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INTRA-ASSEMBLY FLOW REDISTRIBUTION IN LMFBRs: A SIMPLE COMPUTATIONAL APPROACH*

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The liquid-metal fast-breeder-reactor (LMFBR) core consists of fuel, blanket, control, and shielding assemblies packed in a hexagonal configuration.

Radial blanket assemblies occupy peripheral locations in the reactor core and are characterized by steep power gradients, while inner blanket assemblies are located within the fuel assembly region and have higher power levels but flatter distributions.

It is due to the presence of this radial power gradient that large sodium temperature distributions exist at full power operation. However, at low power, low flow natural convection conditions, a significant flow redistribution takes place leading to considerable radial temperature flattening.

The prediction of assembly flow and temperature distributions must consider a wide range of coolant flow and thermal convection conditions which include laminar, transition, and turbulent flow [1] during natural, mixed, and forced convection regimes of reactor operation.

Analytical models to date are based on numerical solutions of the conservation equations using various approximations as outlined by Khan [2].

Incorporation of these complex models in large system simulation codes is not very practical; however, a simple method is desired, which provides the peak sodium temperatures within representative assemblies with a sufficient degree of accuracy.

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The purpose of the present study is to formulate a simple flow-regime dependent model supported by experimental data for prediction of sodium temperature flattening due to buoyancy-induced flow redistribution in LMFBR subassemblies with significant radial power gradient.

The model is an adaption of the porous-body model for laminar flow originally proposed by Meyer [3]. In this model, the rod bundle is approximated by a porous slab of width ℓ and height L_h corresponding to the assembly flat-to-flat and the rod heated length, assuming: (1) two-dimensional flow, (2) steady-state condition, (3) negligible spatial acceleration, (4) second derivatives are negligible, (5) Bousinesque approximation applies, and (6) pressure is uniform at any axial level.

The conservation equations can be written as:

$$\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} = 0 \qquad \text{(Mass Continuity)} \quad (1)$$

$$\rho c_p \left[\frac{\partial (uT)}{\partial x} + \frac{\partial (vT)}{\partial y}\right] = Q''' \qquad \text{(Energy)} \quad (2)$$

$$-\frac{dP}{dx} = \frac{\rho f}{2D_e \lambda_a^2} u^2 + g\rho^* [1 - \beta (T-T^*)] \qquad \text{(Axial Momentum)} \quad (3)$$

where u is the axial component of the velocity, v is the transverse component of the velocity, T is the temperature, ρ is the density, β is the isobaric thermal expansion coefficient, C_p is the specific heat, λ_a is the axial area fraction occupied by the coolant, P is the pressure, the superscript * refers to the reference conditions, and the friction factor f is represented by the Darcy-Weisbach relation ($f = C_f Re^n$).

Equations (1) through (3) must be solved subject to: (a) known inlet temperature; $T(o,y) = T^*$, (b) specified average velocity; $\hat{u}$, (c) assembly edge condition, $v(x,\pm \ell/2) = 0$, and (d) linear transverse power distribution of the form:

$$Q''' = \hat{Q}''' \left[1 + (F-1)\left(\frac{2y}{\ell}\right)\right] \tag{4}$$

where F is the nuclear heat input factor.

Furthermore, consider an axial velocity distribution which is linear in the transverse direction at each axial position:

$$u(x,y) = \hat{u}\left[1 + \left(\frac{2y}{\ell}\right) Y(x)\right] \tag{5}$$

where Y is as yet an unknown function of x.

Equations (1) through (5) are solved analytically using an approximate integral method [4] to obtain:

$$\frac{dY}{dx} = \frac{\hat{Q}'''}{\rho c_p \hat{u}} \quad \frac{1}{B_o B_1 \hat{u}^{1-n}} \left[1 - \frac{F-1}{Y}\right] \tag{6}$$

$$\frac{dT_c}{dx} = \frac{\hat{Q}'''}{\rho c_p \hat{u}} - B_o B_2 \, \hat{u}^{1-n} \frac{dY}{dx} \tag{7}$$

$$T(x,y) = T_c(x) + \frac{B_o \hat{u}^{1-n}}{2-n} \left\{\left[1 + \left(\frac{2y}{\ell}\right) Y(x)\right]^{2-n} - 1\right\} \tag{8}$$

and,

$$v = \left(\frac{\ell}{4}\right) \hat{u} \frac{dY}{dx} \left[1 - \left(\frac{2y}{\ell}\right)^2\right] \quad (9)$$

where:

$$B_o = \frac{C_f \lambda_a^{n-2} \mu^n \rho^{1-n}}{g \beta D_e^{1+n} \rho^*} \left(1 - \frac{n}{2}\right)\hat{u} \quad (10a)$$

$$B_1 = \left[\frac{(1+Y)^{2-n} - (1-Y)^{2-n}}{4(2-n)}\right] + \frac{1}{4Y^2}\left[\frac{(1+Y)^{4-n} - (1-Y)^{4-n}}{4-n} - \frac{(1+Y)^{2-n} - (1-Y)^{2-n}}{2-n}\right] \quad (10b)$$

$$+ \left(\frac{1}{Y^3} - \frac{1}{Y}\right)\left[\frac{(1+Y)^{2-n} + (1-Y)^{2-n} - 2}{2(2-n)}\right] - \left[\frac{(1+Y)^{4-n} + (1-Y)^{4-n} - 2}{2Y^3(4-n)}\right]$$

and

$$B_2 = \left[\frac{(1+Y)^{2-n} - (1-Y)^{2-n}}{4(2-n)}\right] + \frac{1}{4Y^2}\left[\frac{(1+Y)^{4-n} - (1-Y)^{4-n}}{4-n} - \frac{(1+Y)^{2-n} - (1-Y)^{2-n}}{2-n}\right] \quad (10c)$$

Equations (6) and (7) together with appropriate flow-dependent parameters (n and C_f) are integrated numerically to obtain a complete characteristic of the temperature and flow field as a function of the radial power factor F at various average power and flow conditions in the bundle.

The temperature rise hot channel factor is defined as the fractional reduction in F caused by intra-assembly flow redistribution; that is:

$$\eta^* = \frac{\alpha^* - 1}{F-1} \quad (11)$$

where α^* is the temperature peaking factor.

Figure 1 shows the calculated temperature rise hot channel factor as a function of the inlet Reynolds number using the present model. Also shown are the measured data of Engel, et al [5] corresponding to the same conditions.

It is seen that the agreement between the experimental measurements and the calculated results is better at lower power skew; furthermore, the maximum discrepancies are seen to occur in the transition from forced to free convection regime, where the transverse thermal conduction becomes increasingly important [4].

It is therefore concluded that buoyancy-induced intra-assembly flow redistribution can significantly reduce the large transverse temperature gradients in subassemblies. Furthermore, the impact of the transverse conduction is quite important in the mixed and free convection regimes and must therefore be included [4] for the best estimate calculations.

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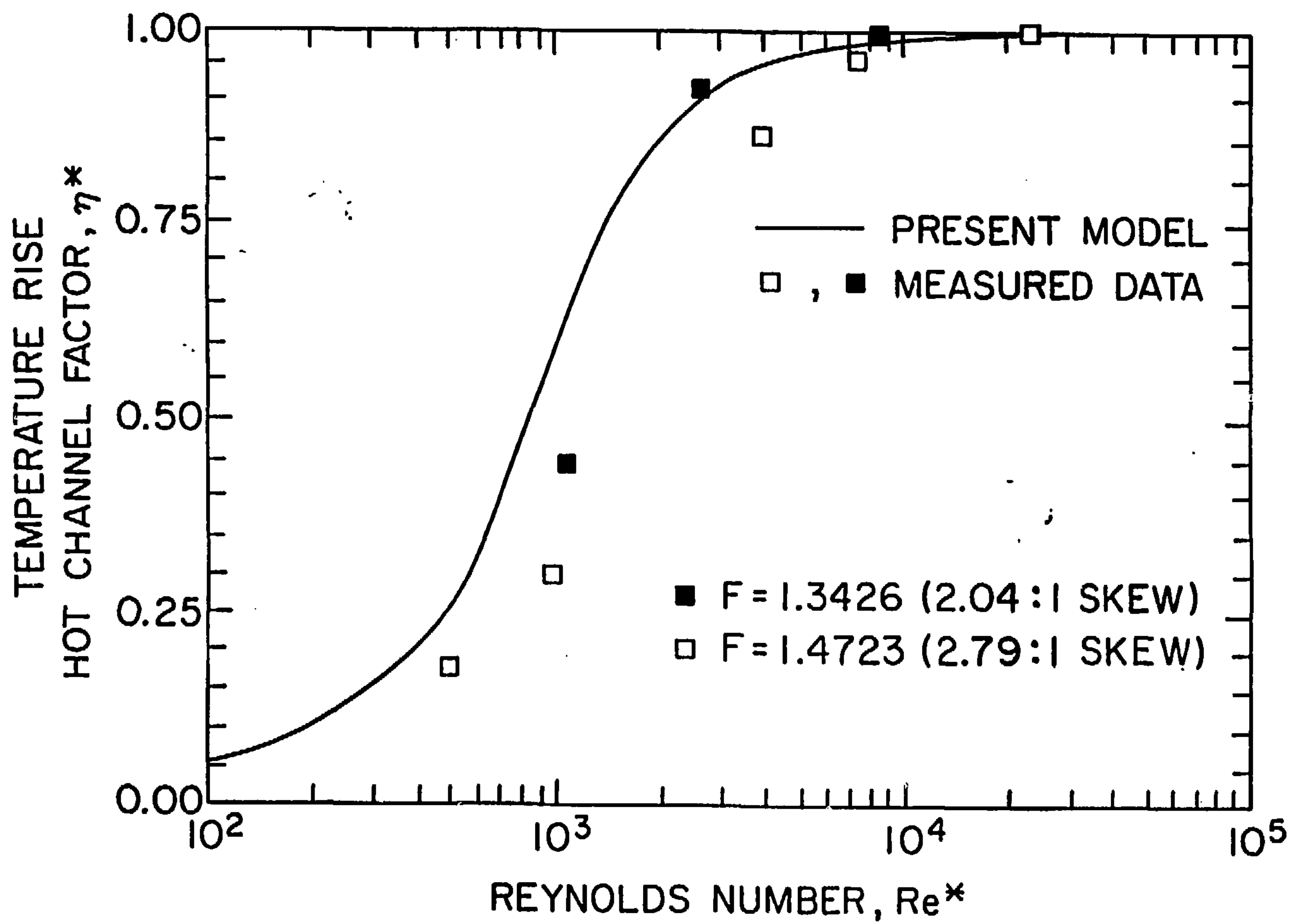

Figure 1 Comparison of Calculated and Measured Temperature Rise Hot Channel Factor