TRANIENT NON-BOILING HEAT TRANSFER IN A FUEL ROD BUNDLE DURING ACCIDENTAL POWER EXCURSIONS

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SUMMARY

One important aspect in analysis of the unprotected transient overpower accident is the prediction of time and location of fuel pin failure. The accuracy of such predictions decisively depends on the adequate modeling of the heat transfer processes involved. The usefulness of most of the past studies in the area of transient reactor heat transfer is limited due to simplifying assumptions applied. The ideal approach to reactor power-excursion studies is to describe the conservation of energy, mass, momentum and neutrons from first principles. The present investigation focusses on the fundamental heat transfer aspects in the first phase of the power-excursion transient before boiling of the coolant starts, by solving the four coupled three-dimensional time-dependent energy equations for the fuel rod (fuel, gap, clad) and the coolant. The analysis surpasses similar previous studies, e.g. by Gopalakrishnan, in several important aspects. Most notable is that the geometry of the fuel rod bundle is correctly described in its three dimensions and the turbulent energy transport within the coolant is modeled more realistic and in a consistent manner over the entire Prandtl number range of interest using results from a specially tailored phenomenological turbulence model.

The physical problem studied is the transient non-boiling heat transfer of a cylindrical fuel rod consisting of fuel, gap, and cladding to a steady, fully developed turbulent flow. The fuel pin is assumed to be located in the interior region of a subassembly with regular triangular or square arrangements. The turbulent velocity field as well as turbulent transport properties are specified as functions of the coordinates normal to the axial flow direction. The heat generation within the fuel may be specified as an arbitrary function of the three spatial coordinates and time. A digital computer program has been developed, on the basis of finite-difference techniques, to solve the governing partial differential equations with their associated subsidiary conditions.

Results have been obtained for a series of exponential power transients of interest to safety of liquid-metal and water cooled nuclear reactors. The general physical features of transient convective heat transfer as explored by previous investigators have qualitatively been substantiated by the present analysis. Emphasis has been devoted to investigate the differences of heat-transfer (coefficient) results from multi-region analysis including a realistic fuel rod model and single-region analysis for the coolant region only. A comparison with the engineering relationships for turbulent liquid-metal cooling by Stein, which are an extension of the heat transfer coefficient concept to account for transient heat fluxes, clearly demonstrates that, at the parameters studied, Stein's approach tends to largely overestimate the convective heat transfer at early times. Differences in Nusselt number from multi- and single-region analyses decrease at large times and finally stabilize, however, single-region values for Nu will always be higher. For water-cooled reactors, application of steady-state heat transfer coefficients during the non-boiling phase of power transients would be completely adequate.
1. Introduction

One important aspect in the analysis of the unprotected transient overpower accident is the prediction of time and location of fuel pin failure. The accuracy of such predictions decisively depends on the adequate modelling of the heat transfer processes involved. The usefulness of most of the past studies in the area of transient reactor heat transfer is limited due to simplifying assumptions applied. In the currently used computer codes for the evaluation of transient reactor performance, the traditional engineering method to account for forced-convection heat transfer by the simple heat-transfer-coefficient relation is assumed to be also applicable during transients. The familiar relationship

\[ q'' = h (T_w - T_f) \]  

(1)

postulates that the local heat flux at the rod surface \( q'' \) is directly proportional to the difference between local wall temperature \( T_w \) and mixed mean ("bulk") temperature of the fluid \( T_f \). Thus, this relation specifies the continuity of heat flux at the clad-coolant interface and replaces solution of the governing energy equation within the coolant which would involve rather extensive computational efforts. Difficulties arise from the fact that, generally, the heat transfer coefficient \( h \) is a complicated function of various parameters describing the hydrodynamic and thermal characteristics of the fluid and, further, of local position and time. Most troublesome, it also depends on local and time variations of its independent defining variables. Since solution of the appropriate energy equation of the coolant in every case to avoid the difficulties and uncertainties associated with the heat transfer coefficient is certainly still impractical today, it is necessary to carefully examine the potential for inaccuracies resulting from use of eq. (1) as well as simplifications in evaluating the heat transfer coefficient. Concern for these inaccuracies has existed in the nuclear reactor field for some time [1].

The most simple approach for evaluating the heat transfer coefficient is to apply one of the familiar correlations for the steady-state asymptotic heat transfer coefficient, e.g., the Dittus-Boelter correlation [2]. A slight improvement of this method has been utilized in the CHIC-KIN transient analysis program [3] where a transient heat transfer coefficient is calculated from a simple formula based on a one-dimensional solution of the conduction equation within the fluid for exponentially increasing power [4, 5]. The formula expresses the asymptotic heat transfer coefficient as a function of the reactor period which implies that \( h \) assumes a constant value for a given reactor period. In reality, however, the heat transfer coefficient is remarkably time-dependent at early times. By application of a reduced time on the basis of heat generation rate or surface heat flux to the transient heat conduction problem, Sakurai and Mizukami [6] developed an approximate expression for the heat transfer coefficient for any functional increase in reactor power and for all times. Recently, Stein [7, 8] extended the heat transfer coefficient concept by generalizing the formulation of eq. (1), and thus derived improved
engineering relationships for calculating liquid-metal turbulent forced convection heat transfer with nonuniform and transient heat fluxes. The method involves the use of asymptotic, steady-state heat transfer coefficients and several dimensionless coefficients which are presented for steady flow in circular channels and in-line flow through rod bundles of infinite extent with equilateral triangular arrangement.

Other attempts to evaluate transient heat transfer (coefficients) due to time-dependent heat generation or heat fluxes consist in correlating experimental data in terms of appropriately chosen dimensionless parameters. These correlations, however, are of limited generality and uncertain accuracy. Typical examples of this kind of approach are the publications by Koshkin et al. [9] and Dreitzer et al. [10].

Numerous analytical studies related to prediction of convection heat transfer under transient wall-heat flux conditions may be found in the literature. However, all these investigations consider idealizations, e.g., with regard to the boundary conditions applied, and/or are restricted to simple one-dimensional duct configurations such as the circular tube, the parallel plate channel or the concentric annulus (cf., for instance, reference 11). Probably the most detailed of these investigations which is also most useful in view of its close simulation of typical nuclear reactor conditions was performed by Gopalakrishnan [12, 13] who treated the case of forced-convective turbulent flow in a parallel-plate channel with transient internal heat generation within the duct wall. The numerical (finite-difference) solution of the two coupled two-dimensional time-dependent energy equations for the wall and the fluid regions has been applied to a number of specific cases of exponentially increasing heat generation. Results also clearly demonstrated that, for rapid heat generation transients which are of interest to nuclear reactor safety, use of a steady-state heat transfer coefficient in eq. (1) would be incorrect.

Though certainly a considerable amount of important information regarding the fundamental aspects of transient heat transfer at typical reactor conditions has been gained by Gopalakrishnan's study, the question has remained whether his results can directly be transferred to the geometrically rather different rod bundle geometry. In the present study, thus emphasis has been placed to correctly model both the cylindrical fuel rod consisting of fuel, gap and cladding and the two-dimensional fluid cross-section associated with a rod in the center region of a reactor fuel bundle. Further, turbulent energy transport within the coolant has been represented in a more consistent manner over the entire Prandtl number range of interest using results from a specially tailored phenomenological turbulence model [14, 15].

The present analysis is restricted to the single-phase-flow part of an arbitrarily prescribed power transient with steady-state flow. Inclusion of a simultaneous flow transient in an approximate manner, i.e., specifying a decay of the coolant's mean velocity input and applying a quasi-steady approach [16],
may be subject of a rather simple further extension. An analysis of this kind allows determination of position and time of boiling inception as well as gives a complete description of three-dimensional temperature fields, at boiling inception, of both the fuel rod and the coolant. Results which, in this paper, concentrate on exponentially increasing power transients, may also be used to examine the errors resulting from application of eq. (1) and the different approaches to evaluate a transient heat transfer coefficient.

2. Description of the Problem

2.1 Geometry and Assumptions

The analysis deals with the transient non-boiling heat transfer of heat-generating nuclear fuel rods to a steady, fully-developed, turbulent flow. The fuel rods are regularly arranged in either a rectangular or a triangular array of infinite extent; the fluid flow is parallel to the axis of the rods (Fig. 1). Provided that the heat generation rate of the rods is equal and, within each rod, invariant with the circumferential coordinate, the analysis may be confined to the domains in the interval \( 0 \leq r \leq 1 \) for reasons of symmetry as illustrated in Fig. 1 by the cross-hatched areas. The parameter \( s \) has the values 6 and 4 for a triangular array and a rectangular array, respectively. The regions considered are that of the fuel for \( 0 \leq r \leq R_1 \), that of the gap (or bonding) for \( R_1 \leq r \leq R_2 \), that of the clad for \( R_2 \leq r \leq R_3 \) and that of the coolant for \( R_3 \leq r \leq 0 \) (2 cos \( 4 \)) (Fig. 2).

In the axial direction, a pure, Newtonian, incompressible fluid with temperature-invariant properties flows steadily along the rods; the flow is turbulent and fully developed and has a uniform inlet temperature. At the axial position \( z=0 \), heat generation within the fuel starts, and for \( z>0 \) the volumetric heat-generation rate is a specified function of axial position and time.

Additional assumptions underlying the analysis are: Radiative and electromagnetic energy and energy transport are excluded; negligible viscous dissipation of heat within the fluid; negligible nuclear heat generation within gap, clad and coolant; negligible axial conduction; no phase change of materials during the transient; materials may be considered as isotropic continua. The properties of the material within the gap between fuel and clad may be taken constant.

2.2 Governing Differential Equations

In view of the above assumptions, the governing differential energy equations for transient heat transfer in the different material regions can be expressed in usual notation as

\[
\rho_1(T_1) \frac{\partial T_1}{\partial t} = \kappa_1(T_1) \nabla^2 T_1 + q''(r,z,t),
\]

\( t>0; \ 0<r<R_1; \ 0<z<s \) (2)

\[
\rho_2 c_2 \frac{\partial T_2}{\partial t} = k_2 \nabla^2 T_2.
\]

\( t>0; \ 0<r<R_2; \ R_1<r<R_2; \ 0<s<z/s \) (3)
\[
\rho_3(T_3) C_{p3}(T_3) \frac{\partial T_3}{\partial t} = \nabla \cdot \left[ k_3(T_3) \nabla T_3 \right], \tag{4}
\]

\( t > 0; \ 0 < z < L; \ R_2 < r < R_3; \ 0 < \phi < \pi/s \)

\[
\rho_4 C_{p4} \frac{\partial T_4}{\partial t} + \rho_4 C_{p4} u_2(r, \phi) \frac{\partial T_4}{\partial z} - \frac{1}{r} \frac{\partial}{\partial r} \left[ r \left( k_4 + \rho_4 C_{p4} c_{h4}(r, \phi) \right) \frac{\partial T_4}{\partial r} \right] + \frac{1}{r^2} \frac{\partial}{\partial \phi} \left[ \left( k_4 + \rho_4 C_{p4} c_{h4}(r, \phi) \right) \frac{\partial T_4}{\partial \phi} \right] \tag{5}
\]

\( t > 0; \ 0 < z < L; \ R_3 < r < R/F/(2 \cos \phi); \ 0 < \phi < \pi/s. \)

The subscripts 1, 2, 3, and 4 pertain to the fuel, gap, clad, and coolant regions, respectively. In eq. (5), it is assumed that the timescale involved in averaging turbulent fluctuations is small compared to that of the transients. Note, however, that anisotropy of turbulent heat transport is considered taking different eddy diffusivities of heat in the radial and circumferential directions. Velocity components in the plane perpendicular to the axial flow direction, i.e. secondary flow effects of the second kind, have been tacitly neglected.

### 2.3 Initial and Boundary Conditions

A general set of initial conditions for the transient problem can be specified as

\[
T_4(r, \phi, z, 0) = T_{40}(r, \phi, z), \tag{6}
\]

\[
q_i''(r, z, 0) = q_{i0}''(r, z), \tag{7}
\]

where \( T_{40} \) designates solutions to the steady-state formulations of eqs. (2) to (5) for given steady heat-generation rate \( q_i'' \) in the fuel (\( i = 1, 2, 3, 4 \)). It is obvious that an unlimited number of initial conditions may be generated from eqs. (2) to (5) which depend on the particular choice for \( q_i'' \) and the temperature boundary conditions at \( z = 0 \). For example, \( q_i'' = 0 \) and \( T_{40}(z = 0) = T_{40,\text{in}} \) = const. for all \( i \) corresponds to the special case of an initially isothermal state.

Symmetric requirements applied to the boundary \( \phi = 0 \) and \( \phi = \pi/s \) of the domain shown in Fig. 2 result in the following boundary conditions of the four regions:

\[
\frac{\partial T}{\partial \phi} = 0; \ \phi = 0 \text{ and } \pi/s; \ O \leq z \leq L; \ t \geq 0. \tag{8}
\]

The fuel temperature distribution is symmetric with respect to the radial coordinate at \( r = 0 \), hence:

\[
\frac{\partial T}{\partial \phi} = 0; \ r = 0; \ O \leq \phi \leq \pi/s; \ O \leq z \leq L; \ t \geq 0 \tag{9}
\]

The general joining conditions at the interfaces between the adjacent regions \( i = 1, 2, 3 \) may be expressed by
\[ -k_1 \frac{\partial T}{\partial r} = T_i - T_{i+1}; \quad r=R_1, \quad 0 \leq \tau / \tau_0, \quad t>0; \]

\[ -k_1 \frac{\partial T_i}{\partial r} = k_1 \frac{\partial T_{i+1}}{\partial r}; \quad r=R_1, \quad 0 \leq \tau / \tau_0, \quad t>0, \]

where \( \delta \) in the boundary conditions of the third kind, eq. (10), denote circumferentially uniform contact resistances between the different regions. In the special case \( \delta = 0 \), eq. (10) degenerates to the Dirichlet boundary condition which has been applied in the present analysis.

At the outer boundary of the characteristic flow area, \( r=P/(2 \cos \phi) \), the coolant temperature field is assumed to satisfy the Neumann boundary condition

\[ (k_0 + \rho c_0 p_0) \frac{\partial T}{\partial r} = (k_0 + \rho c_0 p_0) \frac{1}{r} \frac{\partial T}{\partial \phi}, \]

\[ r=P/(2 \cos \phi); \quad 0 \leq \phi \leq \pi; \quad 0 \leq \tau / \tau_0. \]

It should be noted that this formulation of the boundary condition (as well as eq. (5)) is an approximation only in view of the tensor properties of the eddy diffusivity of heat, \( c_{\phi} \). This question has previously been discussed, for example, by Nijssing [17].

The temperature distribution at the beginning of the heated section of the channel may, in general, be specified as an arbitrary function of time. In the present paper, all results were obtained for a uniform, constant temperature in all the regions at \( t=0 \):

\[ T_i(r, \phi, 0, t) = T_{i,\text{in}}(r, \phi, t) = \text{const.}; \quad 0 \leq \tau / \tau_0, \quad t>0. \]

2.4 Velocity Distribution and Turbulent Transport Properties of Heat

The velocity distribution of the fully-developed turbulent flow in the coolant area is obtained by application of a phenomenological turbulence model [14] whose results have previously been shown to agree reasonably well with experimental velocity data in infinite rod arrays [18]. The same model is also used to generate the distributions of the eddy diffusivities of heat in the radial and circumferential directions. For liquid-metal heat transfer in rod bundles which the present paper concentrates on, correlations for these diffusivities have recently been published [15].

3. Numerical Solution

The four coupled three-dimensional, time-dependent, (in part) nonlinear differential equations (2) to (5) and their associated subsidiary conditions were transformed into non-dimensional forms, and then solved via finite-difference techniques using a digital computer program. For reason of stability, the implicit method was adopted for the axial and time directions. Having chosen an appropriate time increment for the particular transient problem under consideration, a complete two-dimensional "entrance" solution is performed at each time step. In course of this entrance solution which proceeds in flow direction according to the size of the axial increment chosen, the
two-dimensional \((r,s)\)-problem in the plane perpendicular to the \(z\)-axis is solved iteratively via the alternating direction method at each axial position. The latter method has performed favorably in view of computer time and storage requirements. The accuracy of the computer solution could be tested with respect to the mean fluid temperature for the special case of three-di-

3. Sample Calculations

Most of the sample calculations have been performed for geometric and operating conditions typical for liquid-metal cooled fast breeder reactors (LMFBR) and pressurized water reactors (PWR). The data used for the LMFBR cal-

4. Discussion of Results

Most of the results presented in the following are for LMFBR conditions. Fig. 3 is an example of the calculated variation of the Nusselt number with axial distance and time for a very fast transient \(t_o = 1\) ms in a blanket as-

To demonstrate the effect of the numerical value taken for the exponen-

tial period \(t_o\) of the transient, the Nusselt number at the midth of the heated section, \(z/L = 0.5\), has been plotted versus dimensionless time for different transients of both fuel and blanket assemblies (Fig.4). Results of both the multi-region analysis and a single-region analysis for the coolant only, where the surface heat flux was assumed to vary in the same manner as the heat gene-

ratable flux where an analytical solution could be obtained. The compari-

son of results, though certainly somewhat limited, revealed satisfactory agreement provided that the time and axial increments where appropriately cho-

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\[
q''(z,t) = q''_{max} \cos\left(\frac{\pi z}{L}\right) \exp\left(\frac{t}{t_o}\right),
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where \(t_o\) is the period of the transient. The coolant properties were evaluated at the inlet coolant temperature and taken invariant with po-

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sition and time.
tic Nusselt numbers exceed the steady-state values at initial conditions the more the smaller the period of the transient is. The appreciable thermal heat capacity as well as internal thermal resistance of the fuel rod causes a time-lag to occur before the Nusselt number begins to increase. The use of results for the heat-transfer coefficient which are obtained from single-region analyses which exclude this effect will therefore lead to a serious overestimation of surface heat transfer at early times and, hence, to an underestimation of surface temperatures. For large times, the application of single-region results for Nu would only slightly be in error.

In Figs. 5 and 6, single- and multi-region results for the Nusselt number and the temperature difference between dimensionless circumferentially averaged rod surface temperature \( \theta_{3,av} \) and average coolant temperature \( \theta_{4,av} \) are compared with a corresponding result from Stein's "engineering relationships" \( [7,8] \) \( \theta_{3} = \frac{(T_{3} - T_{3,in})}{(q''_{av} R_{3}^{2}/2k_{3})} \). Stein's relationships are certainly attractive for engineers to apply due to their simplicity, however, their accuracy as well as parameter ranges of applicability are uncertain. For instance, no physically meaningful result could be obtained from Stein's relation for the case \( t_{0} = 1 \) ms. For the case \( t_{0} = 5 \) ms, the three results for Nusselt number evaluated by application of the different orders of approximation \( (R_{1} \text{ to } R_{3} \text{ approximations}) \) did not seem to reveal any major improvement (except for physical correctness along the entire heated length) when compared to the present single-region result (Fig. 5). Relative to the wall temperature difference, reasonable agreement exists between Stein's result and the present corresponding single-region result, except for early times. No agreement is found with respect to the appropriate multi-region result.

In Fig. 7, the effect of dimensionless time on the maximum circumferential temperature difference at \( z/L = 0.5 \) is studied. At any axial position, the maximum temperature at the outer cladding surface occurs at the closest position to the neighboring rod, i.e., where \( \phi = 0 \) (cf. Fig. 2) while the minimum temperature is found at \( \phi = \pi/3. \) This temperature difference, though - at normal operating conditions - rather small at interior rods but of appreciable magnitude at rods located near to the duct wall, will increase exponentially at large times when the shape of the temperature distribution has reached its asymptotic state. Again, single-region analysis would overestimate the wall-temperature difference.

Similar calculations of power transients for typical PWR fuel rods starting from normal, steady-state operating conditions did not reveal any time-variations in surface heat transfer before complete fuel melting would have occurred. This behavior is in line with the well-known fact that, with increase of the fluid's Prandtl number, the effect of axial or time variations of surface heat flux strongly diminishes. Due to the rod's thermal capacity and internal thermal resistance, the time variation of surface heat flux is negligible compared to that of the heat generation rate in the fuel at early times, and by far to small to affect the convective heat transfer in any notable manner. Hence, in water-cooled reactors, use of the steady-state heat-transfer
coefficient for evaluating power transients would be completely adequate during the non-boiling phase.

The situation would be different for thin-walled heaters as used in transient boiling tests [19]. In Fig. 8, we have compared an experimental result by Johnson et al. [19] with analytical results by Gopalakrishnan for a parallel-plate channel and a present square rod array result. Obviously, the channel geometries used in the experiments and applied in the various analyses are rather different. Since complete similarity cannot be obtained, geometry for our comparative result has voluntarily been based on the equality of hydraulic diameters in experiment and theory; further, the outer rod diameter used in analysis was chosen equal to the heater thickness. Results of a calculation based on those assumptions (other parameters equal) are plotted in Fig. 8 and compare favorably with the experimental data.

References


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**Fig. 1** Infinite arrays of parallel rods
Table I. Input Parameters for LMFBR Sample Calculations

<table>
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<tr>
<th>Array</th>
<th>CASE 1</th>
<th>CASE 2</th>
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<td>Inner cladding diameter, 2 R₂ (cm)</td>
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<td>Average linear power in fuel, qᵢᵥ (W/cm)</td>
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<td>300</td>
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<td>Axial power distribution cos(ณz/L)</td>
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<td>Asymptotic Nusselt number for axially uniform heat flux, Nuᵢ, at coolant inlet conditions</td>
<td>4.54</td>
<td>13.28</td>
</tr>
</tbody>
</table>

Fig. 2 Characteristic section of infinite rod array
Fig. 3
Development of the Nusselt number distribution during an exponential transient of a blanket assembly

Fig. 4
Effect of an exponential period on the Nusselt number for transients initiated from an established steady power level
Fig. 5
Axial Nusselt number profiles at different times for exponential transients of a fuel assembly as calculated by single-region analysis. Comparison with Stein's result.

Fig. 6.
Comparison of single-region and multi-region results for the wall temperature rise during an exponential transient of a fuel assembly with an analytical result by Stein [7,8].
Fig. 7.
Effect of exponential period on the dimensionless maximum circumferential temperature difference at outer cladding wall for transients of a fuel assembly

Fig. 8.
Comparison of transient heat transfer data of Schrock et al. [19] with analytical results (Run No. 19403)