine Eqs. (5) and (6) and obtain

$$s = \sigma_w v B_{\perp}^2 \Delta L, \quad (7)$$

or for nonuniform $B_{\perp}$, we have

$$s = \sigma_w v B_{\perp}^2 dL. \quad (8)$$

Equations (7) and (8) are somewhat surprising in that:

- (1) there are no fluid properties in the expression for pressure induced stress, and
- (2) the wall thickness is not a determining factor of the pressure stress. This result was apparently first arrived at in Ref. 5.

Point 1 above is discussed in arriving at Eq. (4). Point 2 is a consequence of the dual effect of duct wall thickness. The wall thickness determines the circulating current which determines the pressure drop, but it also determines the wall stress. Thus, if the wall

thickness is doubled, it doubles the pressure drop with the consequence that the pressure stress remains the same.

The pumping power is the product of volumetric flow rate and pressure drop. The ratio of pumping power to rate of increase in coolant heat content may be expressed as

$$PPF = \frac{\Delta p}{\rho C_p \Delta T}. \quad (9)$$

2.2 ESTIMATION METHODS INVOLVING PERTURBATIONS OF FLOW AND MAGNETIC FIELD

Reference 2 is an account of a study which addresses some of the problems of bends, discontinuities of cross section, and changes of magnetic field which one would encounter in a lithium-cooled blanket. It deals with the cases of insulating walls, thin conductive walls, and highly conducting walls. The second case is important to us, and these conditions must be met: $M \gg 1$, $N \gg 1$, and $C \gg 1$. They are met for our conditions. The important indication (insofar as flow in round ducts transverse to a magnetic field which changes slowly in terms of duct diameter) is that such effects are equivalent to adding a few diameters of duct length where these few diameters are treated as though they were in a uniform field. This conclusion seems to apply generally except where flow turns so as to move parallel to the magnetic field. Then two adverse effects occur:

- (1) Upon making such a turn, the flow is confined to very thin layers parallel to the magnetic field. These layers diffuse slowly as they move downstream to fill the entire duct. The rest of the cross section (other than the jets) is stagnant.
- (2) The pressure gradient in this region is substantially higher than the no-field case (the assumption of equality has frequently been made).

Specific cases pertinent to the tube bundle blanket are as follows:

- (1) Turns in a plane perpendicular to $\vec{B}$ are said to produce very small effects.

- (2) Turns where the tube axis is in a plane parallel to $\bar{B}$ produce a larger effect, but still only the equivalent of a few diameters if flow does not become parallel to $\bar{B}$.
- (3) Step change in field strength by a factor of two produces the equivalent of about a one diameter length increase.
- (4) Changes in duct diameter are said to be nearly equivalent to the effect of item 3 above.

In connection with the effect of a changing transverse magnetic field, Ref. 3 gives a method for treating pressure drops for field entry or exit. Discussion with the first listed author of Ref. 2 brought out his view that this method applies only for low values of N . The method overstates the pressure drop so predicted. Earlier calculations by the present author have shown that for realistic entry lengths, the effect even by the methods of Ref. 3 tends to be negligible.

Following the quantitative and qualitative arguments in Ref. 2, the writer of this paper estimates the several perturbations in the tube bundle blanket might be the equivalent of 10 diameters out of a total of about 250 in a circuit composed of a 6-cm-diam tube.

One of the valuable contributions of Ref. 2 is the presentation of a qualitative method for ascertaining velocity distribution and shape of stagnant zones for specific flow or magnetic field perturbations. Its value is substantially that of deciding whether a problem exists rather than a means for numerical evaluation. Using this method, one is able to define the so-called "characteristic surface" which forms the boundary between stagnant zones and "cores" of relatively uniform velocity. It is beyond the scope of this paper to further describe the method here.

2.3 BASIC QUANTITIES AND DUCT ROUTING CONSIDERATIONS

The following studies were conducted with the objective of meeting the basic criteria which were used in the *ORNL Fusion Power Demonstration Study*.¹

Neutron wall loading	3.14 MW/m ²
First wall surface heat flux	0.78 MW/m ²
Maximum magnetic field in superconductor	7 T
Stainless steel temperature limitation	500°C

Volumetric power generation rates are taken to be as used in another *ORNL Fusion Power Demonstration Study*,⁶ in particular,

$$\text{volumetric power generation rate in lithium} = \frac{y}{0.385} \text{ MW/m}^3$$

$$\dot{q}''' = 12.1 e^{-\frac{y}{0.385}} \text{ MW/m}^3$$

same for stainless steel

$$\dot{q}''' = 48.4 e^{-\frac{y}{0.385}} \text{ MW/m}^3$$

where y is measured from the first surface into the blanket.

The surface heat flux results from assuming that there is no divertor. The effect of a divertor would be to reduce the 78-W/cm²-surface heat flux to a small fraction of that value.

The configuration of the toroidal field (TF) coils and the plasma is shown in Fig. 1. Also shown is the approximate space available for the blanket and radiation shield. The magnetic field in the blanket region is assumed to vary inversely with radius measured from the pole of the machine. The $1/r$ assumption was checked by a computer program⁷ and found valid within 2% over most of the blanket and within 6% in a thin region furthest from the pole.

It is probable that penetration between TF coils by ducts carrying coolant will not be possible in the region nearest the pole because of inadequate gap between the coils and possibly because an iron core may occupy essentially all the available volume. Thus it becomes apparent that the region of the blanket nearest the pole and at midheight of the blanket must be serviced by coolant supply and return lines which traverse a considerable distance in and transverse to the magnetic field. Such an arrangement would be a likely candidate to utilize the insulated

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Fig. 1. Configuration of TF coils and plasma.

sandwich construction discussed elsewhere in this paper as a means of reducing pressure gradients. These supply and return ducts could be routed so that they are outside the radiation shield, and there is then no problem with radiation degradation of the ceramic insulation. Figure 2 shows ducts following a logical route wherein the distance to enter or leave the magnetic field is minimized. One additional problem becomes apparent upon inspection of Fig. 2. The ducts are sized for a velocity of 0.25 m/sec and are moderately large. Lithium is a rather poor neutron and gamma radiation shield, and such a configuration would lead to excessive heating in the superconducting coils.⁸ This consideration renders the duct routing shown somewhat unattractive.

2.4 THE TUBE BUNDLE BLANKET

It came as somewhat of a surprise to find that a blanket in which all the flow of lithium coolant is perpendicular to the magnetic field

ORNL/DWG/FED-7884

Fig. 2. Reactor cross section showing alternate duct routings.

could be a satisfactory solution. However, calculations made here on such a configuration indicate that it may well be the most attractive of the several concepts which have been proposed for lithium-cooled tokamaks. One of the important factors arose from defining the best routes for supply and return lines to and from discrete blanket modules, as discussed in Appendix 2. There it is shown that for the region of the blanket nearest the pole of the machine, the route for minimum pressure drop is through the blanket to a region of the blanket furthest from the pole (see Fig. 2). In this outer region, the magnetic field is weak enough that the exact route for passing between the coils is unimportant. If, as just described, we find that supply and return pipes pass through the blanket, then in fact the lithium in these lines is part of the

blanket. If access to the innermost such regions is feasible, the entire blanket could be comprised of such tubes. The superiority of round tubes as regards pressure containment leaves no other choice. It remains to define an acceptable topology and perform the necessary calculations to demonstrate feasibility. Fabrication of round tubes and understanding of flow and heat transfer problems are probably better than in any other configuration.

Figure 3 shows such a blanket consisting of an array of round tubes. This configuration represents a self-consistent and acceptable solution. Optimization has been conducted only to the extent of finding an acceptable solution. Undoubtedly, more exhaustive treatment of the possibilities for arranging tubes in blanket arrays along with the other variables could result in further optimization.

It may be seen in Fig. 4 that the blanket consists of modules containing two types of sub-modules or arrays of tubes: in Type 1 the tube array has constant thickness in the toroidal direction; in Type 2 it is wedge-shaped. The two tube sizes selected for this configuration are

1.5 cm and 6 cm in diameter. For most of our calculational purposes, it suffices to deal with the mean diameter. The larger tube has a wall thickness of 1 mm (0.039 in.), and the smaller has a wall thickness of 0.75 mm (0.030 in.). The thickness is taken to be uniform here. It is shown in Sect. 3.1 that substantial improvement in MHD effects can be made if one judiciously tapers the wall thickness.

Figures 3 and 4 show how tubes emerge from the blanket. The routing will continue to a distance where there are no significant effects from the magnetic field; then the tubes will be manifolded. It needs to be pointed out that once out of the MHD effects, velocities can be increased by at least an order of magnitude. This indicates that ten or so pipes which need to be joined can be connected to a single pipe of, say, the same size as those joined. If manifolding were accomplished inside the magnetic field, velocities could not be so increased. This would lead to additional complexity as well as uncertainty regarding flow characteristics and possible stagnant regions.

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Fig. 3. Views of the tube bundle blanket.

Fig. 4. Another view of the tube bundle blanket.

The 1.5-cm tubes comprising the first wall travel halfway around the circumference and then eight of them make a transition to a 6-cm-diam tube (shown schematically in Fig. 5). This reduces the velocity by a factor of two, and the larger tube then returns in a route which shields it from the high surface heat flux. This is necessary because (at least for uniform wall thickness) the pressure stress is excessive if a first wall tube traverses the entire circumference.

1-1/2 cm TUBES
6 cm TUBES

Fig. 5. Schematic arrangement for manifold-ing eight first wall tubes into one interior tube.

Inner circuits make various numbers of turns, depending on power density.

To cope with the combination of factors affecting flow velocity and tube size for the first wall required some trade-off involving predominately these factors:

- (1) pressure stress (which increases with velocity);
- (2) pumping power (which increases with velocity and wall thickness);
- (3) temperature drop from the heated tube surface to the mixed mean temperature (which increases with diameter);
- (4) diametral temperature difference as affects thermal stress in bending of the tube (which is independent of most variables, but is proportional to tube diameter); and
- (5) coolant temperature rise through the circuit (which decreases with velocity).

In Table 1 are parameters for the design configuration shown in Figs. 3 and 4.

In terms of flow path arrangement, the blanket consists of a number of parallel routes. It is assumed that pumps are located at the blanket discharge, or hot part of the circuit. This avoids increasing the pressure level in the blanket tubes by the amount of pressure drop through the primary heat exchanger. That pumps made from stainless steel can operate at these temperature levels is well established by ORNL molten salt reactor experience.⁹ Considering the inner (6-cm-diam) tubes, it turns out that the turn nearest the plasma must make only one complete circuit of the blanket (to require two would lead to excessive pressure stress). The next furthest in can make two turns (reduced power density allows reduced velocity to attain the same temperature rise). As power density falls further with distance into the blanket, undoubtedly more turns

Table 1. Parameters for the tube bundle blanket

Number of modules	54
Length of tubing for entire blanket	100 km, 6 cm; 37 km, 1.5 cm
Tube diameters and wall thicknesses	1.5 cm, 0.75 mm 6 cm, 1 mm
Pressure drop through 1.5-cm first wall tube plus that through 6-cm return pipe	10 MPa (1450 psi)
Pressure stress at entrance to 1.5-cm tube	100 MPa (14,500 psi)
Diametral ΔT in 1.5-cm tubes	125°C
Combined ^a elastically calculated thermal stress in 1.5-cm tubes	7.06 MPa (48,700 psi)
Pumping power as a fraction of heat removal rate for first wall circuit	2.3%
Same based on electrical power	6.9%
Pressure drop in internal (6-cm) tube circuits	≤ 1.5 MPa (213 psi)
Pressure stress at entrance to internal 6-cm circuits	≤ 44 MPa (6400 psi)
Pumping power-to-heat removal rate for inner (6-cm) circuits	$\leq 0.7\%$
Same based on electrical energy	$\leq 2\%$
Pumping power-to-heat fraction for entire blanket	$\leq 1.1\%$
Same based on electrical energy	$\leq 3.2\%$
Lithium inlet temperature 186° (mb) + 50° (safety)	236°
Temperature rise in first wall circuit	228°C
Temperature rise in inner circuits	264°C
Maximum metal temperature in first wall	500°C
Maximum metal temperature away from first wall	500°C
Lithium velocity in first wall tubes	1.54 m/sec
Lithium velocity in 6-cm tubes nearest plasma	0.77 m/sec

^aDue to combined effect of diametral ΔT and wall ΔT .

will be permissible. In the wedge-shaped subelements, multiple passes (without leaving the magnetic field) will be necessary for attaining the desired temperature rise. This arises from the fact that many tubes in this subelement are short. These particular elements have not been worked out tube by tube; rather, it has simply been assumed that such series passes can be satisfactorily worked out. Undoubtedly, the high pressure drop through first wall elements would be supplied with one or more pumps separate from

the other pumps. In table 1, the sense of the pumping fraction being designated as less than or equal to some number means that the pumping power can be as great as the indicated number if only one pump is used for the inner circuits; where individual circuits require lower pressure drop (lower than the 213-psi value in Table 1), this is achieved by throttling.

It is expected that the radiation shield for the tube bundle module will provide structural support and the means for support during removal

and reinstallation. Figure 4 shows that it is kinematically possible to remove the module outlined darkly, which allows the other modules also to be removed without disturbing the magnetic field coils.

Some means of joining the tubes to form a module is probably desirable. Two possibilities have been considered briefly:

- (1) Banding with straps composed of small diameter tubes through which lithium or helium is circulated. Cooling is mandatory for any straps or bands on the plasma side.
- (2) Selective brazing joining adjacent tubes. Care may be necessary to avoid good thermal connection between tubes that are at different temperatures.

2.5 VARIATIONS ON THE TUBE BUNDLE BLANKET

Two variations on the blanket design just outlined could prove to be advantageous. If the surface heat flux were to be reduced significantly (such as through use of a divertor) while other requirements remain the same, then the first wall tube diameter could be increased. The smaller number of larger tubes to cover the first wall might be considered advantageous. This situation is dominated by thermal stresses which re-

sult mainly from the diametral ΔT in these first wall tubes. It is shown in Appendix 4 that to an adequate approximation for laminar flow (independent of velocity profile) this temperature difference is uniquely determined by proportionality to diameter and surface heat flux. The basic repeating array of tubes associated with the first wall is shown schematically in Fig. 6.

The 3-cm first wall tubes complete a half circuit, then make the transition to a 6-cm tube which then returns through the half circuit and exits the blanket. The larger tube, as before, is shielded from the surface heat flux. Table 2

Fig. 6. Schematic arrangement for manifold- ing 3-cm first wall tubes into 6-cm interior tubes.

Table 2. Parameters for the tube bundle blanket for reduced surface heat flux (0.52 MW/m²) using lithium throughout

Number of modules	54
Length of tubing for entire blanket	100 km, 6 cm; 18 km, 3 cm
Tube diameters and wall thicknesses	3 cm, 1 mm 6 cm, 1 mm
Pressure drop through 3-cm first wall tube plus that through 6-cm return pipe	6.7×10^6 Pa (970 psi)
Pressure stress at entrance to 3-cm tube	101×10^6 Pa (14,700 psi)
Diametral ΔT in 3-cm tubes	167°C
Combined elastically calculated thermal stress in 3-cm tubes	391 MPa (56,700 psi)
Pumping power as a fraction of heat removal rate for first wall circuit	3%
Same based on electrical power	9%
Pumping power in lithium-cooled 6-cm tubes is same as for Table 1	
Lithium inlet temperature 186°C (mp) + 50° (safety)	236°C
Temperature rise in first wall circuit	212°C
Temperature rise in inner circuits	264°C

shows the parameters for this modified blanket. The approach was to lower the surface heat flux (compared to the design enumerated in Table 1) until the combined stress (56,700 psi) was below the ASME Code allowable described in Appendix 5.

The second variation on the tube bundle blanket is to cool the first wall tubes with helium. A satisfactory solution can be so obtained with 3-cm tubes containing the helium. Results

are given in Table 3. Helium permits the first wall tubes to make a complete circuit of the blanket between entry and exit. Another advantage of the use of helium is that in this difficult first wall area, the inlet temperature is not constrained to be above the melting point of lithium (186°C). Finally, in contrast to helium tubes immersed in lithium, one is not obliged to provide double containment for the helium.

Table 3. Parameters for the tube bundle blanket for 3-cm- ϕ -helium-cooled first wall tubes

Number of modules	54
Length of tubing for entire blanket	100 km, 6 cm; 18 km, 3 cm
Tube diameter and wall thickness	(helium) 3 cm, 1 mm (lithium) 6 cm, 1 mm
Inlet pressure	6.9 MPa (1000 psi)
Pressure drop	4.1 KPa (16 psi)
Pressure stress	103 MPa (15,000 psi)
Inlet flow velocity	265 m/sec
Diametral ΔT in tube	164°C
Combined elastically calculated thermal stress in 3-cm tubes	436 MPa (63,200)
Pumping power-to-heat removal rate for 3-cm tubes carrying helium in first wall	2.2%
Same on electrical basis	6.6%
Pumping power in lithium-cooled 6-cm tubes is same as for Table 1	
Inlet helium temperature	150°C
Temperature rise in helium	147°C
Film temperature drop	164°C
Maximum metal temperature in first wall	461°C

3. RELATED MATTERS REGARDING LIQUID METALS

3.1 METHODS FOR MITIGATION OF MHD-INDUCED PRESSURE STRESSES

There are some alternatives to accepting the pressure drops associated with flow transverse to the magnetic field in a conductive round duct of uniform thickness. The most effective, but least feasible, is to make the inner wall of the duct electrically insulating. This would result in a large reduction of pressure gradients, but there is essentially universal acceptance of the position that hot lithium is incompatible with available ceramic insulators even without consideration of the radiation damage aspects of the ceramic. Three other approaches have more realistic possibilities for such mitigation. They are:

- (1) tapered wall thickness in the ducts,
- (2) sandwich construction of ducts, and
- (3) subdivision of area within round ducts.

The pressure gradient in round ducts flowing transverse to magnetic fields which vary slowly along the flow direction is proportional to the wall thickness. Thus, in regions where the pressure is relatively low, the thickness may be reduced, which — because of this coupling of thickness and pressure gradient — will reduce pressures in upstream regions. In Appendix 3 this is discussed further, and an expression for the required variation in thickness is derived for the case where velocity is increased (over that in a duct of uniform thickness) without increasing the maximum stress in the duct. This approach can also be used to increase duct length, decrease duct stress, or allow increased magnetic field strength, all compared to the case with uniform wall thickness. Such tapered tubes have been fabricated¹⁰ of stainless steel. The technique involved programmed positioning of the mandrel as the tube was pulled through the drawing die. The tubing manufacturer consulted on this matter stated that they are now fabricating such tubes of stainless steel which have our de-

sired range of wall thickness, but the diameters are less than some of those used in this study (their maximum OD is slightly less than 1.6 cm). They expressed no reservation as to technical feasibility, but pointed out that our larger diameter (6 cm) would require equipment larger than they now have.

The term "sandwich construction" is used here to describe layered construction where the metallic layer adjacent to the lithium is as thin as feasible and is isolated electrically from the thicker metal structural wall by insulation, which could be ceramic or glass. This sleeve is presumed to be so thin that it serves substantially no function in resisting the pressure stresses, and all resistance to internal pressure is now in the heavy structural wall outside the insulator. The current is now constrained to the thin sleeve, and the pressure stress is now decoupled from the pressure gradient. It is perhaps reasonable to consider that the thickness of this liner might be 0.025 cm, which compares with the tube thickness of 0.10 cm used in the rest of this study. This would result in a reduction by a factor of four in the pressure gradient. One is now free to reduce the wall stress by increasing the wall thickness, so an order of magnitude reduction in pressure stress appears reasonable. The ultimate feasibility of such construction seems assured, but development and detailed evaluation would be required. Also, it is expected that such construction may not be feasible in a high radiation environment, which may restrict it to application outside the shield.

Reference 2 demonstrates that if a round duct is subdivided into diamond-shaped elements, as in Fig. 7, where the subdivision is accomplished with flat elements incorporating sandwich construction, then a large reduction in pressure gradient is possible. This approach shares all the reservations associated with a lined round duct, plus those arising from the fragility of thin flat elements.

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Fig. 7. Subdivided round tube to reduce pressure gradient.

3.2 HEAT TRANSFER CONSIDERATIONS

3.2.1 Heat Transfer in Laminar Flow

It is established that the MHD controlled flow with which we are dealing is laminar, and analytical treatment of heat transfer into a laminar stream is not difficult. However, the specific velocity profiles which are encountered in our problems are not encountered elsewhere, so it was necessary to extend the existing theory a little for our thin-walled round tubes in a uniform transverse magnetic field. The velocity distribution is elliptical as a function of the direction perpendicular to the magnetic field.¹¹ In the interest of simplicity, guidance was sought from the case for flow between flat plates, but preserving the elliptical velocity profile. In performing the necessary temperature distribution calculation, two quantities are needed for our asymmetrically heated tubes:

- (1) The temperature difference across the diameter of the tube, which is assumed to be the same as for flat plates with the same spacing as the tube diameter (see Appendix 4 for

justification). This quantity is necessary to evaluate the critical thermally induced bending stress.

- (2) The temperature difference between the heated side of the stream and the mixed mean temperature. This is necessary to obtain the maximum wall temperature.

The diametral ΔT for the flat plate geometry was found to be equal to one-half of the steady temperature difference across the same medium when the medium is stationary, or

$$\Delta T_d = \dot{q}'' \frac{a}{2k} \quad (10)$$

This result is independent of the velocity profile and is assumed (see Appendix 4) to apply to the round tube case. The second temperature difference listed is dependent on the velocity profile and for the elliptical profile turns out to be $0.714 \Delta T_d$.

3.2.2 Nusselt Number for Use With Helium in First Wall Tubes

For turbulent helium flow in round tubes, axisymmetric heat input which is constant with length is well treated by the correlation¹²

$$Nu = 0.022 Re^{0.8} Pr^{0.6} \quad (11)$$

For a heat input distribution that is not axisymmetric, the heat transfer coefficient is known to be lower at the point of maximum heating, leading to a larger ΔT_f . Reference 12 gives a treatment of heat flux variation which is distributed as follows:

$$\dot{q}''(\theta) = \dot{q}_0'' (1 + b \cos \theta) \quad (12)$$

If we take $Re = 5.6 \times 10^5$ and $Pr = 0.7$, then the local Nusselt number at the point of max $\dot{q}''$ is reduced so as to give an 18% increase in the ΔT_f . This Reynolds number applies to the example of the helium-cooled first wall used in this report, and so the 18% effect will be used. Pumping power for the one case of helium flow is evaluated with density taken at a temperature which is the average of that at the inlet and outlet.

Using this density, the pumping power is calculated as though the flow were incompressible.

3.2.3 Entry Length Considerations

Reference 12 deals with entry lengths to reach fully developed temperature profiles for laminar flow. These have been found to be a significant, although minor, portion of the circumference around the blanket. However, since temperature differences are reduced in the developing region, it is conservative to ignore these effects and this is done herein.

3.2.4 Twisted Tape in First Wall Tubes

If the flow in first wall tubes were rotated in a length which is short compared to the thermal entry length, the troublesome diametral temperature difference could be substantially reduced. In concept, this is possible with a twisted tape inserted in the tube. If the pitch of the twist is large enough, the MHD effects will be negligible. This matter needs further attention, perhaps experimentally.

3.2.5 Reduced Cyclic Thermal Stress in Inner (6-cm) Lithium Tubes

The tube wall temperature will follow that of the lithium closely. There will be a small ΔT_d due to variation in the volumetric heating rate. This has been estimated to be 10°C. If, in the power-off portion of the cycle, the flow in these tubes is stopped, then the ΔT_d should disappear fairly quickly, but further equilibration of temperature must take place by the mechanisms of:

- (1) axial conduction in the long flow passages,
- (2) radiation, and
- (3) contact conductance.

These should be slow, suggesting that cyclic thermal stresses would be largely eliminated.

3.3 COMPARISON OF LITHIUM AND OTHER COOLANTS

3.3.1 Boiling Potassium or Cesium as a Coolant

Insofar as the MHD effects on liquid metal are concerned, there are two obvious advantages

to use of these two coolants:

- (1) Only the supply flow is a liquid with high electrical conductivity. This eliminates the MHD effect associated with the return flow.
- (2) Both fluids have a high heat of vaporization, which reduces the flow requirements.

The volumetric flow rate of either cesium or potassium liquid normalized to that of lithium may be shown to be

$$\frac{\rho_c \Delta T_p}{\rho_b \Delta h_e},$$

where the numerator refers to lithium and the denominator to either of the other (boiling) coolants. The flow area required to carry vapor of density ρ for an assumed velocity v and heat rate input $\dot{Q}$ may be shown to be

$$\frac{\dot{Q}}{\rho \Delta h_e v}.$$

Assuming that the structure temperature is limited to 500°C and that the corresponding fluid temperature is restricted to 475°C, these quantities are evaluated in Table 4 for a heat input of 3.7 MW/m². Reference 13 is the source of the data for cesium and potassium.

The potential gain in liquid metal MHD effects for cesium or potassium is obscured by the large flow area required for vapor flow. If a substantial temperature increase were available, the increase in vapor density for these two fluids could negate this adverse conclusion.

3.3.2 Comparison with Sodium and with Helium

Sodium, of course, has no breeding potential, but one can conceive of using it as a coolant in the immersed tube concept. One can compare the heat transfer merits with lithium via the quantity $\rho v A_c \Delta T$. It will be assumed that the flow area available for the two fluids is the same; then we can examine the required velocity ratio for the two coolants. One can expect that the temperature rise is greater in the lithium

Table 4. Liquid velocity ratios and vapor flow area^a

	Δh_e Enthalpy change for evaporation $\frac{J}{kg}$	ρ_L Liquid density $\frac{kg}{m^3}$	ρ_v Vapor density $\frac{kg}{m^3}$	Liquid volumetric flow rate normalized to lithium	Area required for vapor flow per m^2 of first blanket wall, m^2
Lithium	-	490	-	-	-
Potassium	2.1×10^6	850	0.014	0.35	1.26
Cesium	0.62×10^6	1150	0.16	0.86	0.37

^aProperties evaluated at 475°C. Vapor velocity assumed to be 100 m/sec. Wall loading 3.7 MW/m². Lithium c_p taken as 4190 J/kg°C. Lithium temperature rise of 300°C.

because there is the potential of largely eliminating the heat transfer ΔT from the structure to the lithium. This arises from the heat being generated in the lithium. Thus we obtain:

$$\frac{v_{Na}}{v_{Li}} = \frac{(\rho c_p \Delta T)_{Li}}{(\rho c_p \Delta T)_{Na}}$$

Table 5 results.

Table 5. Velocity comparison for sodium and lithium

	$\rho (\frac{kg}{m^3})$	$c_p (\frac{J}{kg^\circ C})$	$\Delta T (^\circ C)$	$\frac{v_{Na}}{v_{Li}}$
Sodium	865	1260	214	2.3
Lithium	490	4190	264	

The increased velocity results in increased MHD effects and thus probably rules out sodium.

Using the same equation, we can compare the relative merits of helium and lithium as coolants in regard to the flow area required to carry heat energy away at a given rate. For this purpose, we obtain

$$\frac{A_{Li}}{A_{He}} = \frac{(\rho v c_p \Delta T)_{He}}{(\rho v c_p \Delta T)_{Li}}$$

Using 400°C as the temperature for property determination, and assuming the helium pressure is 6.9 MPa (1000 psi), Table 6 results.

Table 6. Flow area comparison for lithium and helium

	$\rho (\frac{kg}{m^3})$	$v (m/sec)$	$c_p (\frac{J}{kg^\circ C})$	$T (^\circ C)$	$\frac{A_{Li}}{A_{He}}$
Lithium	490	1	4190	264	1.07
Helium	4.97	100	5450	214	

The flow areas are substantially the same. This means that where the necessary shield penetrations occur for coolant ducts, radiation streaming will be worse for helium than for lithium. When the lithium duct is outside the magnetic field, the velocity can be increased by about a factor of ten, which clearly favors lithium in terms of flow area in that region.

3.4 STAGNANT REGIONS AND THEIR CONSEQUENCES

A potential problem has been identified associated with any thermonuclear reactor having a lithium blanket which is not circulated for the purpose of heat removal. The potential problem is that MHD effects on the lithium may sufficiently immobilize portions of it as to retard removal of tritium and to restrict heat removal to the conduction mode. The two primary factors which bear on this are:

- (1) the size of superficially stagnant lithium regions and the extent to which motion is suppressed, and
- (2) the extent to which tritium can diffuse through regions of nonflowing lithium.

Two dimensionless parameters which govern liquid metal MHD are the Hartmann number M and the interaction parameter N . It is well established that turbulence is suppressed when M exceeds certain values (which differ for flow perpendicular or parallel to the field) and that values of M which prevail in thermonuclear reactors are usually several orders of magnitude above those which dictate suppression of turbulence. Thus, one can expect that there will be no mass transfer due to eddy diffusion for those circumstances.

The following analytical or experimental results suggest that stagnant regions may be a common feature of flow of liquid metals in strong magnetic fields.

- (1) A Russian paper¹⁴ presents experimental results for a planar, submerged jet which is perpendicular to the magnetic field. It is shown that the jet forms two planar jets which are perpendicular to the original one and that the rest of the fluid is superficially stationary.
- (2) Analytical results supported by experiments¹⁵ were obtained for the case of rectangular ducts where the flow direction is perpendicular to the magnetic field. Two opposite walls were perfectly conducting and the others were insulators. For some orientations where the field is not aligned with a side, triangular regions of zero velocity were shown to exist.
- (3) Reference 16 deals analytically with flow over obstacles in a constant area channel with a transverse magnetic field. If the walls are nonconducting, then it is found that in a cylinder which is parallel to the applied magnetic field and circumscribing the body, there is no flow provided the body is insulating. If the body is conducting, flow takes place in the corresponding cylinder, but it differs (in an unspecified manner) on the inside and outside of the cylinder.
- (4) Reference 17 measures the drag coefficient for a sphere immersed in sodium with the

flow parallel to the magnetic field. It is found that the drag coefficient increases by about an order of magnitude as the interaction parameter is increased from 0 to 100. The data is well correlated with N . It is established that as N increases, upstream and downstream wakes are formed which tend toward cylinders that enclose the sphere and are parallel to the magnetic field. The fluid in these wakes is at least superficially stationary. When one estimates N for a thermonuclear reactor, it is reasonable to expect a value four orders of magnitude greater than the above value of 100. This results from increases in B and d and a decrease in v (associated with removing lithium in order to extract tritium).

Reference 18 is a review article dealing with incompressible MHD flows. It states that "cores" (or regions of uniform velocity) and "shear layers" (or thin regions of velocity gradient away from a wall) are common when M is large. The shear layers emanate from wall discontinuities such as corners or changes of conductivity.

The simplest mathematically, and historically the first cases involving cores, is found in the original Hartmann solution (p. 146, Ref. 11) for flow between flat plates with the magnetic field perpendicular to the plates. For high values of M , the deviation of velocity from uniformity throughout most of this core is exceedingly small. If this guidance is proper, the assumption of no motion at all is a good one.

Supply and discharge of lithium for the sole purpose of extracting tritium almost surely will fall in the geometrical context of small ducts going into and out of large pot-like lithium containers, and discontinuities probably are unavoidable. It is apparent that there are phenomena encountered in these MHD flows which have no counterpart in ordinary fluid mechanics. It probably is correct that the extent of immobility is answerable only experimentally.

The following factors may offer some mitigation of the tritium problem and some worsening of

the cyclic thermal stress problems:

- (1) With each plasma cycle, the poloidal field goes off and on. Some stirring may result.
- (2) The postulated lock-up can be released by turning off the toroidal field.

In addition to these effects on pressure driven flow, it has been well established¹⁹ that a magnetic field has a suppressing effect on natural convection.

The recent availability of data for the diffusion coefficients for hydrogen and deuterium in lithium allows some quantification of the seriousness of the potential immobilization under the assumption that there is no motion in the lithium.

Data obtained for temperatures from 625°C to 900°C²⁰ was extrapolated to 500°C to yield the diffusion coefficient $D = 10^{-6}$ cm²/sec. Also, data was obtained verbally from the same author on deuterium in lithium measured over about the

same temperature range. The same type of extrapolation yields a value of $D = 1.1 \times 10^{-6}$ cm²/sec at 500°C.

Using the equation for steady-state, one-dimensional diffusion, the following question was addressed:

Assume that half the blanket volume consists of slabs of stationary lithium arranged such that all faces of the slab allow removal of tritium. Assume that we can tolerate 2000 gm of tritium held up so as to drive the diffusion process; then what is the maximum slab thickness tolerable? The answer for the slab half-thickness was calculated to be 2.2 cm under the assumption of uniform generation of tritium. This could be troublesome and may require special attention.

The effect on cyclic thermal stress would be that arising from redistributing fluid having a preexisting temperature distribution resulting from internal generation and conduction.

4. CONCLUSIONS

The most important conclusion seems to be the establishment of the feasibility of a lithium-cooled blanket which consists entirely of round tubes containing lithium. The space between tubes is empty. This has become feasible because recent studies have indicated the possibility of smaller size and reduced magnetic field strength (higher β). When compared to blankets cooled by other fluids, three obvious and substantial advantages accrue:

- (1) In most of the blanket, the sensible heat which must be carried away is born in the lithium. Thus, it is not necessary to incur temperature differences in heating the coolant. Provided there are no stagnant regions, this can lead to an increase in cycle temperature (compared to helium) of $\sim 60^\circ\text{C}$, which results in an increase in cycle efficiency by a factor of about 1.07.
- (2) Cyclic thermal stress away from the first wall may be largely eliminated.
- (3) Compared to the case where static lithium is cooled by another fluid, tritium removal is rendered easier.

Use of round tubes provides credibility in terms of (1) fabricability and (2) calculability of pressures, stresses, and heat transfer.

The basis for MHD calculations with flow in uniform transverse fields has existed and been accepted for some time. Recent work dealing with transitions in flow area, magnetic field, and flow direction indicates that for these small diameter tubes, such effects have a minor, perhaps negligible, consequence. This substantially provides the remaining tools to deal with MHD flow in such tubes. A lesser, but significant, advantage to such use of lithium (compared to helium)

is that high pressure stresses occur only in low temperature regions.

The high first wall surface heat flux leads to tubes in the first wall that are smaller than those which are acceptable in the rest of the blanket. This condition changes if surface heat fluxes are reduced or if helium is used in the first wall tubes.

While it has not been necessary for the assumed conditions to invoke these approaches, two techniques are available to mitigate pressure stresses:

- (1) The wall thickness of these round tubes can be tapered to reduce the integrated pressure gradient. Fabrication of such tubes is now a commercial reality.¹⁰
- (2) Insulated sandwich construction outside the radiation shield can reduce such pressure stresses by an order of magnitude. This would require some development.

In the temperature regime permitted by stainless steel, heat removal with boiling potassium or cesium appears to be ruled out by vapor flow area considerations. Use of liquid sodium as a coolant requires about twice the velocity as does use of lithium. This causes a doubling of the MHD pressure drops.

There may be potential for blankets which have round tubes containing lithium immersed in nominally stationary lithium, although the basis for this calculation does not exist and would need to be addressed experimentally. Another area which could be addressed in the experimental regime is demonstration of the tapered wall thickness concept. The matter of reduction of diametral ΔT in the first wall by use of twisted tapes as discussed in Sect. 3.2 could be addressed with relatively simple experiments.

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APPENDICES

A.1 OTHER BLANKET DESIGNS

A.1.1 CELL CONSTRUCTION

Several studies have been published of blanket designs where the wall exposed to the plasma consists of prismatic or nearly prismatic cells, the axis of which is nominally perpendicular to the first wall surface. Reference 21 is typical of the design concept in several papers generated by Culham Laboratory. Reference 22 is a recent study from Argonne National Laboratory. The nature of these cells is shown approximately in Fig. A.1.1 (taken from Ref. 21). The cell walls have been proposed to be cylindrical (in which case the pressure containment would be by the

cylindrical walls), hexagonal, or square (in which case the cell walls push on each other). A common feature is the central bayonet tube supplying lithium flow to the cell. The bayonet tube must have electrical insulation between the inner and outer surfaces, or else there must be two coaxial tubes with a void between them.

It is the writer's view that demonstration of technical feasibility (at least for the neutron wall loading and surface heat fluxes used in this study) probably cannot be ascertained within the context of most of the readily applicable analytical treatment of fluid flow, heat transfer, and stress analysis. Such analysis faces difficult three-dimensional treatment.

Some specific reasons for uncertainty, and in some cases pessimism, follow:

- (1) A closely packed array of equal size circles, hexagons, or squares cannot be used to cover a toroidal-like configuration.
- (2) For circular cells, even if they did achieve the coverage just mentioned, there would still be many substantial size streaming paths for neutrons.
- (3) The lithium pressures are reasonably high in the cell, and the diameter of the cell is large (at least compared to the tubes in the tube bundle blanket herein). It is implied that the cell walls are fairly thick, which can involve significant internal heating.
- (4) If adjacent flat-sided elements depend upon each other to contain the internal pressure, loss of pressure in one of them could lead to a domino effect. Even with normal operation, careful pressure balancing would probably be required.
- (5) Hexagonal or similar shapes must be joined to spherical or similar ends (nearest the plasma). The corners implicit in this could be troublesome stress raisers as regards pressure and thermal stresses.
- (6) Reference 2 describes how the flow would be expected to turn so as to flow parallel to the magnetic field in a thin layer without

Fig. A.1.1. Blanket design using cell construction.

impinging on the first wall. Between the end of the bayonet and the first wall there would be a stagnant region in which conduction alone would be available for heat transfer. If one assumes that the first wall is 2.5 mm thick (following Ref. 22) and that there is a stagnant lithium layer of 1 cm, then the combined effects of surface heat flux and internal power generation in stainless steel and lithium as used in this report lead to a temperature difference of 290°C. In addition, there are other regions involving abrupt changes in flow cross section, which implies that other stagnant regions are likely.

A.1.2 LITHIUM IN IMMersed ROUND TUBES

Heat transfer consideration tends to preclude thick containment walls, and MHD effects inevitably lead to pressures that are reasonably high. There is potential attractiveness in a concept which contains most of the lithium required for breeding as nonflowing; thus it is at a low pressure and allows filling the inevitable odd shapes. Cooling would be accomplished by circulating other lithium through small circular tubes which contain the flow-induced high pressure. While the concept has potential attractiveness, calculational treatment must await development of methods for treating flow in tubes which are also immersed in conductive liquid. Electrical separation of the two regions by insulators seems precluded by requirements for good heat conduction and radiation stability. Two factors are different from existing treatment of flow in thin conducting tubes:

- (1) The current return path is no longer confined to the thin wall but can be partly in the surrounding liquid.
- (2) This surrounding fluid is free to move as a result of $j \times B$ forces.

About all that can be done with the present state of knowledge is to describe some indications for two cases.

Case 1

Most of the heat transfer into the tubes takes place with the flow being nearly parallel to the magnetic field, keeping the distance associated with the necessary field crossings at a minimum. If the solution for Case 1 resembles Eq. (2), then whether or not pressure gradients are acceptable can be considered in terms of the relative magnitude of $B_{\perp}^2$ and C and in the light of having obtained acceptable pressure gradients for the tube bundle blanket. If the immersed tubes follow the toroidal direction, then $B_{\perp}$ will have a value of about 10% of the toroidal magnetic field, so that $B_{\perp}^2$ is about 1% of the toroidal value. This implies that C can have a value increased by 100 and still be acceptable, which seems plausible.

Case 2

The round tubes are arranged in planar arrays so that the flow is perpendicular to the magnetic field and the planar array is parallel to the magnetic field. This is to take advantage of the tendency of the lithium outside the tubes to be dragged along, as in the case of a cylinder moving perpendicular to the magnetic field but along its own axis.

Exact analytical solutions exist²³ for a group of incompressible MHD problems which appear to have implications regarding the magnitude of pressure drop for immersed flow and the manner in which the array of round tubes should be arranged to minimize pumping losses. The class of solutions is that for motion of cylinders (including noncircular) parallel to their own axis and transverse to a uniform magnetic field. For our purposes, we will consider only circular cylinders. For this case, the solution describes two regions of motion (or cores) separated from the stationary region by boundary layers which are parabolic, as shown in Fig. A.1.2.

For $M \gg 1$ the parabolas degenerate to planes which contain the cylinder and are parallel to the magnetic field. For this degenerate condition the paper shows that for the case of a

Fig. A.1.2. Core moving with a cylinder in a magnetic field.

perfectly conducting cylinder the velocity in the core is equal to that of the cylinder, whereas for an insulating cylinder the velocity in the core is one-half that of the cylinder. It is further shown that the drag force D per unit length is given by $D = 2\Delta\phi\sqrt{n\sigma}$, where $\Delta\phi$ is the potential difference between the two undisturbed regions. For our degenerate case $\Delta\phi$ is the product of core velocity, B_y , and $2a$. The exact velocity distribution is presented as an infinite series of Bessel functions, but an asymptotic approximation given in the paper allows estimation of how the velocity decays with distance from the cylinder in the y direction.

This asymptotic solution turns out to be

$$w = \frac{Ma}{\pi y} \text{ for } x = 0,$$

where w is velocity normalized to the maximum velocity in the core. If we take M as 32,000, $w = 1/2$, and $a = 2.5$ cm, we obtain $y = 1$ km.

Clearly, these cores can be taken as planar within blanket dimensions.

Some qualitative insight is readily available for the degenerate case above. In liquid metal flows with $M \gg 1$ the dominant effect tends to be the $j \times B$ forces. In the case of $M = \infty$, the flow has arranged itself so that the planar cores are infinite plates. Thus there is no current return path. In a finite geometry there would be return paths, but the suggestion seems to be that these current return paths would be reasonably long and of high resistance.

In order to draw the desired inference, one additional similarity must be postulated. This has to do with the similarity between the solutions just described and that for flow in a thin-walled, round, stationary conducting tube. This latter flow situation has a velocity distribution (in the absence of immersion) which is elliptical as a function of the direction perpendicular to the magnetic field.¹¹ If we now postulate that the velocity distribution in the planar core is the same elliptical distribution, we have a case where currents do not flow (as they did not for uniform velocity above).

The drag relationship permits comparison of an equivalent drag pressure gradient defined in terms of the quantity D above, and thus:

$$\left(\frac{dp}{dz}\right)_D = \frac{1}{\pi a^2} D.$$

We may now compare this with Eq. (2), which gives the pressure gradient for such a duct without immersion. If we use the subscript f to denote this latter gradient we find that:

$$\left(\frac{dp}{dz}\right)_D / \left(\frac{dp}{dz}\right)_f = \frac{1}{MC}.$$

For our conditions, the quantity MC is about 100, so the drag effect is negligible.

Given the planar cores as described above, it is apparent that more moving cylinders or ducts can be added in the planar core without altering the current flow situation. Thus one imagines an array of tubes as in Fig. A.1.3 with

Fig. A.1.3. Cores surrounding flow in round tubes.

flow into or out of the paper. Consider the interaction between two such planar arrays of tubes. If the flow direction is opposite in these two arrays it is apparent that the potential at the two points A in Fig. A.1.4 is the same, and there is no current flow between these two points. This would not be the case for flow in the same direction.

Extending the suggestion, it seems reasonable to expect that planar arrays of heat transfer tubes can be routed in blanket modules as in Fig. A.1.5 without large penalty for pressure

Fig. A.1.4. Multiple banks of parallel tubes.

Fig. A.1.5. Convoluted array of round tubes.

drop. There is no requirement that the tube axis be perpendicular to the magnetic field, but only that the plane of the array of tubes be parallel to the magnetic field. The infinite nature of the dimension of the solution just discussed must be tempered with finite reality, and thus current return paths will be available.

A.1.3 GRAVITY-INDUCED LITHIUM FLOW

This last concept is nothing more than an unusual idea that seemed worth recording. However, such less developed concepts do point out some unique features of liquid metal flow which may be subject to exploitation. In flow transverse to a uniform magnetic field, nearly all the resistance to flow results from $\mathbf{j} \times \mathbf{B}$ forces on currents flowing in the core. The currents result because there is a return path for them, either through an external path or through a boundary layer associated with the no-slip condition. If one imagines a quantity of liquid metal falling transversely through a uniform magnetic field, it is apparent that there will be no such boundary layer or solid current return path; ideally, the flow will occur without resistance. It remains to incorporate this effect into a realistic scheme. Perhaps the reader will have more success at this than the writer.

A.2 DUCT ROUTING FOR THE TUBE BUNDLE DESIGN

Figure 2 studies duct routing. Shown on it are values of toroidal magnetic field strength as a function of radius from the poloidal axis of the machine. It is shown in Sect. 2.1 that pressure drop and pressure stress for flow transverse to a slowly varying magnetic field are given by:

$$\Delta p = \sigma_w \frac{t_w}{a} v \int B^2 d\ell, \text{ and}$$

$$s = \sigma_w v \int B^2 d\ell.$$

The importance of the quantity $\int B^2 d\ell$ is obvious.

The blanket region is shown subdivided as though an immersed tube concept would be used, cooling with circulated lithium through the immersed tubes. The round ducts shown penetrating

the shield are sized to be consistent with a ΔT_r value of 300°C and $v = 0.26$ m/sec. This velocity turns out to be that required to limit the pressure stress s to 103 MPa (15,000 psi) for route B. It is obvious that these ducts replace large pieces of the radiation shield. Inward towards the pole of the machine the superconducting toroidal field coils present nearly a solid wall as far as interception of radiation is concerned. This has been indicated by an authority in the field⁸ to pose an unacceptable radiation load on the superconductive coils, which is a result of penetration through the lithium by neutrons and gamma radiation.

Also shown in Fig. 2 are three alternate routes — A, B, and C — which reveal something about the value of $\int B^2 dL$ for these routes. They all have to do with paths required to enter or leave the magnetic field in order to service the most difficult module (at midheight and closest to the pole). Route A is the shortest distance possible for this 25-cm-diam line to pass through the coils. Route B is intended to be more realistic, but nearly the shortest route. Route C recognizes the fact that because B^2 varies substantially with radial position, the most direct route is not the best one. Table A.2.1 gives the value needed for calculating the MHD-induced pressure stresses for routes A, B, and C. The factor of two is necessary because entry and departure are required. The point is obvious that a route through the blanket has two clear advantages:

- (1) reduced pressure stress, and
- (2) relocation of the region for entry and exiting the field to where the coils are widely spaced, which can make the coil irradiation problem acceptable.

Table A.2.1. Values of $\int B^2 dL$ for serving modules

Route	$2\int B^2 dL$ (T ² m)
A	287
B	312
C	154

A further logical development arises from the fact that if the supply or return pipe passes through the blanket region, it is indeed part of the blanket. It seems advantageous to make the entire blanket consist of these round tubes, which evolve to the tube bundle blanket. The routes D and E and their associated doglegs are necessary for calculation in the blanket region; values are reported in Table A.2.2.

Table A.2.2. $\int B^2 dL$ for routes in blanket

Route	$\int B^2 dL$ (T ² m)
D	66.1
Dogleg for Route D	5.7
E	72.7
Dogleg for Route E	4.4

A.3. DERIVATION RELATIVE TO THE ADVANTAGE OF TAPERING THE WALL THICKNESS IN TRANSVERSE MHD FLOW

The pressure at a given point in any duct, whether or not subject to MHD effects, can be viewed as the integral of the pressure gradient from the discharge end upstream to the point in question. One unique aspect of MHD flow transverse to a substantially uniform magnetic field is that the pressure gradient is proportional to the wall thickness. Consider a duct with uniform wall thickness where the thickness is set by pressure stress requirements at the upstream end. At points downstream, the stress will be at a lower level, but the maximum pressure stress is determined by this uniform thickness. If in these downstream regions one were to reduce the thickness, then the pressure at our most highly stressed point would be reduced, allowing a reduced stress, an increased velocity, a greater length, or a stronger magnetic field. It is intuitively clear that if one provides the proper variation of wall thickness, the case of uniform wall stress along the length can be achieved. This case will be addressed here. A further constraint will be that there will be some minimum wall thickness set by fabrication, handling, or corrosion requirements. This may be shown on a

plot of pressure stress vs distance as in Fig. A.3.1, where a uniform field is assumed. Curve A shows the case for uniform wall thickness where the pressure stress is at the allowable value at the upstream end. For curve B, the stress will be assumed to reach the allowable value in a shorter distance L_1 due to an increased velocity. Upstream from that point the stress is assumed to be uniform. The pressure and stress are related as

$$s = p \frac{a}{t}.$$

If we solve for t and differentiate, recognizing that s and a are constants, we find

$$\frac{dt}{dz} = \frac{a}{s} \frac{dn}{dz}. \quad (\text{A.3.1})$$

We may now substitute Eq. (4) into Eq. (A.3.1) and obtain

$$\frac{dt}{dz} = \frac{\sigma_w}{s} v B^2 t.$$

Separating variables and integrating, we obtain

$$\ln \frac{t}{t_1} = \frac{\sigma_w}{s} \int_0^z v B^2 dz, \quad (\text{A.3.2})$$

where t_1 is the thickness at $x = 0$, which is also the minimum allowable thickness as discussed above.

For curve A in its entirety and curve B over the length L_1 , we have from integrating Eq. (4) and substituting $s = p a/t$ that

$$s = \sigma_w \int_0^L v B^2 d\ell \quad (\text{curve A}), \text{ and}$$

$$s = \sigma_w v_1 \int_0^{L_1} B^2 d\ell \quad (\text{curve B});$$

manipulating

$$\int_0^L B^2 d\ell - \int_0^{L_1} B^2 d\ell = \frac{s}{\sigma_w} \left(\frac{1}{v} - \frac{1}{v_1} \right), \quad (\text{A.3.3})$$

but

$$\int_0^L B^2 d\ell - \int_0^{L_1} B^2 d\ell = \int_{L_1}^L B^2 d\ell = \int_0^{L_2} B^2 dz. \quad (\text{A.3.4})$$

Substituting from Eq. (A.3.2) (with $v = v_1$) into (A.3.4), which is then substituted into (A.3.3), we obtain

$$\frac{t}{t_1} = e^{\frac{v_1}{v} - 1}.$$

Table A.3.1 shows the required inlet thickness ratio to obtain an increased velocity for the same pressure stress (normalized to the case of the uniform wall thickness).

Table A.3.1. The ratio of inlet thickness for tapered wall to that for uniform wall

v_1/v	t/t_1
1	1
1.2	1.22
1.5	1.65
1.7	2.01
2	e

This result has been derived without any requirement on the fashion in which B varies with distance. If B is assumed to be invariant with distance, then it follows from Eq. (A.3.2) that the thickness variation is exponential with distance. As discussed in Sect. 3.3, there is precedent for the fabricability of stainless steel tubes having a wall thickness which tapers with length.

Results similar to those in Table A.3.1 may be obtained when the stress or the magnetic field is assumed to vary, rather than the velocity of flow.

Fig. A.3.1. Pressure stresses along the length of a duct in a magnetic field.

A similar derivation can achieve the result

$$\frac{t}{t_1} = e^{\frac{s}{s_1} - 1},$$

where s/s_1 is the desired stress ratio (<1) and t/t_1 is the required thickness ratio (>1) at the entrance to produce the reduced stress. As before it is assumed here that towards the upstream end there is a length which varies in thickness so as to produce constant stress. Table A.3.2 results.

Table A.3.2. Thickness increase (at inlet) to achieve a specified pressure stress reduction

s/s_1	t/t_1
1	1
0.75	1.4
0.6	1.95
0.5	e

A.4 DETERMINATION OF DIAMETRAL ΔT FOR LAMINAR FLOW

A.4.1 METHOD FOR DETERMINING TEMPERATURE DISTRIBUTION

Solution of temperature distribution for the one-dimensional problem¹² involves the differential equation

$$\frac{d^2T}{dy^2} = \frac{1}{\alpha} \frac{dT}{dz} v(y), \quad (\text{A.4.1})$$

where dT/dz is a constant for fully developed flow. Given $v(y)$ with $v(0) = dT/dy(0) = 0 = v(e)$, solution at $z = e$ can be obtained by the definite integral

$$\int_0^e f(y) dy$$

applied twice. It is established that such two-time integration is equal to the moment of area of the right-hand side of Eq. (A.4.1). This principle can follow from consideration of a single differential element as in Fig. A.4.1. The

last result was arrived at using area considerations. It is readily seen that this is the area about the right-hand limit, and it can be applied successively to more elements to be valid for any $f(y)$.

The cases at hand are for different velocity distributions in the gap where $v(0) = v(e) = 0$, and these distributions are symmetrical about $y = e/2$. For $y = 0$ the boundary is adiabatic and $T(0) = T_0$.

The situation is shown diagrammatically in Fig. A.4.2. Using the moment of area argument it is apparent that

$$T(e) - T(0) = \frac{1}{\alpha} \frac{dT}{dz} \bar{ve} \left(\frac{e}{2} \right).$$

We also have that

$$\dot{q}''(a) = k \frac{dT}{dy} \Big|_e = \frac{k}{\alpha} \frac{dT}{dz} \bar{ve}.$$

Combining this with the previous relation yields

$$T(e) - T(0) = \dot{q}''(e) \frac{a}{2k},$$

which is the desired result. This has been verified for three velocity profiles — uniform, parabolic, and elliptical.

Sufficient verification of the validity of this expression when applied to the round tube case was supplied by a computer program prepared by T. J. Huxford of ORNL. With this, it was possible to treat the laminar flow case with:

- (1) axially symmetrical parabolic velocity profile (laminar flow), and
- (2) surface heat flux applied to half the circumference, varying as half a sine wave.

This computation yielded ΔT_d 4% greater than Eq. (1) for the same total heat flow, indicating that the flat plate treatment is an adequate representation of the parallel tube first wall.

A.4.2 NUSSELT NUMBER DETERMINATION

We follow Chapter 8 of Ref. 12, which gives the velocity profile $v(y)$ symmetrical about $y = e/2$ and conduction from $y = e$ into the stream

$$f(y)$$

$$\int_0^e f(y) dy = f(y_1) \Delta y$$

$$\int_0^e \left[\int_0^y f(y) dy \right] dy = f(y_1)(e - y_1) \Delta y$$

Fig. A.4.1. Illustrating equivalence of integration and moment of area.

$$\frac{d^2 T}{dy^2} = \frac{1}{\alpha} \frac{dT}{dz} v(y)$$

$$\left. \frac{dT}{dy} \right|_e = \frac{1}{\alpha} \frac{dT}{dz} \int_0^e v(y) dy = \frac{1}{\alpha} \frac{dT}{dz} \bar{v} e$$

Fig. A.4.2. Showing velocity and temperature profiles between flat plates.

filling the region $y = 0$ to $y = e$ (see Fig. A.4.3). The steps are:

- (1) Calculate $\bar{v}$.
- (2) From the Eq. $d^2T/dy^2 = (v/a) (dT/dz)$ (dT/dz constant), integrate twice and use boundary conditions to get $T(y)$.
- (3) Calculate mixed mean temperature $T_m \equiv 1/e\bar{v} \int_0^e vTdy$.
- (4) We then have $T(e) - T_m$.
- (5) Equate $h[T(e) - T_m] = \dot{q}'' = k (dT/dy)|_e$.
- (6) This gives he/k , and using the hydraulic diameter concept we have hd/k . For the uniform and parabolic velocity profiles this was possible analytically. Numerical integration was necessary for the elliptical case.

The results are:

Case 1: uniform velocity

$$v(y) = \bar{v}$$

Fig. A.4.3. Showing velocity and temperature profiles between flat plates.

$$T(y) = T_0 + \frac{\bar{v}}{\alpha} \frac{dT}{dz} \frac{y^2}{2}$$

$$Nu = 6$$

Case 2: parabolic velocity profile

$$v(y) = 6\bar{v} \left(\frac{y}{e} - \frac{y^2}{e^2} \right)$$

$$T(y) = T_0 + \frac{6}{\alpha} \frac{dT}{dz} \bar{v} \left(\frac{y^3}{6e} - \frac{y^4}{12e^2} \right)$$

$$Nu = 5.385 \text{ (tabulated in Ref. 12)}$$

Case 3: elliptical velocity profile

$$v(y) = \frac{8\bar{v}}{\pi e} \sqrt{ey - y^2}$$

$$T(y) = T_0 - \frac{4\bar{v}}{\pi e \alpha} \frac{dT}{dz} \left[\frac{1}{3}(ey - y^2) \sqrt{ey - y^2} + \frac{e^3}{8} \left(1 - \frac{2y}{e} \right) \arccos \left(1 - \frac{2y}{e} \right) - \frac{e^2}{4} \sqrt{ey - y^2} \right]$$

$$Nu = 5.601$$

We may use the two relations derived in this appendix

$$\dot{q}'' = \frac{2k}{a} [T(e) - T_0]$$

$$\dot{q}'' = h[T(e) - T_m] = \frac{Nu}{2} \frac{k}{e} [T(e) - T_m]$$

to obtain, for the elliptical case,

$$\frac{T(e) - T_m}{T(e) - T_0} = \frac{2}{2.8005} = 0.714$$

A.5 ASME CODE TREATMENT AND OUTLINE OF DETAILED CALCULATIONS

Section III (Nuclear Power Plant Components) of the ASME Boiler and Pressure Vessel Code has evolved into an exacting, conservative set of procedures to ensure safe design of metal components subjected to pressure (primary) and cyclic thermal (secondary) stress. It deals with multi-axial stresses as well as ratcheting and deals mostly in terms of apparent elastic stress, where the formalism acts as though the material is elastic, but the properties treatment properly

accounts for inelastic action. It has allowable materials data for annealed 316 stainless steel. The suggested material for our purposes is 20% cold-worked rather than annealed. Based on limited data, comparison of fatigue properties of 316 stainless steel in the annealed and 20% cold-worked states²⁴ shows only a slight difference. The annealed shows slightly better fatigue resistance for low cycles, and the cold-worked is better at high cycles. Our concern is about 2.5×10^5 cycles, so this tends to put us in a slightly conservative position. The approach has two major deficiencies:

- (1) The code has no treatment of radiation effects whatever. Lack of specific data and scope limitations for this study require leaving this area untouched, beyond saying that irradiation consequence is somewhat similar to cold-work, which, in light of the above comparison of cold-worked and stainless properties, may not be so bad.
- (2) The code is almost literally a cookbook, formulated to permit little deviation and to have legal force. Consequently, obtaining a comprehension in terms of engineering basics from it is almost impossible. It is, however, concise. Its application in what follows both suffers and benefits from these factors. Clearly one must gain some familiarity with the code to follow this treatment.

Outline of Calculations

The intent here is to provide the bare information necessary for the reader who seeks to comprehend or check specific numbers.

The equations used are:

$$\frac{\dot{Q}}{L} = \dot{q}''_d + \frac{\pi}{4} d^2 \dot{q}''_{Li} + d T_w \dot{q}''_{ss} \quad (A.5.1)$$

$$\dot{Q} = \rho v A c_p \Delta T_r \quad (A.5.2)$$

$$\Delta T_f = 0.714 \dot{q}''_d \frac{d}{2k} \quad (A.5.3)$$

$$\Delta T_d = \dot{q}''_d \frac{d}{2k} \quad (A.5.4)$$

$$S_p = \sigma_w v / B^2 d L \quad (A.5.5)$$

$$S_d = \frac{2}{3} E \alpha \Delta T_d \quad (A.5.6)$$

$$S_w = \frac{1}{2(1-\nu)} E \alpha \Delta T \quad (A.5.7)$$

$$PPF = \frac{\Delta p}{\rho c_p \Delta T_r} \quad (A.5.8)$$

$$\frac{dp}{dx} = \frac{f}{d} \rho \frac{v^2}{2} \quad (A.5.9)$$

$$Nu = \frac{hd}{k} = 0.022 Re^{0.8} Pr^{0.6} \quad (A.5.10)$$

With one exception, these equations are either arrived at in this paper or are widely enough accepted as to require no justification. That exception is Eq. (A.5.6) for the first wall tubes, which is justified at the end of this appendix.

A.5.1 TUBE BUNDLE BLANKET WITH FIRST WALL COMPOSED OF 1.5-cm-diam TUBES

These tubes are arranged so that 6-cm tubes carry the lithium to the vicinity of the first wall, and then the flow makes a transition from one 6-cm tube to eight 1.5-cm tubes. The reverse transition occurs after the 1.5-cm tubes have completed half of the circumference around the blanket.

Use of the peak value of the three "q" quantities in Eq. (A.5.1) with Eq. (A.5.2) and assuming a value of $\Delta T_r = 175^\circ\text{C}$ yields $v = 1.54$ m/sec. Equation (A.5.3) is applied to yield $\Delta T_f = 89^\circ\text{C}$. Using an inlet temperature of 236°C (melting point plus 50°C), we obtain a peak material temperature of 500°C ($236 + 89 + 175$). Equations (A.5.4) and (A.5.6) yield $S_d = 252$ MPa (36,600 psi). The conduction temperature drop through the first wall (thickness = 0.75 mm) is calculated as $\Delta T_w = 28^\circ\text{C}$. Equation (A.5.7) yields $S_w = 68$ MPa (9900 psi). Using Eq. (A.5.5) with $\sigma_w = 1/1.1 \times 10^{-6}$ mho/m, $v = 1.54$ m/sec, and $B^2 d L = 66.1$ T²m, we obtain $S_p = 92.5$ MPa (13,400 psi). The pressure drop through the series 6-cm

tube forming a return line ($v = 0.77$ m/sec) adds to that incurred in the 1.5-cm tube to give a peak pressure stress of $S_p = 100$ MPa (14,500 psi), which corresponds to a pressure of 10.0 MPa (1450 psi) at the inlet to the 1.5-cm tube.

The 6-cm return tube is treated using Eqs. (A.5.1) and (A.5.2), where $\dot{q}''$ is zero and the other q 's are reduced in accordance with exponential relations in Sect. 2.1. Using $v = 0.77$ m/sec, we obtain $\Delta T_r = 53^\circ\text{C}$. Using Eq. (A.5.8), we obtain $\text{PPF} = 2.3\%$. Assuming a cycle efficiency of $1/3$, we conclude that 6.9% of the electrical energy produced by this stream is used for pumping.

In Fig. 5, the tubes labeled A have higher power density than those in the following calculation, and it turns out that they can make only one circuit of the blanket. Velocity is low enough that these tubes are less stringent in terms of pressure stress than for the following. Those next furthest into the blanket average a depth below the blanket of 0.12 m and the two values of $\dot{q}''$ are reduced by the factor given by the exponential in Sect. 2.1. For two complete turns we have (assuming that $\Delta T_r = 300^\circ\text{C}$) from Eq. (A.5.1) and Eq. (A.5.2) that $v = 0.48$ m/sec. For the four half-turns $\int B^2 d\ell = (4 \times 72.7 + 4.4) = 295 \text{ T}^2\text{m}$, and Eq. (A.5.5) gives $S_p = 129$ MPa (18,700 psi) and $\Delta p = 4.3$ MPa.

The following is a check of calculated stresses per ASME Boiler and Pressure Vessel Code, Section III, Subsection NA and NB. This follows the procedure outlined in Sec. NB-3222.4 and .5 and Fig. NB-3222.1. Materials properties are taken from Subsection NA. The symbols and units are not those of this report, but rather those of the code for the convenience of the rare reader who will delve that deeply. For tabulated stresses for power-on and power-off conditions, a "+" indicates tension and a "-" indicates compression. The point under study is that on a first wall tube at the inlet to the 1.5-cm tube and nearest the plasma.

	σ_r	σ_θ	σ_z	
power-on	0	+14,500	+7,250	pressure stress
	0	-36,600	0	due to ΔT_d
	0	-12,100	-12,100	due to ΔT_w
	Σ	0	-34,200	-4,850
		$S_{z\theta} = +29,350$		
		$S_{zr} = -4,850$		
		$S_{\theta r} = -34,200$		
power-off	σ_r	σ_θ	σ_z	
	Σ	0	+14,500	+7,250
		$S_{z\theta} = -7,250$		
		$S_{zr} = +7,250$		
		$S_{\theta r} = +14,500$		
		Maximum range of S_{ij} is for $S_{z\theta}$ and is 36,600 psi.		

The following comparison of numbers follows the table in Fig. NB-3222.1:

- Step 1: $P_e = 36,600 < 3 S_m = 58,500 \quad \checkmark$
 Step 2: $P_m + P_e + Q = 34,200 < 3 S_m \quad \checkmark$
 Step 3: $P_m + P_e + Q_{\text{range}} = 36,600 < 2 S_a = 64,000 \quad \checkmark$
 Step 4: ratcheting check (NB-3222.5)

$$x = \frac{14,500}{1.5 S_m} = \frac{14,500}{1.5 \times 19,500} = 0.50$$

$$y'_{\text{allowable}} = 2.7$$

$$y'_{\text{actual}} = \frac{36,600}{1.5 S_m} = 1.25 < 2.7 \quad \checkmark$$

Step 3 is in actuality a fairly straightforward fatigue analysis which uses the allowable fatigue stress in Subsection NA. $S_a = 32,000$ psi is for a number of cycles of 2.5×10^5 .

A.5.2 HELIUM-COOLED FIRST WALL COMPOSED OF 3-cm TUBES WITH 1-mm WALL

Helium pressure assumed as 6.89 MPa (1000 psi) gives pressure stress of 103 MPa (15,000

psi), and v is taken as 125 m/sec (265 ft/sec) with viscosity of 2.8×10^{-5} Pa (1.88×10^{-5} lb/sec ft). This gives $Re = 5.57 \times 10^5$; using $f = 0.013$ gives [using Eq. (A.5.9)] $dp/dx = 9160$ Pa/m (58.3 lb/ft³). PPF for a 13-m (40-ft) path length from Eq. (A.5.8) is 2.2%. Equation (A.5.2), using a surface heat flux on the 3-cm tube of 78 W/cm² with $c_p = 5028$ J/kg°C allows calculating $\Delta T_r = 147^\circ\text{C}$. Using Eq. (A.5.10) with $k = 841$ W/m°C (0.135 Btu/hr ft°F) multiplied by 1.18 (to account for asymmetrical heating as discussed in Sect. 3.4) we obtain $\Delta T_f = 164^\circ\text{C}$. Taking the inlet temperature as 150°C , we obtain the maximum temperature of 461°C . Using $\Delta T_f = 164^\circ\text{C}$ in Eq. (A.5.6), we obtain $S_b = 352$ MPa ($51,100$ psi), and applying Eq. (A.5.7) as in Sect. A.5.1 we obtain $S_w = 83.4$ MPa ($12,000$ psi) using $\Delta T_d = \Delta T_f$.

Applying the ASME Code as in Sect. A.5.1, we find acceptable conditions, with the two criteria closest to being unacceptable as:

$$\text{Step 2: } P_m + P_e + Q_{\text{range}} = 63,200 \text{ psi} < 2 S_a = 64,000 \quad \checkmark$$

and for the ratcheting check

$$x = \frac{15,000}{1.5 S_m} = \frac{15,000}{1.5 \times 19,500} = 0.51$$

$$y'_{\text{allowable}} = 0.49 \times 5.2 = 2.55$$

$$y'_{\text{actual}} = \frac{63,200}{1.5 \times 19,500} = 2.16 < 2.55 \quad \checkmark$$

A.5.3 THE CASE WHERE THE FIRST WALL CONSISTS OF SINGLE TUBES 3 cm IN DIAMETER WITH FLOWING LITHIUM

These first wall tubes pass for one-half turn while exposed to the surface heat flux. The analysis proceeds exactly as in Sect. A.5.1, except the surface heat flux (0.78×10^6 W/m²) is reduced to 0.62×10^6 W/m² to bring the cyclic thermal stress to a value which is acceptable ($56,700$ psi) compared to the value of $2 S_a = 64,000$ psi.

A.5.4 JUSTIFICATION FOR EQ. (A.5.6) FOR THE FIRST WALL TUBES

It is assumed here that the temperature distribution is parabolic across the diameter. If it is further assumed that the axial force on the tube cross section is zero, but that the bending moments are not reduced at all, elementary elastic thermal stress analysis produces Eq. (A.5.6) as the maximum, longitudinal, uniaxial stress in the tube.

REFERENCES

1. D. Steiner et al., *ORNL Fusion Power Demonstration Study: Interim Report*, ORNL/TM-5813, Oak Ridge National Laboratory (March 1977).
2. J. C. R. Hunt and R. J. Holroyd, *Applications of Laboratory and Theoretical MHD Flow Studies in Fusion Reactor Technology*, Culham Laboratory (May 1977).
3. M. A. Hoffman and G. A. Carlson, *Calculation Techniques for Estimating the Pressure Losses for Conducting Fluid Flows in Magnetic Fields*, UCRL-51010, Lawrence Livermore Laboratory (February 1971).
4. G. A. Carlson, "Magnetohydrodynamic Pressure Drop of Lithium Flowing in Conducting Wall Pipe in a Transverse Magnetic Field," *Proc. of the First Topical Meeting on the Technology of Controlled Nuclear Fusion*, April 16-18, 1974, San Diego, California, CONF-740402-P1.
5. J. C. R. Hunt and R. Hancox, *The Use of Liquid Lithium as Coolant in a Toroidal Fusion Reactor*, Culham Laboratory Report CLM-R115, 1971.
6. R. W. Werner, *ORNL Fusion Power Demonstration Study: The Concept of the Cassette Blanket*, ORNL/TM-5964, Oak Ridge National Laboratory (October 1977) to be published.
7. W. D. Cain (ORNL), private communication, 1977.
8. R. T. Santoro (ORNL), private communication, 1977.
9. A. G. Grindell (ORNL), private communication, 1977.
10. Larry Shaheen (Superior Tube Co., Norristown, PA), private communication, 1977.
11. J. A. Shercliff, *A Textbook of Magnetohydrodynamics*, p. 174, Pergamon Press, Great Britain, 1965.
12. W. M. Kays, *Convective Heat and Mass Transfer*, 1st ed., p. 116, McGraw-Hill, New York, 1966.
13. *Properties of Inorganic Energy Conversion and Heat Transfer Fluids for Space Application*, WADD-TR-61-16, Wright Air Development Division, USAF, Dayton, Ohio (November 1961).
14. G. G. Branover and E. V. Shcherbin, "Magnetohydrodynamic Jet Flow in a Bounded Space," *Magnetohydrodynamics* 2 (2), 55 (1966).
15. C. J. N. Alty, "Magnetohydrodynamic Duct Flow in a Uniform Transverse Field of Arbitrary Orientation," *J. Fluid Mech.* 48, Part 3, 429-461 (1971).
16. J. C. R. Hunt and G. S. S. Ludford, "Three Dimensional MHD Flows with Strong Transverse Magnetic Fields, Part I, Obstacles in a Constant Area Channel," *J. Fluid Mech.* 33, Part 4, 693 (1968).
17. G. Yonas, "Measurements of Drag in a Conducting Fluid with an Aligned Field and Large Interaction Parameter," *J. Fluid Mech.* 3, 37 (1971).
18. J. C. R. Hunt and J. A. Shercliff, "Magnetohydrodynamics at High Hartmann Number," *Annu. Rev. Fluid Mech.* 3, 37 (1971).
19. I. Michiyoshi, O. Takahashi, and A. Serizawa, "Natural Convection Heat Transfer from a Horizontal Cylinder to Mercury under Magnetic Field," *Int. J. Heat & Mass Transfer* 19, 1021 (1976).
20. R. M. Alire (LASL), "Transport of Hydrogen in Liquid Lithium," paper submitted to *J. Chem. Phys.*
21. J. T. D. Mitchell and R. Hancox, (Culham Laboratory), "A Lithium-Cooled Toroidal Fusion Reactor," *Proc. 7th Intersociety Energy Conversion Engineering Conference*, p. 1275 (1972).
22. B. Misra, H. C. Stevens, and V. A. Maroni, (Argonne National Laboratory), "Thermal Hydraulic and Power Cycle Analysis of Liquid Lithium Blanket Designs," paper presented at the 1977 National Heat Transfer Conference, August 1977, Salt Lake City, Utah.
23. H. Hasimoto, "Steady Longitudinal Motion of a Cylinder in a Conducting Fluid," *J. Fluid Mech.* 8, 61 (1960).
24. W. J. McAfee (ORNL), private communication, 1977.

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