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POST-DRYOUT HEAT TRANSFER IN SODIUM-COOLED FAST REACTORS

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ABSTRACT

The heat transfer behavior of a heat-generating, dry, particulate bed resulting from a postulated core meltdown accident in sodium-cooled fast reactors is investigated theoretically. The processes of combined conduction and thermal radiation in the dry particulate bed and transient heat conduction in the structure in contact with the bed are modeled. Also modeled is the contribution of direct gamma-ray radiation to heat transfer. Results for the transient temperature distribution of the system are obtained by the method of successive approximation. Various post-dryout situations are considered and the conditions leading either to an incipient melting system or to a coolable non-melting system are identified. These are found to depend on four groups of dimensionless parameters, namely, the thermal property group, the radiation and internal heating group, the gamma-ray heating group, and the external cooling group. Numerical examples are given for a UO_2 bed-steel structure system to illustrate the physical significance associated with each parameter.

1. INTRODUCTION

The coolability of a large mass of core debris resulting from a hypothetical core-disruptive accident has long been an important issue in the studies of fast reactor safety [1]. One major concern is the behavior of the core debris bed immediately following the dryout of sodium. In the post-dryout stage, there is no heat sinks available within the heat-generating mass of core debris. As a result, the temperature of the bed will rise due to decay heating of the fuel particles. Meanwhile the temperature of the surrounding structure will also rise due to heating by the bed. If there is no external cooling of the system or if the rate of external cooling is not sufficiently high, incipient melting will eventually occur. Decay-heat removal may then be achieved at the expense of latent heat of the system. However, it is not clear whether melting will first occur in the bed or in the structure.

Should the structure melt first, the void volume of the lower portion of the bed would likely be occupied by the molten structural material, which would substantially enhance the coolability of the core debris. In that situation, the fuel particles would likely remain solid (at least for a period of

time) as they penetrate into the body of the structure [2]. On the other hand, a molten fuel pool would form should the fuel particles melt first. The fuel pool might then grow as it penetrates into the surrounding structural material [3]. Generally, the mechanism of heat transfer and the rate of melting in the molten-pool-penetration mode are markedly different from those in the debris-bed-penetration mode. To accurately predict the post-dryout scenario and thus to describe the likely course of the accident, it is of prime importance to first evaluate under what circumstances one mode of penetration would prevail over the other.

To achieve this goal, the maximum temperature of the bed and of the structure must be determined as functions of time in the post-dryout stage. In this study, non-linear thermal interaction between a heating-generating, dry, particulate bed and a structural material is investigated. The process of combined conduction and thermal radiation in the particulate bed is modeled by treating the bed as a continuum with its bulk properties related to the average bed voidage [4,5]. Transient heat conduction in the structure is formulated with the contribution of gamma-ray heating. The resultant system of partial differential equations is solved analytically by the successive approximation method. Comparison of the approximate solutions to some existing finite-difference numerical solutions is made to justify the present approach. Results for various post-dryout situations are obtained and their physical significance discussed.

2. ANALYSIS

The physical system under consideration consists of a large mass of heat-generating dry particulate bed packed on top of a solid. Initially, both the bed temperature, T_b , and the solid temperature, T_s , are considered to be at T_0 , which is here taken to be the boiling point of sodium. The particles of the bed are assumed to be spherical and the depth of the bed is much larger than the size of the particles, so that the bed heat transfer may be described in terms of its bulk properties. In addition, the heat transfer area between the bed and the solid is assumed to be sufficiently large such that the heat-transfer process is virtually one-dimensional. If the bed voidage, ω , is assumed to be constant, then the temperature field in the bed region is governed by

$$\frac{\partial T_b}{\partial t} = \frac{\partial}{\partial x} \left\{ \alpha_b [1 + k_r/k_b] \frac{\partial T_b}{\partial x} \right\} + S, \quad 0 \leq x \leq l_b \quad (1)$$

where S is the local decay-heat generation rate, k_b and α_b the thermal conductivity and diffusivity of the bed, respectively, l_b the depth of the bed, and k_r the apparent radiation conductivity of the particles. The latter usually takes the form

$$k_r = \sigma T_b^3 D_p f(\omega, \epsilon), \quad (2)$$

where σ is the Stefan-Boltzmann constant, D_p the local particle size, and $f(\omega, \epsilon)$ is a function of the emissivity of the particles, ϵ , and the average bed voidage [6,7]. Introducing the modified conduction-radiation coupling parameter

$$N = 4k_b/3\sigma T_0^3 D_p, \quad (3)$$

equation (1) becomes

$$\frac{\partial T_b}{\partial t} = \frac{\partial}{\partial x} \left\{ \alpha_b \left[1 + \frac{4}{3N} \left(\frac{T_b}{T_0} \right)^3 \right] \frac{\partial T_b}{\partial x} \right\} + S, \quad 0 \leq x \leq l_b. \quad (4)$$

The temperature field in the solid region is governed by

$$\frac{\partial T_s}{\partial t} = \alpha_s \frac{\partial^2 T_s}{\partial x^2} + I e^{\beta x/l_s}, \quad -l_s \leq x \leq 0 \quad (5)$$

where α_s is the thermal diffusivity of the solid, I the intensity of gamma-ray heating, β the index of gamma-ray penetration, and l_s the thickness of the structural material. The initial and the boundary conditions are

$$T_b(0, x) = T_s(0, x) = T_0, \quad (6)$$

$$x = 0 : T_b = T_s = T_i,$$

$$k_b \left[1 + \frac{4}{3N} \left(\frac{T_b}{T_0} \right)^3 \right] \frac{\partial T_b}{\partial x} = k_s \frac{\partial T_s}{\partial x}, \quad (7)$$

$$x = l_b : -k_s \left[1 + \frac{4}{3N} \left(\frac{T_b}{T_0} \right)^3 \right] \frac{\partial T_b}{\partial x} = h_b (T_b - T_0), \quad (8)$$

$$x = -l_s : k_s \frac{\partial T_s}{\partial x} = h_s (T_s - T_0), \quad (9)$$

where T_i is the unknown interface temperature to be determined in the course of analysis, k_s the thermal conductivity of the solid, and h_b , h_s the heat transfer coefficients for top and bottom cooling, respectively. In the above formulation, N and S are treated as local variables whereas the material properties have been assumed constant to minimize the total number of parameters. For later con-

venience, the following dimensionless variables are introduced:

$$\eta = x/2 (\alpha_b t)^{1/2}, \quad \xi = S_0 t/T_0 \quad (10)$$

$$\xi \theta_b = T_b/T_0 - 1, \quad \xi \theta_s = T_s/T_0 - 1,$$

$$\xi \theta_i = T_i/T_0 - 1 \quad (11)$$

where S_0 is a reference value of S . With equation (11), the initial conditions given by equation (6) are satisfied automatically. In dimensionless form, the governing system becomes

$$g_b \theta_b - \frac{\eta}{2} \theta_b' = \frac{S}{S_0} + \frac{1}{4} \left\{ \left[1 + \frac{4}{3N} (1 + \xi \theta_b)^3 \right] \theta_b' \right\}', \quad 0 \leq \eta \leq L_b \quad (12)$$

$$g_s \theta_s - \frac{\eta}{2} \theta_s' = \frac{\alpha_s}{4\alpha_b} \theta_s'' + \frac{1}{S_0} e^{\beta \eta}, \quad -L_s \leq \eta \leq 0 \quad (13)$$

$$\eta = 0 : \theta_b = \theta_s = \theta_i, \quad \left[1 + \frac{4}{3N} (1 + \xi \theta_b)^3 \right] \theta_b' = \frac{k_s}{k_b} \theta_s', \quad (14)$$

$$\eta = \xi^{-1/2} L_s : \left[1 + \frac{4}{3N} (1 + \xi \theta_b)^3 \right] \theta_b' = -H_b \xi^{1/2} \theta_b \quad (15)$$

$$\eta = -\xi^{-1/2} L_s : \theta_s' = H_s \xi^{1/2} \theta_s \quad (16)$$

where the primes denote the partial derivative with respect to η and

$$g_b = 1 + \frac{\partial \ln \theta_b}{\partial \ln \xi}, \quad g_s = 1 + \frac{\partial \ln \theta_s}{\partial \ln \xi}, \quad (17)$$

$$H_b = 2 \left(\frac{\alpha_b T_0}{S_0} \right)^{1/2} \frac{h_b}{k_b}, \quad H_s = 2 \left(\frac{\alpha_s T_0}{S_0} \right)^{1/2} \frac{h_s}{k_s} \quad (18)$$

$$L_b = \frac{l_b}{2} \left(\frac{\alpha_b T_0}{S_0} \right)^{-1/2}, \quad L_s = \frac{l_s}{2} \left(\frac{\alpha_s T_0}{S_0} \right)^{-1/2} \quad (19)$$

Inspection of equations (12) to (16) indicates that the temperature transients of the system are controlled by four groups of independent dimensionless parameters. These are: (i) thermal-property group, k_s/k_b and α_s/α_b ; (ii) radiation- and internal-heating group, N and S/S_0 ; (iii) gamma-ray-heating group, I/S_0 and β ; (iv) external-cooling group, H_b , H_s , L_b , and L_s .

Under the highly simplifying conditions of $I = H_b = H_s = 0$, $L_b \rightarrow \infty$, $L_s \rightarrow \infty$, $N = \text{constant}$, and $S/S_0 = 1$, numerical solutions have been computed in [8] for some specific values of k_s/k_b and α_s/α_b . Here, in this study, the general case of variable N , variable S , and finite, non-zero values of I/S_0 is investigated. Owing to the large number of parameters involved in the system, a parametric approach is employed to determine the physical significance associated with each dimensionless group.

3. EFFECTS OF VARIABLE PARTICLE SIZE

Previous experimental studies on debris bed formation have indicated that there is generally a strong variation of particle size within the bed [9]. The upper portion of the bed usually has a large number of fine particles in it whereas the lower portion of the bed contains mostly coarse particles. As a result, internal thermal radiation is weaker in the upper portion of the bed but stronger in the lower portion. On the other hand, the rate of volumetric heating is higher in the upper portion of the bed than in the lower portion due to variation of the density of heat sources across the bed. To explore the influence of particle size variation and thus the effects of variable N and S on the conditions of incipient melting of the system, the following expressions for N and S are adopted:

$$N^{-1} = N_0^{-1} (1 + e^{-\eta}), \quad S = S_0 (1 - \frac{1}{2} e^{-\eta}), \quad (20)$$

where N_0 and S_0 are the values of N and S , respectively, in the upper portion of the bed. In writing the above expressions, we have assumed the values of N and S to vary by approximately a factor of two across the dry bed region. Clearly, other values may be assumed but the computed results would be qualitatively similar.[†] To focus our attention on the effects of N and S , we further assume $I = H_b = H_s = 0$ and $L_b, L_s \rightarrow \infty$ in this case.

Equations (12) to (16) may now be solved numerically using the successive iterational scheme. In the first approximation, the transient terms on the right-hand side of equation (17) are omitted so that we have $g_b = g_s = 1$. The independent variable, ξ , is now treated as a parameter rather than a variable. Equations (12) to (16) thus constitute a system of second-order ordinary differential equations that is readily solved by any standard numerical technique. Here, the Runge-Kutta forward difference method was employed to obtain the transient temperature profiles of both regions at a given instant of time. The lower-order solutions so obtained were then used along with equation (17) to correct for the higher-order effects involved in

neglecting g_b and g_s . With the new values of g_b and g_s , equation (12) and (13) were integrated, again as ordinary differential equations, to get the higher-order solutions. This procedure was repeated up to the third approximation where satisfactory convergence in the temperature transients was achieved. A typical set of results is shown in Figure 1. Also shown in the figure are the finite-difference solutions [8] for comparison. Apparently, the third-order solution, which differs only slightly from the second, agrees fairly well with all the numerical points. Hence there is no need to go beyond the third approximation. The present iterational scheme is found to yield rapidly converging and sufficiently accurate results for all cases under consideration.

Figure 1. Transient Temperature Distribution in a UO_2 Bed-Stainless Steel System at $\xi = 3$.

Computation of the interface temperature as a function of ξ was carried out using the values of $k_s/k_b = 66.5$, $\alpha_s/\alpha_b = 62$, and $N_0 = 1$, which are typical of those for a UO_2 bed-stainless steel system. Results for the case of variable N and S , (designated as case (iv) in the figure) are summarized in Figure 2. Also shown in the figure for comparison are the results for three other cases, namely, those of (i) constant N , constant S , (ii) constant N , variable S , and (iii) variable N , constant S . With respect to case (i), the interface temperature in case (ii) is found to rise much slower whereas the interface temperature in case (iii) is found to rise much faster. However, the temperature transient in case (iv) is found to be similar to the temperature transient in case (i). Physically, the speed of rise of the interface temperature signifies the potential for melting of the structural material. This is because of the fact that the maximum temperature of the structure always occur at the interface and that the maximum bed

[†]The local values of N and S are in fact heavily dependent upon the type and size of the reactor as well as the actual accident scenario, and therefore, must be determined by considering each particular situation. This of course is out of the scope of this study.

Figure 2. Effects of N and S on the Interface Temperature Transients for a UO_2 Bed-Stainless Steel System.

temperature, occurring in the far field, always rises according to

$$(T_b)_{\max} = (1 + \xi)T_0. \quad (21)$$

Obviously, variation in heat sources tends to suppress melting of the structure whereas variation in radiation heating tends to promote melting of the structure. These two effects are compensating so that the net effect due to particle-size and heat-sources variations is minimized. It is therefore not surprising that the results for the case of variable N and S are only slightly different from the results for the case of constant N and S .

4. EFFECTS OF GAMMA-RAY HEATING

In the dry bed region, there may be a substantial amount of gamma radiation, known to be long-range and highly penetrative, releasing from the fission products. Therefore, in addition to the heating due to direct thermal contact with the bed, the structure may experience further heating due to the penetration of gamma rays into its body. The intensity of gamma-ray heating, i.e., the value of I in equation (13), depends on the distribution of fission products within the fuel particles and in the void volume among the particles. On the other hand, the index of penetration, i.e., the value of β in equation (13), depends mainly on the form of the structure. For solid steel, β is expected to be large so that gamma-ray heating is limited only near the surface. A smaller value of β is expected for porous material such as concrete. In that case, gamma rays may penetrate deep inside the structure and preheat the body. The actual values of I and β are likely to vary from case to case and must be determined by considering each particular situation. This again is out of the

scope of this study. Here a wide range of values of β is considered. Meanwhile, the value of I is assumed to be a fraction of the value of S , which is quite logical since the gamma rays are only part of the total heat sources. To isolate the effects of I and β on the conditions for incipient melting, we shall assume $N = N_0$, $S = S_0$, $H_b = H_s = 0$, and $L_b \rightarrow \infty$, $L_s \rightarrow \infty$ in the present case. Again, by treating ξ as a parameter, equation (12) to (16) may be solved numerically as a system of second-order ordinary differential equations using the successive iterative scheme outlined in the previous section.

Computation of the interface temperature as a function of ξ was performed for a UO_2 bed-stainless steel system for which $k_s/k_b = 66.5$, $\alpha_s/\alpha_b = 62$, and $N_0 = 1$. Various sets of values of I and β were employed. At any value of ξ , a much higher interface temperature is obtained as the value of I is increased. Clearly, the penetration of gamma-rays greatly promotes the process of heating up the solid. If we assume the bed melts at the melting point of UO_2 , i.e., at 3150K, and the initial temperature of the bed is at the boiling point of sodium, i.e., $T_0 = 1156K$, then by setting $(T_b)_{\max}$ equal to 3150K, the time for incipient melting of the bed is given by

$$\xi_c = (T_b)_{\max} / T_0 - 1 = 1.72. \quad (22)$$

For the structural material, say stainless steel, to melt at or before this critical time, we require the interface temperature to be at or above the melting point of steel, i.e., $T_i > 1683K$. Conditions may thus be determined for incipient melting of the system. Results are summarized in Figure 3. Inspection of the figure indicates that the chance of melting of the structure before the

Figure 3. Effects of Gamma-Ray Heating on the Incipient Melting of a UO_2 Bed-Stainless Steel System.

incipient melting of the bed may be enhanced substantially by increasing the value of I . To the contrary, the chance may be significantly decreased by increasing the value of β . However, below the critical value of I_c (here, for the case of stainless steel, it is $I_c = 0.09$), the UO_2 bed is always found to melt first regardless the value of β . In general, the value of I_c is strongly dependent on other parameters of the system. For a UO_2 bed-concrete system, for example, I_c is a negative quantity, indicating that even for the limiting case of $I = 0$, concrete will melt before the UO_2 bed does.

5. COOLABLE NON-MELTING CONDITIONS

Depending on the type of containment design of the reactor, there may be external cooling of the structure at the bottom and of the bed at the top. If the rate of bottom cooling is sufficiently high and if the structural material is not too thick, we may be able to keep the interface temperature below the melting point of the structure. On the other hand, if the rate of top cooling is sufficiently high and if the bed is not too deep, we may be able to prevent the fuel particles from melting. When these two conditions are achieved simultaneously, there will be no melting of the system. In that case, a coolable bed is obtained even in the absence of sodium. It is therefore important to examine the effects of bottom and top cooling, as there may never be a formation of molten pool nor be any penetration of the fuel particles into the structure.

To investigate this situation, the temperature behavior of the system must be determined for given, non-zero, positive values of H_b and H_s and for finite values of L_b and L_s . Under coolable, non-melting conditions, however, a steady state will eventually be achieved in the system at large ξ . At steady state, the temperature distribution of the system can be shown to be

$$\frac{T_s}{T_0} = \frac{T_i}{T_0} + \left[1 + \frac{(\alpha_s/\alpha_b)^{1/2}}{H_s L_s} \right]^{-1} \left(\frac{T_i}{T_0} - 1 \right) \frac{x}{L_s}, \quad -L_s \leq x \leq 0 \quad (23)$$

$$\begin{aligned} \frac{T_b}{T_0} = & \left[1 + \frac{1}{3N_0} \left(\frac{T_b}{T_0} \right)^3 \right]^{-1} \left\{ -2L_b^2 \left(\frac{x}{L_b} \right)^2 \right. \\ & + \left(\frac{k_s}{k_b} \right) \left(\frac{L_s}{L_b} \right)^{-1} \left[1 + \frac{(\alpha_s/\alpha_b)^{1/2}}{H_s L_s} \right]^{-1} \\ & \left. \left(\frac{T_i}{T_0} - 1 \right) \left(\frac{x}{L_b} \right) + \left[1 + \frac{1}{3N_0} \left(\frac{T_i}{T_0} \right)^3 \right] \left(\frac{T_i}{T_0} \right) \right\}, \quad 0 \leq x \leq L_b \end{aligned} \quad (24)$$

where the interface temperature is given implicitly by

$$\begin{aligned} \frac{T_i}{T_0} = & \left[1 + (1 + H_b L_b)P + \frac{1}{3N_0} \left(\frac{T_i}{T_0} \right)^3 \right]^{-1} \left\{ 1 + 4 \frac{L_b}{H_b} \right. \\ & + 2L_b^2 + (1 + H_b L_b)P + \frac{1}{3N_0} \left[1 + 4 \frac{L_b}{H_b} \right. \\ & \left. \left. - P \left(\frac{T_i}{T_0} - 1 \right) \right]^4 \right\}, \end{aligned} \quad (25)$$

where

$$P = \left(\frac{k_s}{k_b} \right) \left(\frac{H_s}{H_b} \right) \left[H_s L_s + \left(\frac{\alpha_s}{\alpha_b} \right)^{1/2} \right]^{-1}. \quad (26)$$

In deriving the above equations, we have assumed $N = N_0$, $S = S_0$, and $I = 0$ to single out the role of external cooling from other effects. From equation (24), the maximum bed temperature is found to be

$$\begin{aligned} \left(\frac{T_b}{T_0} \right)_{\max} = & \left[1 + \frac{1}{3N_0} \left(\frac{T_b}{T_0} \right)_{\max}^3 \right]^{-1} \left\{ \frac{1}{8} L_s^{-2} \left(\frac{k_s}{k_b} \right)^2 \right. \\ & \left[1 + \frac{(\alpha_s/\alpha_b)^{1/2}}{H_s L_s} \right]^{-2} \left(\frac{T_i}{T_0} - 1 \right)^2 \\ & \left. + \left[1 + \frac{1}{3N_0} \left(\frac{T_i}{T_0} \right)^3 \right] \left(\frac{T_i}{T_0} \right) \right\}. \end{aligned} \quad (27)$$

For a coolable, non-melting system to exist, we require that the steady-state values of T_i and the maximum steady-state value of T_b be lower than the melting points of the structure and the bed, respectively. Thus, by setting the interface temperature and the maximum bed temperature equal to the respective melting points, coolability conditions can be established from equations (25) and (27). Results for a UO_2 bed-stainless steel system are presented in Figure 4 for the case of $L_s = 1$ and $L_b = 4$. Apparently, for small rates of top cooling, H_b , incipient melting of fuel particles will occur in the dry bed region before a steady state be achieved. A similar conclusion applies to the structure when the rate of bottom cooling, H_s , is small. A coolable dry-bed system is obtained only if there are sufficiently large external coolings.

6. FINAL REMARKS

We have identified and discussed the effects of some important parameters that control the post-dryout behavior of a particulate bed system. Generally, these parameters are not only functions of the properties of the materials of the system, i.e., the fuel particles and the structure, but also functions of the composition of the fission products, the particle size distribution, the surface emissivity, and the voidage of the bed. In practical application, therefore, the actual values of these parameters have to be estimated before the

Figure 4. Coolability Conditions for a Non-Melting UO_2 Bed-Stainless Steel System.

results of this study be applied.

When there is insufficient bottom or top cooling, incipient melting of the system will occur sometime after the dryout of sodium. Depending on the magnitudes of the various controlling parameters, which vary from case to case (reactor and accident), the dry-bed may remain solid or a molten fuel pool may form. In the case where the values of the controlling parameters cannot be assessed with reasonable accuracy, it is only prudent to consider both the debris-bed-penetration and the molten-pool-penetration modes in the post-dryout situation.

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