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**Thermal Response of a
Pin-Type Fusion Reactor Blanket
During Steady and Transient Reactor Operation**

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1. INTRODUCTION

The thermal analysis of the blanket examines both the steady-state and transient reactor operations. The steady-state analysis covers full power and fractional power operation whereas the transient analysis examines the effects of power ramps and blanket preheat. The blanket configuration chosen for this study is a helium cooled solid breeder design. The blanket design is summarized in section 1. In this paper we first discuss the full power, steady-state temperature fields in the first wall, beryllium rods, and breeder rods. Next we examine the effects of fractional power on coolant flow and temperature field distributions. This includes power plateaus of 10%, 20%, 50%, 80%, and 100% of full power. Also examined are the restrictions on the rates of power ramping between plateaus. Finally we discuss the power and time requirements for pre-heating the primary loop from cold iron conditions up to startup temperature (250°C).

The results of the thermal analysis have been used in the structural analysis of the first wall and the beryllium and LiAlO_2 rods [1,2] and the tritium inventory calculations [3].

1.1 Reference Blanket Design

The reference blanket design chosen for this study is a helium cooled solid breeder concept. The breeder material is lithium aluminate (LiAlO_2) which is in the form of metal clad rods. The cladding material is 9-C, a low activation ferritic steel similar to HT-9. A neutron multiplier zone of bare beryllium rods is included to enhance the tritium breeding. The blanket is divided toroidally into 24 sectors each consisting of 45 modules. The sector configuration is shown in Fig. 1 and a single module cross section is shown in Fig. 2.

The coolant flow path starts in the module shell and first wall and then

enters the blanket. It then cools the neutron multiplier zone followed by the tritium breeding zone. Notice that the breeding section is divided into 2 zones. The larger rods, toward the back of the blanket are sized to operate near $T_{MAX} = 1100^{\circ}C$. This reduces the diffusive tritium inventory. Selection of the pin sizes is discussed in section 2.2. Table 1 summarizes the major operating parameters of the blanket design. A more detailed description of this blanket design can be found in Ref. 4.

2. STEADY-STATE THERMAL ANALYSIS

2.1. First Wall

The general configuration of the helium cooled first wall is shown in Fig. 3. This design is similar to the tokamak first wall design chosen for the Blanket Comparison and Selection Study (BCSS) [5]. Typical first wall dimensions are also shown on Fig. 3. The thermal analysis was an iterative process in which work was done in parallel with the first-wall stress analysis (Ref. 1). Optimization of the first wall involved several design variables including peak and average temperature, coolant pressure and pressure drop, peak stress, and first wall lifetime.

First wall optimization includes the effects of two additional configuration changes. First we examined the effects of cooling fins inside the coolant channel. This concept is illustrated in Fig. 3. The other design change is the use of 3 dimensional (3-D) surface roughening. Both of these first wall alterations affect the temperature fields and the coolant pressure drop.

Numerical methods were used to calculate the 2-dimensional temperature fields. The finite element heat conduction code, TACO2D [6], was used for the analysis.

The applied heat loads include a surface heat flux of 1 MW/m^2 and an average volumetric heat generation rate of 60 MW/m^2 . The latter value has been calculated from the neutronics analysis [4] while the former is based upon the BCSS guidelines for a neutron wall load of 5 MW/m^2 .

The equation for the turbulent heat transfer coefficient [Eq. (1)] is obtained from Ref. 7. (NOTE: The nomenclature is summarized at the end of the paper.)

$$h = 0.022 \frac{k}{D_H} \text{Re}^{0.8} \text{Pr}^{0.4} \left(\frac{T_w}{T_f} \right)^{-0.18} \quad (1)$$

The temperature effects on Prandtl number are small over the film temperature drop, therefore:

$$\text{Pr}^{0.4} \text{ (for helium at } 250^\circ\text{C)} \approx 0.85 \quad (2)$$

A further simplification of Eq. (1) can be made in the temperature ratio term. Preliminary analysis has shown that the film temperature drop, $\Delta T_f = T_w - T_f$, on the plasma side of the coolant channel is such that $80^\circ\text{C} < \Delta T_f < 320^\circ\text{C}$. With the bulk coolant temperature, $T_f = 250^\circ\text{C}$ (513 K), we see the following range of the temperature ratio.

$$0.92 < \left(\frac{T_w}{T_f} \right)^{-0.18} < 0.97 \quad (3)$$

In this analysis an average temperature ratio of 0.95 is chosen, thus simplifying Eq. (1) to the following:

$$h = 0.018(k/D_H)\text{Re}^{0.8} \quad (4)$$

In addition to Eq. (4), which applies to smooth wall tubes, we examined

the effectiveness of wall roughness. Specifically, we chose 3-D roughening which approximately triples the heat transfer coefficient [7]. The penalty for this enhanced heat transfer is a seven-fold increase in the friction factor. To minimize the coolant pressure drop due to roughness, we only roughen the side of the coolant channel nearest the plasma since this surface has the highest heat flux entering the coolant.

The other critical thermal-hydraulic parameter is the coolant pressure drop. In general, the pressure drop is given by Eq. (5).

$$\Delta P = (L/D_H) f_s (\rho V^2/2) \quad , \quad (5)$$

The smooth wall friction factor, f_s , is given by [Ref. 8]:

$$f_s = 1/[2 \log_{10} (Re\sqrt{f_s}) - 0.8]^2 \quad . \quad (6)$$

For the cases involving surface roughening, the average friction factor, $\bar{f}$, is estimated using

$$\bar{f} = 7f_s \left(\frac{A_R}{A_W} \right) + f_s \left(\frac{A_W - A_R}{A_W} \right) \quad . \quad (7)$$

Equation (7) states that the average friction factor is equal to the sum of the local friction factors weighted by their area fraction.

The following first wall variations have been examined:

1. No fins, smooth walls,
2. No fins, roughness on front surface,
3. One fin, smooth walls,
4. One fin, roughness on front surface,
5. Two fins, smooth walls,
6. Two fins, roughness on front surface.

The choice of case number 1 allows simple comparison to the ECSS studies. Cases 2 through 6 are included as heat transfer enhancement methods. The desire is to decrease the peak and average first wall temperature without sacrificing first wall thickness. We also investigated the effect of fin length on the maximum and average temperatures and the coolant pressure drop.

Table 2 summarizes the results from cases 1 to 6. Temperature contours for cases 2, 4, and 6 are shown in Figs. 4a, 4b, and 4c, respectively. The fin length variation and its effect on maximum and average temperature and pressure drop are shown in Fig. 5.

The maximum surface temperature of a simple first wall channel can be reduced from 827°C to 605°C with slight modification of the geometry. Three dimensional surface roughening on the high heat flux side of the coolant channel greatly reduces the film temperature drop, thus leading to the lower structure temperature. The addition of internal fins has lesser impact on structure temperature. An optimum choice appears to be Case 4, one fin with roughening. The structure temperature is 621°C, and the required first wall pumping power is 6 MW_e. This is not unreasonable for a 1000 MW_e gas cooled power plant.

2.2. Breeder and Neutron Multiplier

The flow geometry in the breeder and neutron multiplier zones is cross flow of helium around rod bundles. Because the pins are grouped into three zones of different outside diameters, an average heat transfer coefficient for each zone is required. The average heat transfer coefficient for cross flow in rod bundles is given by [9]

$$\bar{h} = 0.33 \text{ CF}(k/D)(G_m D/\mu)^{0.6} \text{ Pr}^{0.3} \quad . \quad (8)$$

Scaling factor C is an empirical coefficient that does not vary significantly from unity. F is a factor that depends on the number of rods in each zone, as shown in Table 3. In our design, F is always between 0.95 and 0.92 for each zone, individually. Since each zone is in close proximity to the others, we will assume that the number of rows of pins that affect the local flow paths is large and therefore F is approximately unity.

The rod lattice for each zone is a triangular pitch. The layout is shown in Fig. 6 with the various dimensions for each of the three zones tabulated in Table 4.

An effect that is only encountered in the breeder pins is multi-layer heat flow. The pins are similar to fission fuel rods with the breeder, LiAlO_2 , surrounded by a helium filled gap and a 9-C cladding. One large uncertainty of this configuration is the conductance of heat across the helium gap. Ideally, we would like no gap. However, assembly of the rods requires a small clearance gap to allow insertion of the breeder into the cladding. An initial gap of 0.1 mm was assumed for this analysis. Based on the BCSS analysis [5] we find that the gap heat flow in our design is governed by the gas phase conductance. This results from the gap size being much larger than the surface roughness. Typical surface roughness is 6×10^{-6} m while the gap is 10^{-4} m. Therefore, in the thermal analysis we model the breeder pin as three concentric cylinders of LiAlO_2 , He, and 9-C.

The first analysis performed was a 1-D heat flow in concentric, volumetrically heated cylinders. The breeder temperature as a function of radius is given by

$$T(r) = T_f + \frac{0_{Li}''' r_o^2 + 0_c''' (r_{co}^2 - r_{cl}^2)}{2r_{co}} \left(\frac{t_c}{k} + \frac{1}{h} \right)$$

$$+ \frac{Q_{Li}^{'''}}{2k_g} r_o t_g + \frac{Q_{Li}^{'''}}{4k_{Li}} (r_o^2 - r^2) \quad . \quad (9)$$

Results of the 1-D analysis are illustrated in Figs. 7a and 7b. Figure 7a presents $T(r)$ for the inner and outer pins of breeder zone 1 (rows 1 and 5) and Fig. 7b presents $T(r)$ for the same pins of breeder zone 2 (rows 6 and 11). Equation 9 has also been used to calculate the temperature profiles within the beryllium rods. The maximum temperature and temperature gradients of the Be are found to be quite small. This is a result of using bare Be rods and the good thermal conductivity of the Be. Typically the maximum Be temperature is 430°C and the minimum is 410°C.

The diameter of the breeder pins in the final design is based on iteration between the neutronics calculations and the blanket heat transfer. The maximum allowable temperature for $LiAlO_2$ is set at 1100°C. For a given volumetric heat generation rate we can use Eq. (9) to calculate the pin diameter that results in a 1100°C centerline temperature. It is conceivable that we could apply Eq. (9) to each row of pins, thereby designing a blanket with pins of many different sizes, and each one having $T_{MAX} = 1100^\circ C$. This would result in a large portion of the breeder material operating near T_{MAX} which results in a minimum blanket tritium inventory. For reasons of blanket design simplicity, we opted for a two-zone blanket model.

The 1-D thermal analysis is sufficient for calculating average temperature, radial temperature gradients, and maximum temperature of each pin. There are, however, 2-D effects that need to be taken into account. Specifically we need to calculate the azimuthal variation of the cladding temperature so that we can determine the pin deflection due to rod bowing. See Ref. 2 for the rod bowing analysis.

The 2-D effects are: azimuthal variation of heat transfer coefficient and coolant temperature, non-uniform helium gap size due to breeder/cladding eccentricity, and variation of the volumetric heat generation rate across the breeder and the cladding. The latter effect is most pronounced in the first row of breeder pins. In these pins, the peak-to-minimum heat generation rate is 2.9.

Of these 2-D asymmetries, the most difficult to quantify is the variation of the heat transfer coefficient. Early experiments on flow around a single cylinder (Lohrish, 1929 [10,11]) have shown that the local Nusselt numbers and hence the local heat transfer coefficients vary azimuthally from 220% (Re = 50,000) up to 475% (Re = 4000). Typical azimuthal variations of Nu and the heat transfer coefficient are shown in Fig. 8 (calculated from Ref. 10). The situation becomes more complicated as we go from a single cylinder to banks of cylinders. Lohrish, also in 1929, performed some experiments with rod bundles that revealed that there was still an azimuthal dependence on h.

Based on Lohrish's result, we chose to let $h(\theta)$ vary in the following fashion:

$$h(\theta) = h_{AVG}[1.5 - 0.625(\theta/90)] \quad ; \quad 0 < \theta < 90^\circ \quad , \quad (10)$$

$$h(\theta) = 0.875 h_{AVG} \quad ; \quad 90^\circ < \theta < 180^\circ \quad , \quad (11)$$

where h_{AVG} is calculated from Eq. (8). Equations (10) and (11) produce a linearly decreasing heat transfer coefficient from the front of the pin to its midplane and a constant h from the midplane to the back of the pin. The variation of h is illustrated in Fig. 9.

The remaining 2-D asymmetries are more easily applied. The neutronics

calculations produce the local volumetric heat generation rates. In the first row of breeder pins, the heat generation within the LiAlO_2 ranges from 60w/cm^3 up to 179 w/cm^3 . The coolant temperature was assumed to vary linearly with angle around the pin. An energy balance on each row of pins yields the total coolant temperature rise per pin. Finally, the eccentricity of the cladding and breeder was modeled as a line-of-contact at the front of the pin and an azimuthally varying gap around the pin. Figure 10 summarizes all the 2-D asymmetries.

The effects of the 2-D asymmetries were examined for three rows of pins. The first row of each breeding zone, since they have the highest volumetric heating in their respective zones, and the last row of pins in the blanket since the cladding temperature is highest. This latter case could cause problems because of the thermal creep of 9-C [2].

The cladding temperature distributions for the three cases are shown in Fig. 11. A typical temperature contour plot is shown in Fig. 12. These contours are for the first row of breeder pins (o.d. = 1.22 cm).

As stated earlier, the temperature profiles have been used to determine the rod bowing deflection due to thermal expansion [2]. The calculations reveal that thermally induced rod deflections are small ($\Delta X \approx 0.5\text{ mm}$) and can be considered to have no consequence on reactor operation.

2.3. Fractional Power Operation

The objective of the blanket thermal analysis during fractional power operation is to determine what changes will occur in the temperature fields and what the resulting effect will be. Initially, we observe that the thermal stresses in the reactor are reduced. Both the surface and volumetric heating are reduced resulting in lower thermal gradients. This leads us to conclude that at low power the reactor should operate quite safely. However, from eqn.

9 we see that the breeder temperature will be lower. The result is a decrease in tritium diffusion and an increase of the diffusive tritium inventory in the blanket.

To model the thermal behavior of the breeder at fractional power we rewrite eqn. 9 as follows:

$$T(r, \eta) = T_F + \left\{ \frac{Q_{Li}''' r_o^2 + Q_c''' (r_{co}^2 - r_{ci}^2)}{2r_{co}} \left(\frac{t_c}{k_c} + \frac{1}{\eta^{0.6} h} \right) + \frac{Q_{Li}''' r_o t_g}{2k_g} + \frac{Q_{Li}'''}{4k_{Li}} (r_o^2 - r^2) \right\} \eta \quad (12)$$

In eqn. 12, η is defined as the operating power divided by the reactors maximum power capacity. For example, if the reactor is operating at 50% power, $\eta=0.5$. Optimum thermal efficiency is obtained if the primary coolant inlet and outlet temperatures are kept constant. Therefore, the coolant mass flow rate must decrease linearly with η . The heat transfer coefficient calculated from eqn. 8 scales as (velocity)^{0.6} which accounts for the $\eta^{0.6}$ scaling factor for h in eqn. 12. As the flow decreases for smaller η the flow regimes change from turbulent to transition to laminar. The transition occurs at $\eta=0.05$ which implies that eqn. 12 is valid for $0.05 < \eta < 1.0$. This range is virtually the full range of power for the reactor.

Figure 13 illustrates the effect of steady fractional power operation on breeder pin temperature. Also shown on Fig. 13 is the total tritium inventory of the blanket (from Ref. 3). We see a pronounced increase in inventory at low power levels because the tritium diffusion coefficient is a strong function of temperature.

One method of decreasing the tritium inventory in the event of prolonged operation at low power is to alter the coolant flow conditions. For example,

at 50% power we can raise the coolant inlet temperature from 250°C to 350°C while maintaining the outlet at 550°C. Now the coolant temperature and the breeder temperature for each row of pins will be higher. The result is a decrease in diffusive tritium inventory. For the specific example given, the tritium inventory decreases by about 20%. The penalty paid for this type of flow control is increased helium pumping power and lower net efficiency.

Another method of decreasing tritium inventory at low power is to periodically allow the breeder rods to run hotter than normal. This could be achieved by decreasing the coolant mass flow rate at prescribed intervals. The tradeoff between plant efficiency and tritium inventory should be examined before adopting either of these methods of control.

3. TRANSIENT THERMAL ANALYSIS

3.1 Power Ramps

As stated in Section 2.3, steady power generation at fractions of full power does not place strict requirements on the operation of the reactor. Increased tritium inventory is the primary concern and judicious plant operation via coolant flow control will reduce that problem.

A more serious concern relating to fractional power operation is the time rate-of-change from one power plateau to the next. In a fusion plasma the rate-of-change of power production can be quite fast. Typically on the time scale of energy confinement, about one second [12]. If the power loading on the first wall and blanket are increased on this time scale severe transient thermal fields could develop. These transients result from the slow fluid response time in the primary loop. The coolant flow time constant can range from 10's to 100's of seconds. The longest time constant is the time required to reach thermal equilibrium after a change in the level of power

production. In the reference blanket design the time constant for thermal equilibrium is governed by thermal diffusion out of the multilayer breeder rods. We will now discuss the calculation and consequences of various time constants.

3.1.1 First Wall Response

The first wall thermal response to abrupt changes in power load followed by slow changes in coolant flow have been studied. Using the finite element model presented in section 2.1 we are able to produce temperature histories for the first wall under various conditions. Two wall load conditions were examined. A severe rate-of-change of power, 10% per second and a moderate rate-of-change, 10% per minute. In combination with these power ramps were the associated changes in coolant flow rate. We looked at three flow ramps; 10% per second, 10% per minute and 1% per minute. The combinations of these rates yields 6 different cases that cover a wide range of operating scenarios. Each case starts at 50% of full power and ramps up to full power. The entire ramping cycle lasts from 5 seconds to 50 minutes depending on the specific case.

The results of these calculations are plotted in Fig. 14. The most severe conditions exist when the change in coolant flow rate is slower than the change in power production. The worst case is case 3 in which the power increases at 10% per second and the coolant flow increases at 1% per minute. In this situation the structure reaches a maximum temperature of 702°C and then slowly cools to 621°C. We conclude from this analysis that the rate of power ramp-up should never be faster than the rate of coolant flow ramp-up. It is desirable to ramp power and coolant flow at the same rate. This dictates that the plasma power ramp rate will be determined by the available coolant flow ramp rate.

3.1.2 Breeder Rod Response

The thermal response of the multilayer breeder rods has been examined. The worst case is a step change in power followed by a slow ramp-up of coolant flow. However, based on the conclusion of the first wall response analysis we only consider moderate and equal ramps of power and coolant flow to be realistic.

The breeder rods studied were the first row of 3 cm diameter pins, row 6. These pins were chosen because they have a longer thermal time constant than the smaller pins in rows 1-5. The ramp rates for power and flow are 10% per minute. The time response of the centerline temperature in the pins is shown in Fig. 15. We see that the centerline temperature follows closely the power ramp and reaches equilibrium at 600 sec. This is 300 sec after the end of the power ramp. It is interesting to note that even for a step change in power and flow the time to reach equilibrium is about 400 sec. This indicates that a safety margin exists within the breeder in the event of unforeseen rapid power excursion.

3.2 Primary Loop Preheat

The final topic of this paper is a discussion of the requirements for primary loop preheating. It is desirable from a reactor operations viewpoint to perform hot functional tests prior to reactor power generation. These tests will ensure that no problem will be produced from thermal expansion and reactor pressurization. To perform the hot functional testing the primary loop must be heated with an auxiliary heat source. For the purposes of plant design and operation it is necessary to know the power and time required for preheat. In this analysis we assume that the primary coolant, helium, is used as the heat transfer medium.

The model of the primary loop and the method of solution are similar to

those developed at UCLA by Taghavi and Ghoniem [13]. In their analysis the individual primary loop components are modeled as thermal capacitance "lumps". The analysis presented here is similar with slight modifications to account for the physical differences in the specific reactor designs.

The lumped capacitance model requires a thermal time constant for each component. This time constant, τ_i is given by Eq. 13.

$$\tau_i = \left(\frac{\rho C_p V}{A h} \right)_i \quad (13)$$

The time constants for the primary loop components are tabulated in Table 5.

All of the primary loop components are initially at ambient temperature, 20°C. During the hot functional testing we intend to operate all components at 250°C. Thus, our task is to determine the time required to reach this temperature and the total energy needed.

Since we assume that there are no heat loss mechanisms, the preheat energy is easily determined. The energy required is given by Eq. 14.

$$E = \sum_i^N (m C_p)_i \Delta T \quad (14)$$

where i represents an individual component and $\Delta T = 230^\circ\text{C}$. Evaluating the summation for the conditions specified gives the total energy required, 9.1×10^{10} joules (25.3 MW · hr). We must next calculate the time required to preheat. From this information we can determine what the heater power must be.

We will assume that the heater can continually produce hot helium with an outlet temperature of 400°C which is 150°C hotter than the desired primary loop final temperature. Figure 16 illustrates the temperature history of the

primary loop components during preheat. We see from Fig. 16 that the first half of the primary piping heats quite rapidly, reaching the desired temperature of 250°C in 20 minutes. This is expected since there is always hot gas (400°C) in contact with this structure. The blanket modules reach 250°C after 180 minutes of preheat. The average thermal power required for this cycle is 8.4 MW.

It should be noted that during most of the preheat cycle the temperature of the first half of the primary piping is 400°C, which is 150°C hotter than during normal operation. If this is unacceptable, then it may be desirable to heat up the primary loop in reverse order. Thus we would heat up the hot leg of the primary piping first, then the blanket and finally the cold leg of the primary. Now the hot leg, which normally operates at 550°C, will be at 400°C for the majority of the preheat cycle.

4. SUMMARY

Detailed thermal analysis has been performed on a pin-type solid breeder blanket. A first wall with internal fins and 3-D surface roughening is used. The maximum temperature is 620°C and at the beginning-of-life there is 5 mm of structure between the coolant and plasma. The breeding blanket is divided into two zones of 9-C clad LiAlO_2 pins. The pins in each zone are sized to operate near T_{MAX} of the breeder (1100°C).

Fractional power operation of the blanket modules was examined. During steady, fractional power operation no adverse effects are seen except for increased tritium inventory due to lower average temperatures within the breeder. A more interesting condition is observed during power ramps between fractional power plateaus. Specifically, when the fusion power and coolant flow are not increased at the same rate, the first wall experiences atypical

temperature behavior. This condition could lead to structure temperatures that are higher than the allowable.

Finally, we examined the requirements of primary loop preheat. The results indicate that 3 hours of preheat operation will heat the loop to 250°C with an average power consumption of 8.4 MW.

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NOMENCLATURE

A	area, m^2
A_R	roughened area of first wall channel, m^2
A_W	total first wall channel area, m^2
C	empirical coefficient for flow across rod bundles
c_p	specific heat, J/Kg K
D_H	hydraulic diameter, m
F	scaling factor for flow across rod bundles
f_s	friction factor for smooth tubes
G_m	mass flow rate per unit area, $Kg/m^2 s$
h	heat transfer coefficient, $w/m^2 K$
k	thermal conductivity, $w/m K$
k_c	thermal conductivity of the cladding, $w/m K$
k_g	thermal conductivity of the helium filled gap, $w/m K$
k_{Li}	thermal conductivity of the $LiAlO_2$, $w/m K$
L	length, m
m	mass, Kg
Δp	pressure drop, Pa
Pr	Prandtl number
Q'_c	volumetric heat generation rate in the cladding, w/m^3
Q'_{Li}	volumetric heat generation rate in the $LiAlO_2$, w/m^3
Re	Reynolds number
r_{ci}	inner radius of the cladding, m
r_{co}	outer radius of the cladding, m
r_o	outer radius of the $LiAlO_2$, m
T_f	bulk coolant temperature, K
ΔT_f	coolant film temperature drop, K
T_w	wall temperature, K
t_c	cladding thickness, m
t_g	gap thickness, m
V	volume, m^3
ρ	density, Kg/m^3
n	fraction of full power ($0 \leq n < 1$)
θ	angle measured around pins, degrees
τ	thermal time constant, s
μ	viscosity, $Kg/m s$

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TABLE 1

Reactor type	Continuous burn d-t tokamak with a helium cooled solid LiAlO ₂ blanket
Major radius	5.7 m
Minor radius	1.56 m
Elongation	1.6
Number of toroidal sectors	24
Number of blanket modules/sector	45 (full poloidal coverage)
Blanket module type	Helium cooled, pressurized module of 9-C structure (low activation)
Module poloidal height	0.28 m
Module mean toroidal length	1.50 m
Fusion power	2850 MW
Thermal power	3463 MW
First wall 14 MeV neutron flux	5 MW/m ²
First wall surface heat flux	1 MW/m ²
Coolant pressure	5 MPa
Coolant inlet temperature	250°C
Coolant outlet temperature	550°C
Coolant mass flow rate	2.21 x 10 ³ kg/s
Thermal efficiency, gross	35%
Electric output, gross	1034 MW

TABLE 2

CASE NO.	DESCRIPTION	$T_{MAX}(^{\circ}C)$	$V_{He}(m/s)$	$\Delta p(MPa)$	$P_{pump}(MWe)$
1	Simple Channel, Smooth	827	76	0.0023	1.7
2	Simple Channel, Rough	676	76	0.0049	3.6
3	One Internal Fin, Smooth	703	91	0.0037	2.7
4	One Internal Fin, Rough	621	91	0.0080	5.8
5	Two Internal Fins, Smooth	651	127	0.0255	18.9
6	Two Internal Fins, Rough	605	127	0.0548	40.6

TABLE 3

NUMBER OF ROWS	1	2	3	4	5	6	7	8	9	10
F (TRIANGULAR LATTICE)	0.68	0.75	0.83	0.89	0.92	0.95	0.97	0.98	0.99	1

TABLE 4

COMPOSITION	d*	s*	% Struc.**	% Void	Re
BERYLLIUM	1.7	0.3	66	34	26,900
LiAlO ₂ /9-C	1.22	0.2	67	33	16,100
LiAlO ₂ /9-C	3.00	0.3	75	25	67,700

*centimeters, **breeder + cladding

TABLE 5

Component, i	τ_i (sec)*	Total Number of Components with τ_i
1/2 Primary piping	750	2
First wall plus module shell	57	1,080
Beryllium rods	24	64,800
LiAlO ₂ rods (OD = 1.22 cm)	45	86,400
LiAlO ₂ rods (OD = 3.0 cm)	150	45,360

*Time constant τ is defined as $\tau = \frac{\rho c_p V}{hA}$.

FIGURE CAPTIONS

- Fig. 1 One of 24 toroidal blanket sectors [5].
- Fig. 2 Cross section of a single blanket module.
- Fig. 3 First wall configuration. Shown are the three cases studied, no fins and one or two internal fins.
- Fig. 4 First wall temperature contours for the case with no internal fin (a), one internal fin (b) and two internal fins (c). All three cases had surface roughening on the plasma side of the coolant channel.
- Fig. 5 The effect of internal fin length on maximum first wall temperature and helium pumping power (first wall only).
- Fig. 6 Rod lattice used in the beryllium zone and both LiAlO_2 zones. See Table 4 for specific dimensions.
- Fig. 7 Radial temperature gradients in the two breeder zones. Curves A and B in Fig. 7a are for the pins in rows 1 and 5, respectively, and Curves A and B in Fig. 7b are for the pins in rows 6 and 11, respectively. Row numbers are defined in Fig. 2.
- Fig. 8 Azimuthal variation of Nusselt number for flow around a single cylinder.

Fig. 9 Azimuthal variation of Nusselt number for flow normal to rod bundles.

Fig. 10 Summary of the 2-D asymmetries found on the breeder pins. Included are non-uniform heat generation, azimuthal variation of the heat transfer coefficient, helium gap eccentricity and increased coolant temperature around the pin.

Fig. 11 Azimuthal variation of cladding temperature for breeder pins in row 1 (curve A), row 6 (curve B) and row 11 (curve C).

Fig. 12 Temperature contours for breeder pins in row 1 (symmetric half shown). The jagged horizontal lines define zones of different rates of volumetric heat generation used in the finite element analysis.

Fig. 13 Effect of steady, fractional power operation on the maximum and average breeder temperatures and the total blanket tritium inventory.

Fig. 14 Thermal response of the first wall surface temperature under various power and coolant flow ramps.

- A. Power increases at 10%/sec,
Flow increases at 1%/min
- B. Power increases at 10%/sec
Flow increases at 10%/min
- C. Power increases at 10%/sec
Flow increases at 10%/sec
- D. Power increases at 10%/min
Flow increases at 10%/sec

Note that the maximum first wall temperature for full power, steady state operation is 621°C.

Fig. 15 Response of the breeder centerline temperature to a 10% per minute increase in fusion power. The dashed line shows the temperature response following a step-change in power production.

Fig. 16 Temperature response of the primary loop during preheat.

Fig. 1

Fig. 2

HELIUM COOLED FIRST WALL

Fig. 3
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a.

b.

c.

CONTOUR	TEMPERATURE (C)
A	260
B	280
C	300
D	320
E	340
F	360
G	380
H	400
I	420
J	440
K	460
L	480
M	500
N	520
O	540
P	560
Q	580
R	600

Fig. 4

Fig. 5

Fig. 6

Fig. 7b

Fig. 7a

Fig. 7 a and b

Fig. 8

Fig. 9
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CLAD LOC.	ANGLE FROM FRONT	$h(\theta)/h_{avg}$
1	0 degrees	1.50
2	45	1.298
3	90	0.875
4	135	0.875
5	180	0.875

Fig. 10

Fig. 11
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Fig. 12

Fig. 13

Fig. 14

Fig. 15

Fig. 15

Fig. 16

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