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Transient flow behavior for a helium-cooled ceramic breeder blanket *

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This paper presents a thermal-hydraulic analysis of a helium-cooled ceramic breeder blanket for the ITER. It is found that the blanket can operate at moderate pressure and temperatures, which coupled to the use of an inert gas, makes it particularly attractive from a safety standpoint. Transient analysis of the manifolding flow distribution indicates that the introduction of a diffusive plate at the inlet of the blanket coolant flow path produces a more uniform coolant exit temperature. Control of the pump pressure head ensures that the coolant temperature is kept below its maximum limit, set by the maximum allowable operating temperature of the breeder.

1. Introduction

The helium-cooled ceramic breeder blanket design proposed for the International Thermonuclear Experimental Reactor (ITER) and considered here consists of blanket sectors outboard of the reactor, providing good coverage of the torus surface [1]. As shown in fig. 1, each sector comprises several independent blanket canisters lying side by side, which are individually cooled by helium supplied through an inlet manifold and an inlet distribution channel. The coolant manifolds, which run poloidally, surround the sector at the rear of the canisters. This type of design can be characterized as a large number of flow channels having common inlet and outlet plenums operating hydraulically in parallel, all having the same pressure drop. In this connection, the relationship between flow rate and pressure drop plays an important role, in particular when the heating rate varies from canister to canister. The neutron wall load generally peaks at the mid-plane of the torus and decreases with the poloidal distance from the plasma center. While it is conceivable that one could tailor the blanket canister so as to obtain a poloidally flatter power distribution, this solution could only be achieved at the expense of a more complex design. An alternative measure would be to devise a means by which the cooling in each module can be controlled either actively or passively.

This paper presents a thermal-hydraulic analysis of the proposed blanket design. Trade-off studies for the helium coolant pressure and temperature based on pressure drops, pumping power and maximum allowable temperature constraints are discussed first. Results from transient analyses for the manifolding flow behavior are then shown with the aim of comparing the uncontrolled-flow case with a proposed controlled-flow case based on balancing the flow distribution in the blanket regions according to the corresponding power level.

1.1. Canister description

A schematic diagram of a blanket canister is shown in fig. 2. A rod bundle configuration is chosen for several reasons such as the flexibility that it provides of varying the individual rod sizes and compositions in different rows to allow for the exponential decrease in heat generation in the radial direction while maintaining the solid breeder at an acceptable temperature. Each rod consists of a clad Li_4SiO_4 solid breeder inner cylinder with a clad beryllium packed bed region on the outside. Both the solid breeder and the beryllium regions are purged by helium gas to remove the tritium produced during operation. The use of beryllium particles and helium in the region between the solid breeders and helium coolant is to provide the required thermal resistance between the high-temperature solid breeder

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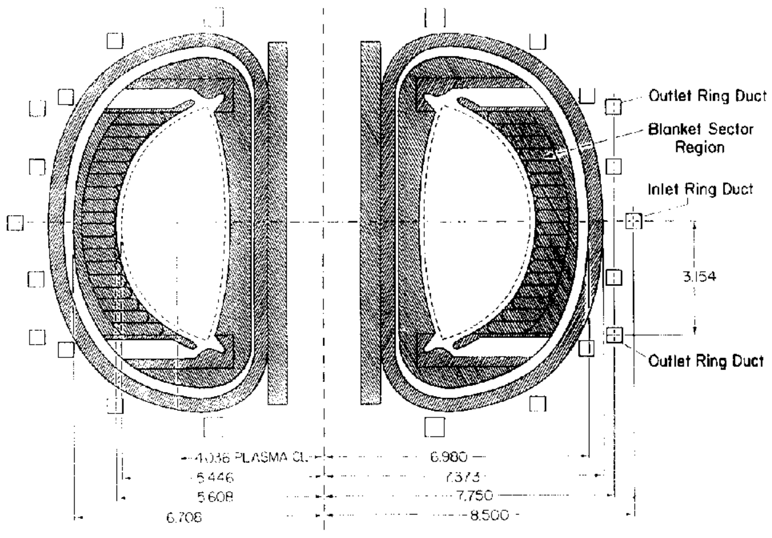

Fig. 1. Cross section of ITER showing canister layout.

(required for tritium release purposes) and the lower-temperature coolant. In addition, it offers several advantages such as the possibility of controlling the Be/He region thermal conductivity through purge flow adjustment and good tritium breeding capabilities. A complete description and an overall analysis of the blanket configuration can be found in ref. [1].

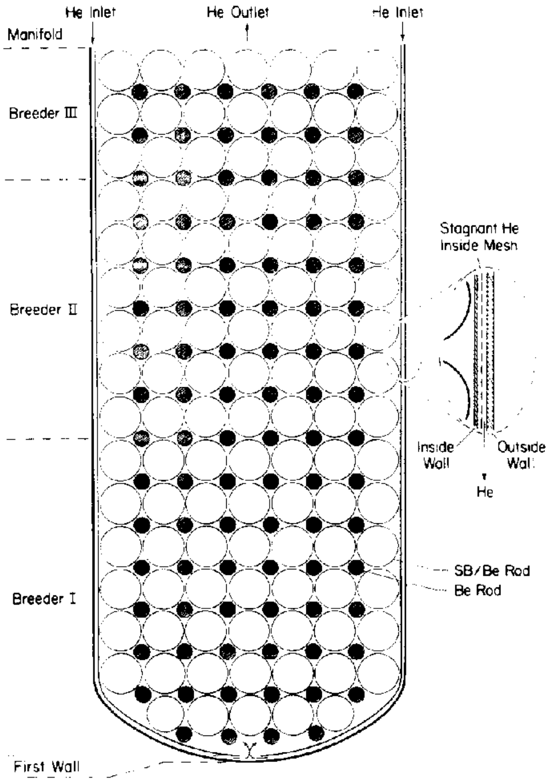

Fig. 2. Helium-cooled solid breeder canister for ITER.

2. Helium operating conditions

2.1. Pressure drop and pumping power

The power density for the ITER is substantially lower than that of a typical fusion power reactor and, the blanket is used purely for tritium production, not for power generation. Thus, an attractive solid breeder blanket would have the solid breeder operating at high temperature for tritium release with the coolant operating at moderate pressure and temperature for safety. Helium, being an inert gas, is particularly attractive as a coolant provided its operating temperature and pressure are reasonable. As shown in fig. 2, the main helium coolant flow comes in along the side wall to provide for the cooling of the first wall and then enters the main canister at the first wall and flows radially over the rod arrangement before leaving the canister at the rear. For a given power density and fractional flow area, the major parameters influencing the choice of the flow inlet pressure and the inlet and outlet temperatures are the flow pressure drop and the required pumping power. From ref. [2], the average neutron wall loading for the outboard is about 1.5 MW/m^2, the average heat flux to the first wall is 0.2 MW/m^2, and the energy multiplication factor is 1.4 . This results in an average power in the outboard blanket and shield of about 2.2 MW per unit area (m^2) of the first wall. The fractional flow area is a function of the first wall channel size, the spacing between the rods and the size of the rods. For compactness reasons, it is desirable to maximize the volume fractions of the beryllium and solid breeder in the canister and hence to minimize the spacing between the rods; however, if the spacing is too small, there is the concern that thermal expansion and/or swelling might cause adjacent rods to touch and create hotspots even if there are spacers. Based on these considerations, a 2 mm spacing seems reasonable and was chosen for the design calculations. A rod diameter of 4 cm is assumed, based on considerations of maximization of space utilization, neutronics and temperature profile considerations [3]. The resulting canister fractional flow area is 5.4%. The side wall fractional area is 1.8% if a 3 mm channel thickness for the side walls and the first wall is assumed, subject to verification based on the resulting velocity and pressure drop in the side walls.

The helium mass flow rate in each canister is calculated from an energy balance for the prescribed power generation and a given temperature rise. The helium velocities in the first-wall channel and the canister can then be calculated based on the local density and flow area. The friction pressure drop per unit length of the first wall, ΔP_{fw}, is calculated by using the friction factor (f) based on the Moody diagram [4] for an effective relative roughness of 0.015 (assuming a mesh filament diameter of 8 μm):

$$\Delta P_{fw} = \frac{f}{d_H}\left(\frac{\rho V^2}{2}\right)_{fw} \tag{1}$$

The friction pressure drop per unit length of the canister, ΔP_{can}, is given by

$$\Delta P_{can} = 2f'(\rho V^2)_{can} N;$$
$$f' = 0.25 + \frac{0.118}{\left[(S_T - D)/D\right]^{1.08}} \mathrm{Re}^{-0.16}, \tag{2}$$

where f' is the friction factor for the staggered rod arrangement estimated from ref. [5]. The acceleration pressure drop, ΔP_{acc}, is estimated from the following expression for both the first-wall and the canister flows using the corresponding parameter values:

$$\Delta P_{acc} = (\rho V)^2\left(\frac{1}{\rho_{out}} - \frac{1}{\rho_{in}}\right). \tag{3}$$

The total pressure drop for the canister ΔP_{tot}, can then be calculated by summing its individual components,

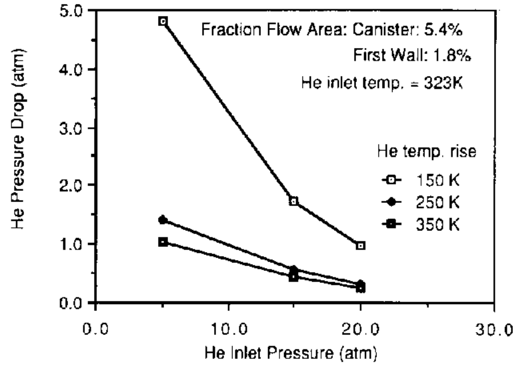

Fig. 3. Canister pressure drop as a function of the helium coolant inlet pressure for different helium coolant temperature rises.

including entry and exit losses assumed as being twice the dynamic pressure. The required pumping power, P_p, is then given by the following expression:

$$P_p = \frac{\Delta p_{tot} W}{\eta \rho_{in}}. \tag{4}$$

From the aforementioned expressions, the pressure drop and pumping power for the canisters were estimated and plotted as functions of the inlet pressure for different helium coolant temperature rises as shown in figs. 3 and 4, respectively. The helium inlet temperature was set at a low value of 50°C based on the safety objective of minimizing the helium temperature. Fig. 3 indicates that for an inlet helium pressure of about 10 bar or more, the corresponding pressure drop is not a binding constraint unless the helium temperature rise is small (less than 150°C). However, as shown in fig. 4, the

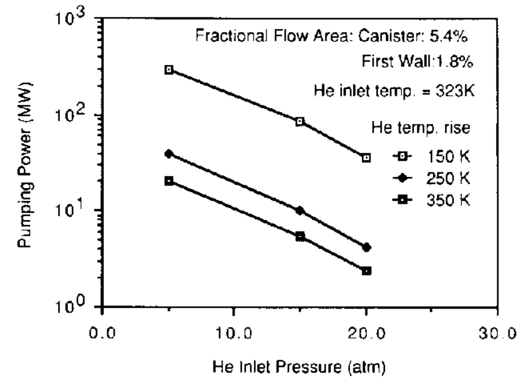

Fig. 4. Canister pumping power as a function of the helium coolant inlet pressure for different helium coolant temperature rises.

pumping power can be appreciable and is more constraining. For design purposes, a moderate inlet pressure of 15 bar is chosen together with moderate inlet and outlet temperatures of 50°C and 300°C, respectively.

Estimates of the pressure drop in the manifold and lines leading to the heat rejection system were also made and added to the canister pressure drop result in a total pressure drop of 1.7 bar and a corresponding pumping power of 30 MW, both of which are acceptable. The total coolant flow rate for the blanket is 1.1×10^6 kg/h. The maximum coolant velocity in the first wall and canister are 53 m/s and 24 m/s, respectively, and the minimum velocities are 36 m/s and 20 m/s, respectively.

2.2. Porous mesh insulating helium layer

In order to insulate the cold helium flowing along the side wall from the hotter helium flowing in the canister, a porous mesh is used to create a stagnant film of helium as shown in fig. 2. The Dittus–Boelter correlation [4] is used to estimate the heat transfer coefficient on each side of the insulating helium layer. At the entrance to the side wall, the helium temperature is 50°C, while on the other side of the insulating layer and second wall, the outlet helium temperature is 300°C. At this location, the calculated heat transfer coefficients are 1831 W/m^2 K and 1109 W/m^2 K for the cold side and the hot side, respectively. The heat flux across the second wall is then 0.125 MW/m^2, assuming no insulating gap (i.e. only a 2 mm thick second wall), and effectively increases the temperature rise in the first-wall channel by 75°C. In the present design, a 2 mm stagnant helium layer is included; the heat flux is then reduced to 0.017 MW/m^2 at that particular location and effectively increases the temperature in the first-wall channel by only 10°C, which is acceptable. The temperature at the exit of the first wall channel is then about 100°C.

3. Transient analysis of flow behavior at the manifold

3.1. Analytical model for flow behavior at manifold

In a baseline design, the fusion plant components are designed to operate most efficiently at the rated full power while maintaining a capacity to operate the system within some safety factor, which accounts for plasma disruptions. To examine the thermal-hydraulic behavior of one blanket sector, a blanket canister was

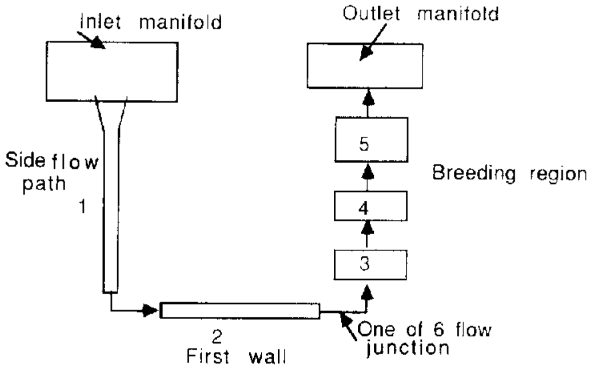

Fig. 5. Model representation of blanket configuration (one of nine parallel channels).

modeled, by using five hydrodynamic volumes and six flow junctions to represent its physical characteristics (see fig. 5). The model also includes two common volumes to simulate the inlet and outlet manifolds. The three volume breeding zones were used to account for the radial variation of power. Note that this model applies to all helium-cooled solid breeder blankets with a canister configuration and is applied here to the helium-cooled solid breeder blanket design proposed for ITER. The one-dimensional transient system of conservation equations used to solve the flow behavior are written as follows:

continuity equation,

$$A\frac{\partial \rho}{\partial t} + \frac{\partial(\rho V A)}{\partial z} = 0; \qquad (5)$$

energy equation,

$$\rho C_p \frac{\partial T}{\partial t} + \rho V C_p \frac{\partial T}{\partial z} = Q + \frac{\partial P}{\partial t}; \qquad (6)$$

momentum equation,

$$\frac{\partial \rho V}{\partial t} + \frac{1}{A}\frac{\partial G^2 A \rho^{-1}}{\partial z} = -\frac{\partial P}{\partial z} - F - \kappa - \gamma\rho. \qquad (7)$$

Where F represents friction pressure losses (for calculation purposes, a smooth first-wall channel was assumed for transient runs) and κ are the pressure losses at the inlet, outlet and internal restrictions, and γ is the direction of flow, which is +1 for upflow and −1 for downflow. The fourth equation used is the equation of state.

To predict the thermal-hydraulic behavior of the system in response to a specified variable power input to the channel, it is possible to separate the inlet mass velocity and the instantaneous axial mass flow rate distribution from the resulting mass flow rate $G(z, t)$. Following Tong [6], assigning $\phi(z, t)$ as the axial mass flow rate distribution in the channel, and $G(0, t)$ as the inlet mass flow rate, which may vary with time, yields

$$G(z, t) = G(0, t)\phi(z, t). \tag{8}$$

Substituting this equation into the momentum equation and integrating over the channel length results in

$$\frac{\partial G(0, t)}{\partial t} = -\left[\Delta P - \int F\,dz - \int \kappa\,dz - \int \gamma\rho\,dz + (\Delta G^2)\rho^{-1} + G(0, t)\int \frac{\partial \Phi}{\partial t}\,dz\right] \Big/ \int \Phi\,dz, \tag{9}$$

where ΔP is the driving force between the common plenums and $\partial\Phi/\partial t$ vanishes if the mass flow rate (GA) remains constant within the channel.

The total inlet mass flow rate, $W(t)$, is written as

$$A_1 G_1(0, t) + A_2 G_2(0, t) + \cdots + A_n G_n(0, t) = W(t), \tag{10}$$

where A_1, A_2, and A_n are the flow areas of channels 1, 2, and n, respectively. Differentiating the above equation yields

$$A_1 \frac{\partial G_1(0, t)}{\partial t} + A_2 \frac{\partial G_2(0, t)}{\partial t} + \cdots + A_n \frac{\partial G_n(0, t)}{\partial t} = \frac{\partial W}{\partial t}. \tag{11}$$

This equation can be used for evaluating the pressure drop common to all channels:

$$\Delta P = \left(\frac{\partial W}{\partial t} - \sum B_n A_n\right) \Big/ \sum C_n A_n, \tag{12}$$

where C and B are defined respectively as

$$C = -\frac{1}{\int \phi\,dz};$$

$$B = -\left[-\int F\,dz - \int \kappa\,dz - \int \gamma\rho\,dz + \frac{\Delta G^2}{\rho} + G(0, t)\int \frac{\partial \Phi}{\partial t}\,dz\right] \Big/ \int \Phi\,dz. \tag{13}$$

The calculated value of ΔP can be substituted into eq. (9) to determine subsequents values of G for individual parallel channels. The coolant temperature distribution along the blanket canister was determined through the use of the energy equation. To obtain the flow rate, pressure drop, and coolant temperature distribution time history, the solution scheme requires an iterative procedure.

The pressure losses along each coolant flow path include friction losses along the channel wall, localized losses from a sudden change in channel diameter and internal restrictions, and variation of momentum from a change in the fluid density. In the reference blanket design, the helium coolant flows along the side walls and across a bundle of staggered rods lying in a toroidal axis. The friction loss in the canister is calculated from eqs. (1) and (2). The aforementioned system of equations are formulated in terms of area- and time-averaged parameters of the flow and are solved numerically using a semi-implicit finite-difference technique. The time step required for the numerical solution method is controlled by the Courant time constant, which is the ratio of flow path to local velocity.

3.2. *Flow behavior during a power increase transient*

During normal operation, a coolant channel in a blanket canister with a higher heating rate will experience higher flow acceleration and reduced density. The resistance to the flow will therefore be higher in this canister and the coolant channel will receive less flow to maintain the pressure drop balance. Table 1 summarizes the results for the steady state normal operating condition. For the present calculation, the poloidal power peaking factor is set at 1.3, the minimum power factor is at 0.9, and a poloidal linear distribution is applied to the rest of the blanket canister. This is optimistic since the poloidal power variation is likely to be significantly higher. Thus, any corrective measure required at this level would be greater for the higher power variation

Table 1
Representative thermal-hydraulic data for the reference blanket design

Channel number	1	3	5	7	9
Region power factor	1.3	1.2	1.1	1.0	0.9
Coolant inlet temperature (K)	323	323	323	323	323
Coolant inlet pressure (MPa)	1.5	1.5	1.5	1.5	1.5
Coolant mass flow rate (kg/s)	1.200	1.204	1.208	1.213	1.218
Coolant exit temperature (K)	573.0	552.9	533.0	513.2	493.5
Coolant pressure drop (MPa)	0.166	0.166	0.166	0.166	0.166

case. It is noted that the coolant exits from the blanket canisters at temperatures ranging from 493 K to 573 K. For the same pressure drop across each canister, the lowest power blanket canister requires the highest flow rate, which results in the lowest coolant exit temperature. The tritium release behavior in solid breeders is heavily dependent on temperature, being unacceptably slow at low temperatures. At the same time, it is desirable that the blanket has the capability to operate at different steady state power level, which is of particular importance for ITER conditions. Thus, the solid breeder is made to operate at its minimum allowable temperature to enable the maximum passive power increase accommodation by letting the solid breeder temperature rise within its allowable temperature window. Power decreases can always be accommodated by reducing the helium flow rate to keep the solid breeder temperature above its minimum allowable value. If the coolant temperature is different in each canister, the canister configuration will then have to be individually tailored to maintain the same minimum solid breeder operating temperature. To avoid this design complexity and to avoid thermal mixing in the manifold, it is desirable to have the same coolant inlet and outlet temperatures for all canisters.

The power was increased linearly with time to 150% full power at 0.05 s after initiation of the transient calculation. The transient was run for 1 s under the assumption that the subsystems are capable of providing the normal coolant inlet conditions and that the pump provides the same pressure head as in the steady state condition. Figure 6 shows the behavior of the individual mass flow rate against time and figure 7 shows the behavior of coolant exit temperature at different channels against time. The results indicate that the flow rate in each blanket canister begins to decrease at the moment the transient begins and reaches a new

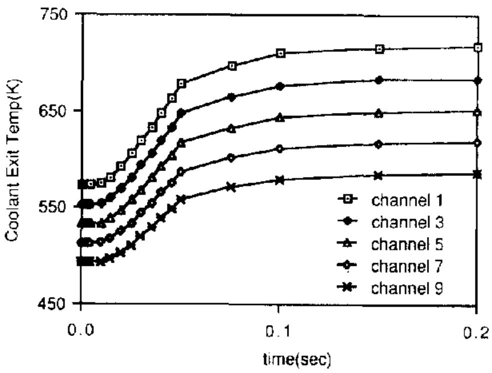

Fig. 7. Canister coolant exit temperature under power increase transient.

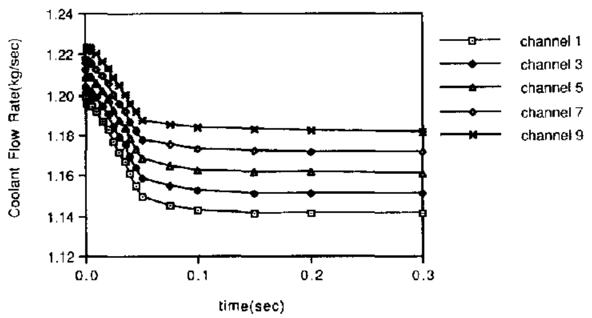

Fig. 6. Coolant flow rate history under power increase transient (assuming pump head is fixed).

equilibrium flow rate at about 0.15 s. The highest power blanket canister (channel 1) has the largest drop in flow rate, since the flow there experiences the largest changes in fluid properties (density reduction) and acceleration losses. Reduction of the flow rate during power transient operation results in an additional increase of the helium coolant temperature. For the highest power blanket canister, the coolant temperature rise is 5% higher then it would be if there were no reduction in flow. The corresponding difference between the maximum and minimum coolant exit temperature is 130 K, which is about 60% higher than under normal operating conditions. Both of these consequences exacerbate the problem caused by non-uniform coolant temperatures and augment the need for corrective measures.

3.3. Introduction of diffusive plate for flow balance

A corrective measure is to install a diffusive plate at the inlet of the coolant flow passage to adjust the helium mass flow distribution and to obtain a uniform helium outlet temperature. The required flow resistance is then calculated by balancing the pressure losses in all the coolant passages. The reason to introduce a flow resistance at the inlet of the coolant channel instead of other locations of the flow passage is that it increases the flow stability by providing for the retention of a large pressure head at the inlet. The calculated inlet flow resistances to balance the pressure drop in a blanket sector made up of nine blanket canisters connected to two manifolds are calculated from the momentum equation and are tabulated in table 2. The same transient condition is run with the installation of the diffusive plate at the inlet of the coolant flow passage and the resulting coolant flow rate history is shown in fig. 8.

Table 2
Representative flow behavior with the introduction of diffusive plate

Channel number	1	3	5	7	9
Coolant flow rate (kg/s)	1.200	1.108	1.015	0.922	0.833
Coolant exit temperature (K)	573.0	573.0	573.1	573.1	572.2
Coolant pressure drop (MPa)	0.166	0.166	0.166	0.166	0.166
Diffusive plate flow resistance [a]	0	3.0	6.95	12.15	18.64

[a] The flow resistances are expressed in terms of velocity head in the side flow path of the reference blanket design.

The flow reduction and the increase of coolant exit temperature follow a similar behavior to the previous case. However, the corresponding difference between the maximum and minimum coolant exit temperature is only 10 K.

Another transient considered in the present study is the sawteeth surge scenario, which arises from the malfunction of the RF heating. A typical power profile of the sawteeth surge transient would result in a power increase to 120% of rated power in about 2 to 3 s and a drop to normal power level in about 0.3 s. The cycle repeats itself until the RF heating backup operates. The coolant exit temperature history for a single power transient is shown in fig. 9, for each channel. The exit temperature history tends to follow the same profile as that of the power level but the difference between the maximum and minimum coolant exit temperatures is again small, about 4 K.

4. Flow control scheme

The basis of fluid flow control is to ensure the integrity of the blanket module during various operating conditions, which include normal operation in the design power range as well as transients. This objective can be obtained by varying the total flow to the blanket channels, so as to maintain the breeder, the support structure and internal components at temperature levels that lie within the temperature design limits. This results in a limited rise of the coolant temperatures during any operating upset conditions so that the breeder and the structure temperatures do not exceed their design operating temperature limits. For example, for the reference Li_4SiO_4 solid breeder, the assumed maximum allowable operating temperature beyond which sintering would occur, thereby affecting the tritium release, is 1000 K [1]. If, as an illustration, a 375 K temperature gradient across the film, multiplier and breeding region is assumed at all canister locations for the reference design [3], the mixed coolant temperature at any location in the breeding region must then be less than 625 K for the solid breeder to stay below its maximum allowable temperature.

To ensure the blanket canister integrity during a change in operating conditions, the total inlet mass flow rate is adjusted through a flow control valve according to the temperature rise of the coolant in the reactor and the imposed flow control scheme. The control system consists of the evaluation of the system behavior through the algebraic equations and logical statements, and is primarily intended to provide the capability to simulate

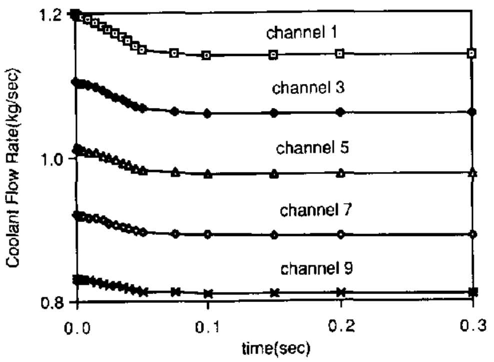

Fig. 8. Coolant flow rate history under power increase transient with installation of a diffusive plate.

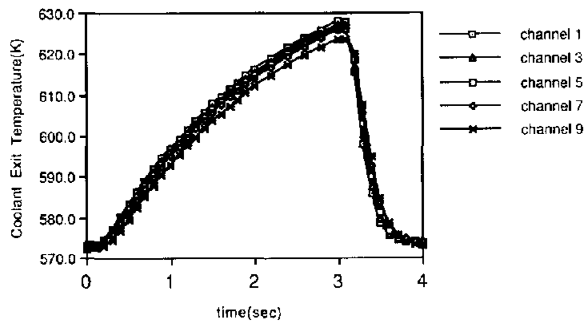

Fig. 9. Coolant exit temperature history during sawteeth surge scenario.

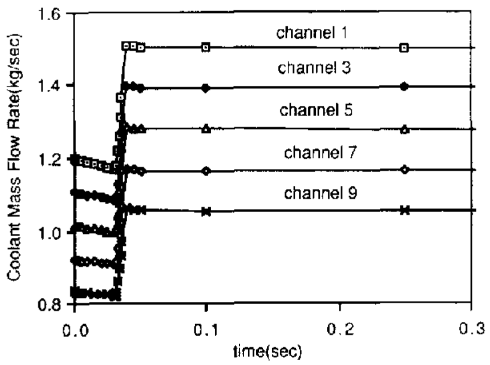

Fig. 10. Coolant flow rate history under power increase transient with proposed control scheme.

the control systems that may be used in the reactor. The coolant temperature rise at each blanket canister is examined after calculation. If the program detects that the coolant temperature is over the design temperature limit, the flow is reset to a new value based on the temperature rise at that blanket canister. Since the coolant temperature rise is proportional to the mass flow rate, it is possible to generate a scheme such that the required flow rate is obtained from the following equation:

$$W_n = \frac{\Delta T}{\Delta T_{design}} W_0, \qquad (14)$$

where W_0 is the mass flow rate before adjustment, W_n is the desired new mass flow rate, ΔT is the total temperature rise at the detected location and ΔT_{design} is the maximum allowable temperature rise.

The initial transient case is rerun with the aid of the control scheme and the results are summarized in figs. 10 and 11. Figure 10 indicates that prior to the control scheme being initiated, the flow rate to each blanket canister is reduced such that the pressure losses in the coolant passage match the available pressure head. The system detects the coolant temperature over the design temperature limit at about 0.03 s and begins to introduce more flow to the blanket sector. Figure 11 shows that a slightly different helium coolant outlet temperature occurs at the new equilibrium state because of total flow being redistributed to the blanket canisters based on the driving force between two common heads. The lowest power blanket module receives more flow, and therefore the lowest coolant exit temperature occurs there during the power transient. If a uniform coolant outlet temperature is preferred, then the flow resistance of the diffusive plate requires adjustment to smooth out the flow distribution and pressure disturbance.

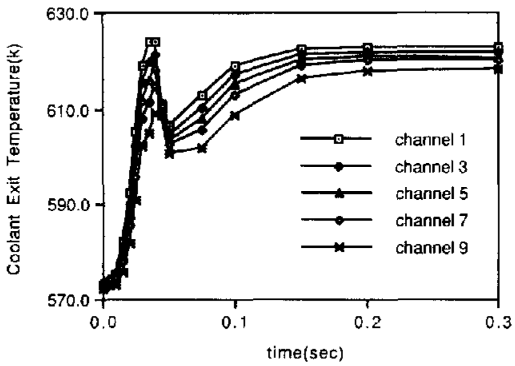

Fig. 11. Canister coolant exit temperature history under a power increase transient with the proposed control scheme.

5. Conclusions

The thermal-hydraulics calculations indicate that the helium-cooled solid breeder blanket for ITER can operate at moderate pressure and temperature, which coupled to the use of an inert gas, makes the blanket particularly attractive from a safety point of view. Transient analysis of the manifolding indicates that the introduction of a diffusive plate at the inlet of the blanket coolant flow path produces a more uniform coolant exit temperature distribution for the poloidal running manifold design. The analysis shows only a small difference of temperature at the exit of the blanket module for the reference operating case. Results indicate that the provision of a different amount of total coolant flow can be achieved by adjusting a flow control valve at the inlet of the coolant pump and that through the control of the pump head, the coolant temperature can be kept below its maximum limit, set by the maximum allowable material operating temperature.

Nomenclature

A cross sectional area of the channel, m^2,
C_p specific heat at constant pressure, J/kg °C,
D rod diameter, m,
d_H hydraulic diameter, m,
f friction factor,
G mass flow rate, kg/m^2 s,
N number of transverse rows of rods per unit radial length of canister,

P pressure, bar,
ΔP pressure drop, bar,
P_p pumping power, MW,
Q heat generation per unit volume, J/m^3,
Re Reynolds number,
S_T transverse pitch between rods, m,
T coolant temperature, °C,
t time, s,
V velocity, m/s,
W mass flow rate, kg/s,
z axial position in the channel, m.

Greek symbols

Γ power per unit area, MW/m^2,
η pump efficiency (= 0.9 for present calculation),
ρ density, kg/m^3.

Subscripts

acc acceleration,
can canister,
fw first wall,
in inlet,
out outlet,
tot total.

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