GAP CONDUCTANCE IN ZIRCALOY-CLAD LWR FUEL RODS

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J B Ainscough

SUMMARY

This Report describes the procedures currently used to calculate fuel-cladding gap conductance in light water reactor fuel rods under both steady-state and transient conditions. The relevant theory is discussed together with some of the approximations usually made in performance modelling codes. The state of the physical property data which are needed for heat transfer calculations is examined and some of the relevant in and out-of-reactor experimental work on fuel rod conductance is reviewed.
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1. INTRODUCTION

In any discussion of the thermal behaviour of light water reactor (LWR) fuel rods it is necessary to distinguish between two regimes of heat production and removal. The first is that appropriate to normal operation, whilst the second applies in transient situations. In the present Report, the only transient considered will be the successfully terminated loss-of-coolant accident (LOCA). Normal steady-state operation is characterised by near-constant reactor and rod power levels and also by the steady removal of heat from the fuel through the cladding to the coolant. A LOCA, on the other hand, is characterised by rapid changes in power and in cladding-to-coolant heat transfer. Because the conditions change so quickly, it is necessary to apply transient heat transfer equations to determine the response of the fuel and cladding.

In both steady-state and transient thermal analysis, the assumption is usually made that there is radial heat flow only and this permits the concept of gap conductance to be used to describe the flow of heat across the boundary between fuel and cladding.

This Report reviews current procedures for calculating gap conductance in LWR fuel rods containing pelletized UO₂ in Zircaloy cladding. The first part considers the state of the theory and some of the approximations which are usually made, whilst the second part examines the physical property data which are needed for heat transfer calculations, and reviews some of the relevant experimental work. Two arbitrary limits have been placed on the scope of the Report. First, it is concerned wholly with processes occurring within the cladding, and does not address the problem of cladding/coolant heat transfer. Second, whilst the thermal conductivities of single gases and of gas mixtures within a fuel rod are discussed, there is no discussion of the fission gas release processes which are primarily responsible for the changes in gas composition which occur in an operating rod.

The literature on heat transfer across small gaps and between surfaces in contact is large, especially insofar as the theoretical aspects are concerned. Many of the papers in this area achieve a degree of sophistication which is inappropriate to fuel rod performance related calculations where the necessary input parameters are often imperfectly known. This Report, then, considers the theory only to the extent to which it is currently used in the performance modelling context, though references are given to some of the more detailed background papers.

2. STEADY-STATE HEAT TRANSFER WITHIN A FUEL ROD

Heat transfer from fuel to cladding to coolant may be summarised in terms of two expressions:

\[-\nabla \left[ k_f(T) \nabla T_f(r, \theta, z) \right] = Q_f(r, \theta, z) \quad \cdots (1)\]

\[-\nabla \left[ k_c(T) \nabla T_c(r, \theta, z) \right] = Q_c(r, \theta, z) \quad \cdots (2)\]

* See Appendix 1 for notation
These are coupled by the steady-state condition that heat lost by the fuel must equal heat gained by the cladding, which in turn is gained by the coolant.

Assumptions and approximations are almost invariably made to simplify these equations in steady-state fuel rod performance computer codes. Some of the most important assumptions are:

(a) There is no source of heat (i.e. gamma heating) within the cladding, so that \( Q_c = 0 \). This is a very minor approximation for standard fuel rod designs.

(b) There is no azimuthal variation of fuel heat generation, so that \( Q_f(r, \theta, z) = Q_f(r, z) \). This is not always a good approximation though the practical effects are small in terms of shift in peak temperature, change in volume average temperature, fission gas release, and cladding temperatures in a transient, except for large azimuthal variations of \( Q_f \). Reference 1 gives a more detailed treatment of this topic.

(c) There is azimuthally uniform cladding-to-coolant heat transfer.

(d) There is azimuthally uniform fuel-to-cladding conductance. This condition would be violated if there were pellet eccentricity within the cladding which would cause azimuthal variation in gap size and hence conductance. The practical effects of this for LWR rods of normal design with helium as filling gas have, however, been shown to be slight.(2,3) Larger effects have been found in a xenon-filled rod.(4)

(e) There is no axial heat flow. This is clearly untrue since the axial power profile in a fuel rod results in axial temperature gradients which will themselves impact on the gap conductance. However, axial gradients are typically two orders of magnitude less than radial gradients, so that axial heat flow is generally ignored. The localised axial heat flow that can accrue from extreme axial variation of pellet eccentricity is discussed in Ref. 3.

Given these approximations, heat is allowed to flow only in the radial direction so that the fuel and cladding temperatures at a given axial location are therefore functions of radius alone and the heat transfer equations reduce to:

\[
- \frac{1}{r} \frac{d}{dr} \left[ k_f \frac{dT_f}{dr} \right] = Q_f(r) \tag{3}
\]

\[
- \frac{1}{r} \frac{d}{dr} \left[ k_c \frac{dT_c}{dr} \right] = 0 \tag{4}
\]

These equations require a total of four boundary conditions:

\[
\frac{dT_f}{dr} \bigg|_{r = r_{f1}} = 0 \tag{5}
\]
\[-k_c \frac{dT_c}{dr} \bigg|_{r = r_{ci}} = \frac{P}{2 \pi r_{ci}} \quad \ldots \quad (6)\]

\[\frac{P}{2 \pi r_{fs}} = h \left[ T_{fs} - T_{ci} \right] \quad \ldots \quad (7)\]

\[-k_c \frac{dT_c}{dr} \bigg|_{r = r_{cs}} = h_{film} \left[ T_{cs} - T_{cool} \right] \quad \ldots \quad (8)\]

In the above, \( P \) is linear power and \( r_{fi} \) will generally be zero.

In equations (1–8) the thermal conductivities of UO\(_2\) and Zircaloy appear as constants. This is not in fact the case and is responsible for much of the complexity in performance modelling codes. Thermal conductivity is a function of temperature. In addition, the conductivity of UO\(_2\) varies with fuel density and, in principle at least, with burn-up. To complicate matters further, \( Q_f \) will vary spatially as a result of self-shielding and may also change with burn-up as a result of depletion of \( ^{235}U \) and the formation and fissioning of plutonium. These points will be taken up in more detail in later Sections of this Report. The equations are also highly non-linear because the gap conductance depends on both the fuel and the cladding temperatures. An iterative solution procedure must, therefore, be followed to find a temperature distribution that simultaneously satisfies the heat transfer equations and the gap conditions.

3. GAP CONDUCTANCE

The conductance across the gap or interface between UO\(_2\) and Zircaloy may be considered as the sum of three terms: heat transfer across the gap by conduction through the gas, \( h_g \); solid conductance across points or areas of contact between fuel and cladding, \( h_s \); and, finally, a radiative heat transfer term, \( h_r \); so that

\[ h = h_g + h_s + h_r \quad \ldots \quad (9)\]

Convective heat transfer within the gap is generally neglected.

It is also normally assumed that the gas composition is uniform throughout the fuel rod, though it is theoretically possible for thermal diffusion to produce a concentration gradient if more than one gas is present. The lighter gases would tend to concentrate at the top of the rod, so enhancing the local gap conductance, whilst the opposite effect would occur at the bottom consequent upon an increased concentration of heavier gases.\(^{(5)}\)

3.1 CONDUCTANCE THROUGH THE GAS

Typical design gaps between fuel and cladding in LWRs are small, about 1 to 2% of the fuel radius. After correction for differential thermal expansion the hot gaps during operation are even smaller. Most performance codes treat gaps of this magnitude as 'open', although such practice has often been questioned. The hot gaps are so small that, for light gases such as helium, the approximation
\[ h_g = \frac{k_{\text{gas}}}{d} \quad \ldots (10) \]

where \( d \) is the open gap width, is inadequate and must be replaced by a form such as

\[ h_g = \frac{k_{\text{gas}}}{d + d_{\text{min}} + g_f + g_c} \quad \ldots (11) \]

where \( d_{\text{min}} \) is related to the roughnesses of the two surfaces and \( g_f \) and \( g_c \) are 'temperature jump distances' at the fuel and cladding surfaces respectively. These jump distances are extrapolations of the true gap size to account for discontinuities in temperature at the bounding surface of a gas. The discontinuities arise not only from imperfect energy exchange between the gas and the surface, but also because the probabilities of a gas molecule colliding with another gas molecule and with a solid surface are markedly different.\(^6\) Figure 1 illustrates the way in which the temperature discontinuities are converted to an equivalent length which is then added to the gap width.

A classical expression for the jump distance\(^6\) is:

\[ g = \left( \frac{2 - a}{a} \right) \cdot \frac{4K}{\gamma + 1} \cdot \frac{k_{\text{gas}}}{\mu_{\text{gas}}} \cdot t \quad \ldots (12) \]

where \( a \) is the accommodation coefficient and \( K \) is a constant with a value of approximately 0.5. If \( a = 1 \) the temperature jump distance of a monatomic gas is slightly greater than the mean free path. Equation 12 may be rewritten in terms of more commonly measured parameters as

\[ g = A \left( \frac{k_{\text{gas}} (T_{\text{gas}})^2}{P_{\text{gas}}} \right) \left( \frac{2 - a}{a} \right) M^2 \quad \ldots (13) \]

where \( A \) is a constant.

Recent measurements of the accommodation coefficients of helium and xenon on stainless steel and \( \text{UO}_2 \) have been made by Ullman et al\(^7\) at temperatures between 500 and 1200K. For both gases \( a \) decreases with increasing temperature, with temperature dependencies which can be described by

\[ a_{\text{He}} = 0.425 - 2.3 \times 10^{-4} T \quad \ldots (14) \]

\[ a_{\text{Xe}} = 0.749 - 2.5 \times 10^{-4} T \quad \ldots (15) \]

for \( T \) in kelvins.

A more important consideration is the definition of the gap width. In equation (11) it has been written as the sum of two terms: an actual gap width, often calculated from the as-assembled gap sizes and the thermal expansions of the fuel and the cladding, and a term to take account of the effect of the surface roughnesses. This second term generally has the form \( A(R_f + R_c) \), where \( A \) is a constant and \( R_f \) and \( R_c \) are the roughnesses of the two surfaces, so that equation (11) becomes:
\[ h_{\text{gas}} = \frac{k_{\text{gas}}}{d + A(R_f + R_c) + g_f + g_c} \quad \ldots (16) \]

The actual value of \( d \) in an operating fuel rod will be considered in Section 10.

The thermal conductivity of a gas increases with temperature and, for most gases of importance in fuel rod performance analysis, the temperature dependence can be accurately described by equations of the form:

\[ k_{\text{gas}} = AT_{\text{gas}}^S \quad \ldots (17) \]

where \( A \) and \( S \) are constants.

The thermal conductivity of steam, which may be required in the analysis of defected rods, cannot be described by equation (17). Hagman\(^8\) gives an expression for \( k_{\text{steam}} \) together with the requisite constants.

An operating fuel rod frequently contains not one but a mixture of several gases and it is necessary to consider the conductivity of gas mixtures and also the temperature jump distance under such conditions. Many expressions have been proposed for calculating the thermal conductivity of a mixture of gases. These have differing amounts of complexity and of physical realism. It is sufficient here to state that satisfactory expressions do exist and to cite only two of them. For monatomic gases Hirschfelder, Curtiss and Bird\(^9\) proposed the equation:

\[ k_{\text{mix}} = \sum_{i=1}^{n} \frac{x_i k_i}{\sum_{j=1}^{n} x_j s_{ij}} \quad \ldots (18) \]

where \( s_{ij} = \frac{1}{2/2} \left( 1 + \frac{M_i}{M_j} \right)^{-\frac{3}{2}} \left( 1 + \frac{k_i}{k_j} \right)^{\frac{3}{2}} \left( \frac{M_i}{M_j} \right)^{\frac{1}{2}} \)

whilst Mason and Saxena\(^10\) recommended that the \( s \) term of equation (18) be multiplied by 1.065 and also that the \( s \) term should not be used when \( i = j \). The Mason and Saxena equation is, therefore:

\[ k_{\text{mix}} = \frac{\sum_{i=1}^{n} x_i k_i}{\left[ x_i + \sum_{j=1, j \neq i}^{n} x_j s_{ij} \right]} \quad \ldots (19) \]

with

\[ s_{ij} = 0.3765 \left( 1 + \frac{M_i}{M_j} \right)^{-\frac{3}{2}} \left( 1 + \frac{k_i}{k_j} \right)^{\frac{3}{2}} \left( \frac{M_i}{M_j} \right)^{\frac{1}{2}} \]
With this adjustment, equation (19) produces a good fit to the near room temperature He/Xe measurements of Thornton and of Srivistava quoted in Ref. 11, but over-predicts by about 6% when compared with the measurements of von Ubisch et al. (12)

The jump distances for the UO$_2$/gas and the Zircaloy/gas interfaces, $g_f$ and $g_0$ in equation (11), are usually combined into a single combined jump distance, $g$. As is clear from equation (13), $g$ is a function of the gas in contact with the surface. In a gas mixture the following expression may be used to obtain an overall jump distance

$$ g = g_1^{x_1} \cdot g_2^{x_2} \cdot \ldots \cdot g_i^{x_i} $$ ...

where $x_i$ is the mole fraction of the $i$th component with jump distance $g_i$. Equations (13) and (17) can be combined to give

$$ g = g_0 \left( \frac{P_0}{P_{\text{gas}}} \cdot \left( \frac{T_{\text{gas}}}{T_0} \right) \right)^{S + 0.5} $$ ...

where $P_0$ and $T_0$ are the reference pressure and temperature, generally 0.1013 MPa and 273K.

It is important to realise that, because of the form of equation (16), a gap conduction term will contribute to the overall heat transfer between fuel and cladding even when the two surfaces are in contact.

It has been assumed throughout this Section that the only species present in the fuel/cladding gap are gaseous. However, post-irradiation examination of irradiated fuel rods often shows additional, principally oxide, phases in the gap. Fission products are generally associated with these phases. So far as the author is aware, no account is taken of the effect of these phases in any fuel performance modelling code used for LWRs.

Finally, it must be pointed out that equation (16) is only valid so long as $[d + A(R_f + R_o)]$ is large compared with the mean free path of the gas molecules in the gap. The mean free path is greatest for the lighter gases, and for helium at 273K and atmospheric pressure is of the order of 0.1 $\mu$m. It will decrease further with increasing temperature and/or pressure. The surface roughnesses of fuel and cladding are, typically, about 1 $\mu$m (r.m.s.) and the constant, $A$, is generally given values in the range 1.5-2.5 so that, unless there exist interfacial pressures which are sufficient to allow deformation of the contacting surfaces, deviations from classical gap conductance ought to be undetectable. Some of the experimental evidence to be discussed later suggests that this may not be so.

3.2 SOLID CONDUCTANCE

In addition to conduction across an open gap, heat can flow through points of fuel/cladding contact. In the literature some quite complicated models have been proposed for the contact case (13-15) which, according to Fench and Rosenthal, (15) allow conductance to be predicted to within 20-30%. The prerequisites for this are that the fuel surface configurations are known very accurately and also that, after contact has been established, there is
no cycling which would cause relative movement of the contacting surfaces. Neither of these conditions applies during the irradiation of LWR fuel rods, so that greater inaccuracies must be expected from heat transfer calculations. Equally there is little to be gained from using complex equations. Neglecting the problems of surface waviness and error of form, the expression most commonly used to define the solid conductance term is:

\[ h_s = A k_m \left( \frac{P_a}{P_i} \right)^n f(\text{surface}) \] ... (22)

where \( A \) is a constant, \( k_m \) is the geometric mean conductivity of the fuel and cladding such that \( k_m = 2 k_f k_o/(k_f + k_o) \), \( P_a \) is the apparent interfacial pressure and \( H \) the Meyer hardness of the softer material, in this case the cladding.

The \( f(\text{surface}) \) term in equation (22), which provides a measure of surface roughness, requires some explanation. A rough surface needs two parameters, a wavelength and an amplitude, in order to define it. Theoretically it is necessary to know both. However, for normal mechanical finishing methods, the ratio of amplitude to wavelength is fairly constant and either measurement appears to be satisfactory in correlating measured conductances with surface topography. It is usual to use the surface roughnesses of fuel and cladding in some combination. Hobbs, in an unpublished analysis\(^{(16)}\) of much of the available literature up to 1973, argued that solid conductance was inversely proportional to the square root of the sum of the roughnesses of fuel and cladding so that

\[ h_s = A' \frac{k_m}{R_T} \left( \frac{P_a}{H} \right)^n \] ... (23)

Ross and Stout\(^{(17)}\) define \( R_T \) somewhat differently. In their analysis

\[ R_T = \left[ \left( R_T^2 + R_c^2 \right)/2 \right]^{1/4} \] ... (24)

The theoretical value of the exponent \( n \) in equation (22) depends on the way in which the points of contact between fuel and cladding behave under the influence of an interfacial pressure. It the radii of the contact spots do not change with pressure but their number does, then \( h_s \propto P \). If, on the other hand, a constant number of contact spots is assumed, the radii of which increase with increasing pressure, then \( h_s \propto P^2 \).

3.2.1 The interaction of solid and gas gap conductance

In equation (9) the total gap conductance is shown as the sum of three independent terms. However the presence of a gas in the interface between two surfaces in contact perturbs the solid/solid conductance. Figure 2 shows the changes in the heat flow path caused by conduction through the gas. The most significant change is the movement of the isothermal plane closer to the plane of contact, which has the effect of increasing the flow of heat through the solid. The theory has been developed by Mikic\(^{(18)}\) and can be incorporated into the equations already described provided the ratio \( b/a \) (Fig. 2) is known.
So far as the author is aware, this degree of refinement is not used in any of the modelling codes mentioned later and its complexities will not be discussed further here.

3.3 RADIATIVE HEAT TRANSFER

The third mode of heat transfer between fuel and cladding is thermal radiation. In steady-state operation it is of little importance, except possibly for large gap rods in which extensive fission gas release has occurred with consequent degradation of the thermal conductivity of the original filling gas. However, in the LOCA transient situation, temperatures in the gap will be higher than in normal operation and the radiation term correspondingly more significant. The expression for $h_r$ is:

$$h_r = \frac{\delta \varepsilon_f \varepsilon_c}{(\varepsilon_f + \varepsilon_a - \varepsilon_f \varepsilon_c)} \frac{(T_{fs}^4 - T_{cl}^4)}{(T_{fs}^4 - T_{cl}^4)}$$

... (25)

where $\delta$ is Stefan's constant, $\varepsilon_f$ and $\varepsilon_a$ are the surface emissivities and $T_{fs}$ and $T_{cl}$ are the corresponding absolute surface temperatures of the fuel outer and cladding inner surfaces. Since the radiation term varies as $T^5$ it becomes very important at high temperatures.

4. MODELLING OF STEADY-STATE OPERATION

The calculation of gap conductance during steady operation is usually carried out as part of an overall calculation of fuel rod performance. For this purpose many modelling codes of differing degrees of complexity have been developed, amongst which can be mentioned ELESTIM(19) GAPCON-2(20) GAPCON-3(21) FRAPCON-1,(22) HOTROD,(23) MINIPAT(24) and COMETHE. All these codes contain a gap conductance iteration for each time step at each axial node of the rod. They also calculate fission gas release, since the contamination of the normal helium filling gas by fission gas will degrade the gas thermal conductivity and hence alter the gap conductance. Hence the dependence of the gap conductance upon gas composition, gap temperature, effective gap size and interfacial pressure is central to the logic of these codes. Other sections of the codes calculate fuel and cladding elastic deformations, including expansion and pressure loads, fuel relocation, fuel and cladding creep, fuel swelling, and fuel densification. All these sections, which interact as burn-up proceeds, impact on the calculated gap size. They can be classified according as to whether they tend to open or close the gap, as follows:

<table>
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<th>Gap opening</th>
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<tr>
<td>Cladding thermal expansion</td>
<td>Fuel thermal expansion</td>
</tr>
<tr>
<td>Fuel densification</td>
<td>Fuel swelling</td>
</tr>
<tr>
<td>Cladding response to internal pressure</td>
<td>Cladding response to external pressure</td>
</tr>
<tr>
<td></td>
<td>Fuel relocation</td>
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The use of steady-state modelling codes also constitutes an essential step in the modelling of fuel/cladding heat transfer in a LOCA, since these
codes predict the conditions in a fuel rod immediately prior to the start of the transient. In particular the energy, $E_s$, stored within the fuel (which is a key parameter during a LOCA since it determines the cladding temperature that will prevail at the start of 'adiabatic' heat-up and which, in turn, affects the peak cladding temperature attained in advance of reflood) can be calculated from the radial temperature profile using

$$
E_s = \sum_{i=1}^{n} 2 \pi \rho_f \int_{r_i}^{r_f} r_i C_f(T) \, dr \quad \text{(26)}
$$

where $n$ is the number of arbitrary rings in the fuel, $T_{\text{ref}}$ is the reference temperature for the calculation of stored energy, and $T_i$ is the average temperature of the $i$th ring of radius $r_i$.

Although computer codes have been developed to predict fuel rod temperatures under varying conditions, the uncertainty in the prediction is important because of its bearing on the analysis of accident conditions. The wider the range of possible conditions prior to a transient, the wider the range of possible fuel rod responses to that transient. Table 1 lists as an example, some input uncertainties that have been used in a PNL study{26} as being applicable to LWR fuel rods at first start-up.

As burn-up increases, however, these uncertainties will change. Fission gas release will produce an uncertainty in filling gas composition and thermal conductivity. Depletion of fission isotopes, and conversion of fertile to fissile isotopes, will change the radial power profile. Cracking and relocation of the fuel may change the effective thermal conductivity of the fuel and affect the gap conductance. All these effects can change the uncertainty of the fuel temperature and hence of the stored energy as a function of burn-up.

The work reported in Ref. 26 has shown that the relative uncertainty for BWRs and PWRs is the same, if the same input uncertainties are assumed. Figures 3 and 4 show the relative uncertainty in calculated centre-line temperature and stored energy, respectively, at a linear rating of 50 kW.m$^{-1}$ at start-of-life with fuel/cladding contact and pure helium filling gas. It is clear that there is a much higher dependence on power uncertainty than on the uncertainty in fuel thermal conductivity, flux depression or specific heat.

5. **TRANSIENT HEAT TRANSFER**

This Section considers the heat transfer equations applicable under the transient conditions of a LOCA and highlights the differences from the corresponding steady-state equations.

The assumptions mentioned in Section 2 are usually applied to the transient fuel and cladding heat transfer equations, thus reducing the spatial dependence of temperature to the radial direction only. The simplified equations are then:

$$
\rho_f C_f \frac{\partial T_f}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r} \left[ r k_f \frac{\partial T_f}{\partial r} \right] + Q_f(r) \quad \text{(27)}
$$
\[ \rho C_c \frac{\partial T_c}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r} \left( r k_c \frac{\partial T_c}{\partial r} \right) \] ... (28)

Not only do these equations differ from equations (3) and (4) in Section 2 by the addition of the energy storage term on the left hand side, but the coupling conditions are also different. The heat lost by the fuel and the heat gained by the cladding in a given increment of time are still equal, but that amount varies with time. Therefore, the surface heat fluxes must be matched. The four boundary conditions now become:

\[ \frac{\partial T_f}{\partial r} \bigg|_{r=r_{fi}} = 0 \] ... (29)

\[-k_f \frac{\partial T_f}{\partial r} \bigg|_{r=r_{fs}} = -k_c \frac{\partial T_c}{\partial r} \bigg|_{r=r_{ci}} \] ... (30)

\[-k_f \frac{\partial T_f}{\partial r} \bigg|_{r=r_{fs}} = h \left[ T_{fs} - T_{ci} \right] \] ... (31)

\[-k_c \frac{\partial T_c}{\partial r} \bigg|_{r=r_{cs}} = h_{film} \left[ T_{cs} - T_{cool} \right] \] ... (32)

Where \( h_{film} \) is now highly variable, being a function of temperature and coolant pressure, both of which may be changing rapidly during the LOCA.

Just as for steady-state conditions, both the equations and the boundary conditions are non-linear, but now there is no possibility of iterating to convergence as was done for the steady state. The usual approach is to approximate the time derivative by finite difference, evaluate the thermal parameters and conductances from the temperature distribution during the previous time step, and then solve for the current temperature distribution. No matter what technique is used rough error and numerical instability must be guarded against, and this is usually accomplished by taking time steps that are sufficiently small.

A new solution method has, however, recently been developed. This involves integral transformation of fuel conductivity and heat capacity to linearize the fuel equation; Laplace and Hankel transformations of the time and space variables respectively; and correlation of the transformed fuel conductivity and heat capacity by means of describing functions. The method has computing speed advantage for long and well-behaved transients because the calculation extends over the full time range simultaneously.

In steady-state calculations, the interaction of the fuel rod with the coolant is straightforward. The coolant temperature varies only slightly with rod power and axial position whilst the film coefficient is also very stable, being a function of the relatively stable coolant conditions. The variations of both coolant temperature and film coefficient can be calculated or even simply input.
All this is totally different during a LOCA, however. As the coolant flow decreases, the applicable regimes of heat transfer change rapidly until, finally, heat transfer degenerates into an 'adiabatic' condition, followed by rewetting and reflood from the emergency core cooling. In the first few seconds of the transient, the heat flux out of the rod is also changing rapidly as the stored energy is released. In such a complicated and rapidly changing situation, it is essential that the film coefficient be calculated on the basis of current coolant conditions and heat flux. Coolant conditions (pressure, flow rate and enthalpy) may be input to a code such as FRAP-T5(28) but, ideally, there should be feedback between representative single rod calculations and the overall coolant flow and conditions. Such is the purpose of linking, for example, FRAP-T to the thermal hydraulics code RELAP.(29) Likewise the can deformation code CANSWEL(30) and a thermal hydraulic module are allowed to interact in the UK transient code MABEL-2.(31) In this code the effects on heat transfer of coolant channel blockage arising from cladding dispersions and vice-versa are calculated by an iterative procedure.

6. DATA OF RELEVANCE TO FUEL ROD THERMAL BEHAVIOUR AND GAP CONDUCTANCE CALCULATIONS

In the Sections which follow, the materials properties data which are required in the calculation of gap conductance and stored energy are reviewed. Of necessity such a review must be brief, in many instances directing the reader to recent, more extensive, reviews where these exist.

No serious attempt is made to recommend a particular data set for any parameter, but merely to point the reader to the available information and, particularly, to point out gaps and inadequacies where these exist.

6.1 THERMAL CONDUCTIVITY

6.1.1 UO₂

In this Section the thermal conductivity of UO₂ as a material is discussed, without considering the effects of thermal stress cracking on its effective thermal conductivity in a fuel rod.

The most recent review of UO₂ thermal conductivity appears to be that due to Brandt et al(32) which appeared in 1976, though the original German language version was published in 1973. It is, therefore, more or less contemporaneous with Washington's review(33) which, although written in an LMFBR context, deals with UO₂ as well as (U,Pu)O₂ fuel. A slightly earlier review is that by Marchandise.(34)

At low temperatures the thermal resistance of UO₂ results from temperature-dependent phonon-phonon interactions, together with temperature-independent phonon-impurity or structural defect interactions, yielding a thermal conductivity which decreases with increasing temperature. However the conductivity increases with temperature at temperatures above about 1750K, and many workers have fitted their data to an expression of the form:

\[ k_T = (A + BT)^{-1} + CT^3 \]  \hspace{1cm} \cdots (33)

with or without postulating a physical significance for the CT^3 term. Where a physical process has been suggested it has generally been thermal radiation.
Brandt discounted this as a major contributor and proposed two further terms, related to electronic contributions, in addition to the phonon-related term. The form of the Brandt curve is unusual in showing an inflexion at about 2650K. Hobson et al(35) have shown that their data could be fitted by a single electronic conduction term, in addition to the phonon term. More recently Killeen(36) has demonstrated from measurements of the Seebeck coefficient that plausible values of the electron-to-hole mobility ratio will give values of thermal conductivity in accord with high-temperature measurements.

The available data show considerable scatter. Amongst the likely reasons for this are: the porosity of the material, densification, stoichiometry effects and the effects of irradiation. Figure 5 shows some of the available data(37-46) and is taken from Ref. 33. The data have been corrected to 95% theoretical density by the expression

$$k'_f = k_f (1 - \beta V) \quad \ldots (34)$$

where $\beta$ is equal to 2.5 and $V$ is the fractional voidage. The porosity correction factor is probably one of the greatest sources of uncertainty. As manufactured, UO$_2$ contains porosity which may have been deliberately introduced or may be residual sintering porosity. During irradiation some of this porosity may disappear and fission gas bubbles may form. Most modelling codes use a single coefficient to translate the effect of these various types of pore into a change of thermal conductivity, despite the fact that pore shape and size are important parameters.(47) Many of the empirical formulae, such as equation (34), differ only in their choice of the coefficient $\beta$. If this is not $\leq 1$ the conductivity becomes zero before the pore volume fraction reaches unity. This difficulty can be circumvented by using the simplified Maxwell-Bucken formula:

$$k'_f = k_f (1 - V)/(1 + \beta V) \quad \ldots (35)$$

or an expression such as:

$$k'_f = k_f (1 - V)^\beta \quad \ldots (36)$$

The thermal conductivity of UO$_2$ decreases as the O/U ratio increases. The most comprehensive reported work is probably that of Goldsmith and Douglas,(43) who studied the effect on the conductivity of O/U ratios from 2.00 to 2.11 and densities from 90.4 to 98.6% theoretical over the range 673-1273K. They found that, for each O/U ratio, the conductivity followed the $k_f = (A + BT)^{-1}$ relationship, provided all oxygen ions were in solution. Their results are summarised in Table 2, which also contains some measurements by Howard and Gulvin.(48)

Finally the effects of irradiation must be considered. Because of the large number of displaced atoms produced by each fission fragment, displacement damage effects are more important than those due to solid fission products at low burn-up. These effects will probably be more significant at low temperatures and saturate at comparatively low burn-ups, and Clough and Sayers(44) have shown that displacement damage has saturated at $\sim 0.1\%$ burn-up and is insignificant at temperatures above about 773K. In consequence there is a low-temperature region where the conductivity is ill-defined.
Turning to the effects of solid fission products it appears that the only study performed at burn-ups greater than 1% is one by Daniel and Cohen.\(^{(49)}\) From an analysis of their data, Marchandise\(^{(34)}\) has concluded that, at 4 at.\% burn-up, the conductivity of UO\(_2\) will have decreased by \(\sim 27\%\), 16\% and 10\% at 773, 1273 and 1773K respectively. However these decreases include the effect of fission gas bubbles and so over-estimate the effect of solid fission products alone. Attempts have also been made to evaluate the effect of fission products from first principles. For example, Kleykamp\(^{(50)}\) showed that solid solutions of various fission product oxides in UO\(_2\) will reduce the conductivity, whereas metallic and oxide phases will increase it. The experimental data are sparse and indeed virtually non-existent about 1773K for appreciable burn-ups. Most LWR modelling codes neglect the effect of solid fission products.

In thermal systems, changes in the O/U ratio and plutonium formation will have only a small effect. Both are likely to depress the conductivity slightly but are generally neglected in modelling codes.

Whilst this Section does not aim to recommend values of thermal conductivity, the IAEA values\(^{(51)}\) do not appear to have been superseded - at least up to about 1500K. The mechanism of high-temperature conduction is not yet identified unequivocally, and it seems to be common practice to add to the phonon term a second term such that the integrated conductivity from 773K to the melting point is correct. The review by Brandt et al\(^{(32)}\) leads to a value of 7.0 kW.m\(^{-1}\) for fully dense UO\(_2\), whilst Washington\(^{(33)}\) proposed 6.8 kW.m\(^{-1}\) for 95\% dense UO\(_2\), which was assumed to have densified to 99\% dense above 1973K.

Under transient conditions the rate of transport of heat, i.e. the thermal diffusivity, is more important than the thermal conductivity. The two are related by the expression:

\[
k_f = \alpha_f C_f \rho_f \quad \ldots (37)
\]

and a knowledge of specific heat and density is needed to convert one to the other. The specific heat of UO\(_2\) is discussed in Section 6.2.1.

### 6.1.2 Zircaloy

The thermal conductivity of Zircaloy has been measured over the whole of the likely range of interest. Reference 52 provides a number of references to individual measurements and also presents a brief analysis of their results. The differences between the conductivities of Zircaloy-2 and Zircaloy-4 are so small as to be within the statistical scatter of the data. The same conclusion appears to apply to textural effects in the \(\alpha\)-phase region.

The composite equation developed in Ref. 52 is:

\[
k_c = 7.51 + 2.09 \times 10^{-2} T - 1.45 \times 10^{-5} T^2 + 7.67 \times 10^{-9} T^3 \text{ W.m}^{-1}\text{K}^{-1}
\]  \(\ldots (38)\)

The standard deviation of the data points with respect to this equation is 1.01 W.m\(^{-1}\)K\(^{-1}\). The data extend from 300 to 1800K.

The work surveyed in Ref. 52 was all carried out on bulk material, however, and the structure of the tubing has an effect on the measured values.
Equation (38) is probably adequate for normal reactor operating conditions but in the case of a LOCA, however, both high temperature and the effect of oxygen on thermal conductivity must be taken into account. A recent Battelle study (53) carried out under the auspices of ERI provides additional data in this area. Measurements of the thermal diffusivity of Zircaloy-4 containing up to 28 at.% oxygen were made over the range 573–1673K with an estimated accuracy of ± 5%. The results were not converted to thermal conductivity because of the unknown effect of oxygen on the specific heat, though the authors of Ref. 53 believe that any such effect would be small. The general pattern of the results is that the diffusivity increases with temperature over the range of oxygen contents explored. At any temperature there is a more-or-less rapid fall in diffusivity over the range 0–14 at.% oxygen, with a much slower rate of fall thereafter.

In use a Zircaloy-clad fuel rod will be covered with an oxide film, the thickness of which may vary during life. The conductivity of massive zirconia, even when fully dense, is < 20% of that of Zircaloy. Zirconia formed on a fuel rod both by water corrosion and by steam is black, defect-type hypostoichiometric in oxygen. It is also essentially fully dense, nonporous and probably of higher thermal conductivity than stoichiometric, fully dense white ceramic zirconia. Most importantly, the bonding of this oxide to the underlying metal is so good that the interface provides little if any barrier to the transmission of heat.

Gilchrist (54) has measured the thermal conductivities of both black and nodular oxide on Zircaloy-2 up to about 1450K. Approximate expressions for the conductivities are:

\[ k = 0.39 + 3.3 \times 10^{-4} T \text{ W.m}^{-1}\text{K}^{-1} \]  
(black oxide) \hspace{1cm} \cdots (39)

and

\[ k = 0.60 + 8.4 \times 10^{-4} T \text{ W.m}^{-1}\text{K}^{-1} \]  
(nodular oxide) \hspace{1cm} \cdots (40)

6.2 SPECIFIC HEAT

6.2.1 UO₂

The specific heat plays two roles in the evaluation of fuel rod response to transient conditions. The first is in determining the stored energy of the fuel rod. Though under steady-state conditions fuel rod temperatures are independent of specific heat, the stored energy is directly proportional to specific heat (equation 26). The second role of specific heat is during the transient itself. As can be seen from the equations presented in Section 5, the specific heat is a controlling factor in determining the rate of heat removal from the fuel rod. Thus, the accuracy with which specific heat is known has a direct bearing on the accuracy of the heat transfer calculations.

In a recent critical review of the thermodynamic properties of the urania phase by Rand et al. (55) a semi-theoretical equation previously employed by Kerrisk and Clifton (56) was selected to describe the specific heat as a function of temperature:

\[ c_p = \ldots \]
\[ C_T = \frac{a \theta^2 \exp(\theta/T)}{T^2[\exp(\theta/T) - 1]^2} + 2bT + \frac{c E_D}{RT^2} \exp(-E_D/RT) \]  

... (41)

in which \( a, b, c, \theta \) and \( E_D \) are adjustable parameters. The three terms of equation (41) represent, respectively: the heat capacity at constant volume; the expansion contribution, taken to be proportional to \( T \); and, finally, a contribution due to defect formation. The authors of Ref. 55 performed a least-squares fitting of two sets of specific heat data\(^{(57,58)}\) covering the range 298-1006K, and five sets of enthalpy data\(^{(59-63)}\) extending from 697.6 to 3112K, to equation (41) and its integrated form in order to define the five constants. In addition, a constraint of \( C_T \) (298.15K) = 15.20 cal.mol\(^{-1}\)K\(^{-1}\), as determined by Huntzicker and Westrum\(^{(57)}\) was also imposed. For temperatures up to 2670K, equation (41) fitted the data well with the constants shown below, but above this temperature the results were better fitted by a constant specific heat of 39.923 cal. mol\(^{-1}\)K\(^{-1}\).

<table>
<thead>
<tr>
<th>Constant</th>
<th>( a )</th>
<th>( b )</th>
<th>( c )</th>
<th>( \theta )</th>
<th>( E_D )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Value</td>
<td>18.448</td>
<td>1.0642 \times 10^{-3}</td>
<td>1.3958 \times 10^{7}</td>
<td>508.0</td>
<td>4.3487 \times 10^{4}</td>
</tr>
<tr>
<td>Units</td>
<td>cal.mol(^{-1})K(^{-1})</td>
<td>cal.mol(^{-1})K(^{-2})</td>
<td>cal.mol(^{-1})</td>
<td>K</td>
<td>cal.mol(^{-1})</td>
</tr>
</tbody>
</table>

Kerrisk and Clifton\(^{(56)}\) estimated the uncertainty of their expression to be \( \pm 3\% \) (2\sigma). Whilst the authors of Ref. 55 do not quote an uncertainty for their analysis, it is unlikely to be any greater than this over the range 298-2670K.

6.2.2 Zircaloy

The specific heat of Zircaloy is considered here without the complications introduced when hydride is being dissolved therein. Brooks and Stansbury\(^{(64)}\) have measured the specific heat of \( \alpha \)-phase Zircaloy-2 up to about 1000K whilst, more recently, Gilchrist\(^{(54)}\) has covered approximately the same range. The results are in close agreement and show an almost linear rise from 0.29 J.g\(^{-1}\)K\(^{-1}\) at 323K to 0.35 J.g\(^{-1}\)K\(^{-1}\) at 1000K. It seems reasonable to extrapolate these data to the limit of the \( \alpha \)-phase at 1090K. For temperatures above this and up to about 1300K, data obtained by Eldridge and Deem\(^{(65)}\) are available, but are appreciably less precise than those reported in Ref. 54 and 64. The reasons for this lack of precision lie with the \( \alpha-\beta \) phase transformation, its hysteresis, the non-equilibrium solute distributions during the transformation, and the long temperature range over which the transformation occurs in Zircaloy.

The authors of Ref. 55 used a specific heat equation from a compilation by Hammer\(^{(66)}\) for temperatures up to 1223K:

\[ C_c = 7.1 \times 10^{-2} + 1.7 \times 10^{-5}T - 0.89 \times 10^3T^{-2} \text{ cal.g}^{-1}\text{K}^{-1} \]  

... (42)

which leads to somewhat higher values of specific heat at low temperatures.
6.3 EMISSIVITY

6.3.1 UO₂

Cladson(67) measured the spectral emissivity of sintered UO₂ over the range 1023–2223K and found it to fall from 0.85 to 0.37 over that range, whilst Ehler and Margrave(68) reported values of 0.40 ± 0.02 over the temperature range 2073–2373K. This fall in emissivity with increasing temperature was not observed in more recent work by Cabannes et al. (69) who measured a value of 0.86 at room temperature rising to ~ 0.90 at 1500K for polished UO₂ at the infra-red end of the spectrum, with slightly lower values (0.85 at 1000K, 0.89 at 1800K) in the visible region. More recently Bober and Karow(70) have extended the range of measurement up to and beyond the melting point of UO₂. So far as the infra-red spectrum is concerned, their results are in excellent agreement with those of Cabannes at 1500–1600K where the two sets of measurements overlap, and show the emissivity to fall slightly with increasing temperature to about 0.86 at the melting point of UO₂. The emissivity in the visible region was found to be effectively constant at 0.87 over the range 1000K to the melting point, in good agreement with Cabannes et al. Up to 2000K Bober and Karow's results are also in good agreement with those obtained by Held and Wilder(71) but these latter workers found that the emissivity decreased at higher temperatures.

6.3.2 Zircaloy

The emissivity of Zircaloy depends critically on whether or not an oxide film is present. Where the oxide thickness represents only a few wavelengths of near infra-red radiation the film is effectively transparent. Thicker layers are opaque; for these opaque layers the thickness is not so important as the nature of the outer oxide surface. For a given oxide and thickness and for temperatures below about 1500K, the emissivity can, to a good approximation, be considered as being independent of temperature, though for thin films the data of Murphy and Havelock(72) do show a small increase with increasing temperature. However the major dependence is on oxide thickness and can be represented(52) by:

\[ \varepsilon_c = 0.325 + 1.246 \times 10^5 \cdot d_{\text{ox}} \quad (d_{\text{ox}} \leq 3.88 \times 10^{-6} \text{m}) \]  \hspace{1cm} (43)

where \( d_{\text{ox}} \) is the oxide film thickness in metres. At greater thicknesses the results of Juenke and Sjodahl(73) and of Burgoyne and Garlick(74) who investigated the effects not only of uniform oxide but also of nodular oxide and crud, can be summarised by equation (44) which is also taken from Ref. 52:

\[ \varepsilon_c = 0.308642 - 50.0 \cdot d_{\text{ox}} \quad (d_{\text{ox}} \geq 3.88 \times 10^{-6} \text{m}) \]  \hspace{1cm} (44)

Recent Battelle work(53) has given rather higher values than would be calculated from equation (44). For oxide thicknesses greater than 100 μm on specimens which were heated in unlimited steam, the emissivity was close to 0.9 at all temperatures from 473 to 1473K, and decreased only slowly if the specimens were maintained at the latter temperature for extended periods. For pure oxide films the standard errors on the emissivities have been calculated to be 0.04 and 0.05 for equations (43) and (44) respectively. If, however, the results of Burgoyne and Garlick on crud and nodular oxide are taken into account, as is perhaps proper in the consideration of transients, the
standard error in equation (44) is increased to 0.07. (52)

6.4 THERMAL CONDUCTIVITY OF GASES

Whilst there are literature reports of measurements of the thermal conductivity of gas mixtures of relevance to fuel rod performance, for example Ref. 10 and 11, it is more usual to use the conductivities of the pure gases as primary data in gap conductance calculations, combining them as appropriate to obtain $k_{\text{mix}}$ for the mixture present in the rod at any instant. Equations such as (18) are included in many fuel rod modelling programs for this purpose.

One of the most widely used correlations of rare gas conductivities is that by Saxena and Gandhi. (11) The data on which their analysis is based are referenced in MATRO. (52) which also gives the appropriate values of the constants $A$ and $S$ in equation (17). A recent review of rare gas conductivities published by the National Engineering Laboratory (75) contains data which give somewhat different values of the two constants when fitted to equation (17). The relevant values are listed in Table 3, which gives the conductivities in W m$^{-1}$K$^{-1}$ for temperatures in kelvins. Hagman (8) has also produced a compilation of the constants for a number of gases and has estimated the uncertainties of the derived conductivities. His values of $A$ and $S$ for helium, krypton and xenon are also included in Table 3.

It is generally considered that gas thermal conductivities are independent of pressure in the range of interest in fuel rod performance analysis. This is not completely true as can be seen by reference to, for example, the work of Ho and Leidenfrost. (76) who measured the conductivity of helium up to pressures of 10 MPa at temperatures between 290 and 398K and found increases of up to about 4% over this range. The effect is small compared with the other uncertainties in conductance calculations and its neglect appears to be justified except, as has been mentioned earlier, in the case of steam.

7. FLUX DEPRESSION AND PLUTONIUM EFFECTS ON TEMPERATURE DISTRIBUTION

In equation (3) the volumetric heat generation rate is shown as a function of pellet radius. Most modelling codes make some allowance for this variation in rating which arises from the self-shielding of the inner regions of the pellet, with the result that the heat generation rate diminishes from the outside to the centre. The necessary equations have been known and used for many years (77) and require, as input, the geometry of the fuel and the inverse diffusion length of the neutrons within it. This latter parameter is a function of the flux spectrum of the reactor and the geometry of the pellet.

Most codes calculate the volumetric heat generation distribution for time zero and for the initial distribution of fissile atoms. The distribution is not constant however and, as irradiation proceeds, $^{239}$Pu is formed preferentially in the outer regions of the fuel. Fission of this plutonium markedly increases the rating in the pellet rim, as can be seen from Table 4 which summarises the results obtained recently by Halsall in calculations on a simulated LWR fuel rod. (78) The ratio of surface fission rate to centre fission rate, which at start of life is 1.06, increases to 2.29 at $\sim 47500$ Mwd/tU. For a constant linear rating this will produce a very significant reduction in fuel centre-line temperature.
8. OUT-OF-REACTOR GAP CONDUCTANCE DATA

In Section 6 the properties of UO₂, Zircaloy and the filling gas which influence the temperature distribution and the stored energy in an operating fuel rod were considered. The nuclear properties of UO₂ were briefly discussed in Section 7, insofar as they affect the temperature distribution within a fuel pellet. In the final Sections of this Report we turn to the actual measurements of gap conductance both in and out-of-reactor and then, finally, to what is perhaps the greatest source of uncertainty at the present time, the definition of 'gap width' in an operating fuel rod.

Until recently, the primary sources of gap conductance data were from controlled, out-of-reactor experiments by Ross and Stoute(17) and Dean.(79) Both groups investigated contact conductance, the former in vacuum and in various pure gases, the latter in argon only. These experiments have provided measurements of the combined temperature jump distance, \((g_f + g_c)\), and of the pressure dependence of contact conductance. In addition, Rapier et al.(80) have measured the effect of different gases, surface roughnesses, and interfacial pressures on the conductance between \(\text{UO}_2\) and stainless steel surfaces.

Ross and Stoute obtained heat transfer coefficients in vacuum from measurements on eight \(\text{UO}_2\)/Zircaloy pairs with different surface roughnesses. On six of these pairs measurements were also made in gas atmospheres. The contact pressures ranged from 5 to 55 MPa and mean interface temperatures from 410 to 640K. From measurements made in vacuum the authors concluded that, to within a factor of two, the solid conductance term could be described by equation (23) with \(A' = 0.5 \text{ cm}^{-2}\) and \(n = \text{unity}\). The gas conductance term, \(h_g\), was obtained by subtracting the values of \(h_g\) obtained in vacuum from the total measured conductance when a gas was present in the interface. The pressure of the gas was generally 0.1013 MPa, but for four of the specimen pairs the effect of reducing the pressure was examined with helium and argon. Values of the constant \(A\) in equation (16) and the combined temperature jump distance were obtained by plotting \(k_{\text{gas}}/h_g\) against \((R_f + R_c)\). \(A\) was found to be close to 2.5 for interfacial pressures less than about 10 MPa, but diminished to about 1.5 at 55 MPa. The authors concluded that the values of \(A\) which they obtained, together with the observed decreases with increasing interfacial pressure, were evidence of waviness and errors of form in the test surfaces.

The combined temperature jump distances obtained are shown below. It was not possible to determine a temperature dependence. So far as pressure dependence is concerned, the authors state that their results are consistent with the theoretical gas pressure dependence of the jump distance. Pressure cycling and/or changing the position of contact between the two surfaces did not produce any significant change in the heat transfer coefficient.

<table>
<thead>
<tr>
<th>Gas</th>
<th>He</th>
<th>Ar</th>
<th>Kr</th>
<th>Xe</th>
<th>(\mu m)</th>
<th>K</th>
</tr>
</thead>
<tbody>
<tr>
<td>((g_f + g_c))</td>
<td>10</td>
<td>5</td>
<td>1</td>
<td>&lt; 1</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Temperature range</td>
<td>423–523</td>
<td>453–593</td>
<td>453–603</td>
<td>453–603</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

More recently the range of experimental conditions has been extended and now includes open gaps as a result of the work of Garnier and Bejel.(81,82)
Their range of parameters is shown below:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gap temperature</td>
<td>273 to 873K</td>
</tr>
<tr>
<td>Gap width</td>
<td>26 μm to contact</td>
</tr>
<tr>
<td>Gas composition</td>
<td>Pure He, Ar, Xe; mixed gases</td>
</tr>
<tr>
<td>Interfacial pressure</td>
<td>0 to 20 MPa</td>
</tr>
<tr>
<td>Gas pressure</td>
<td>0.1 to 13.6 MPa</td>
</tr>
</tbody>
</table>

Most of the data were gathered from an apparatus in which a laser pulse impinged on the back of a UO₂ disc, which itself faced a Zircaloy disc. The temperature on the back of the Zircaloy disc was monitored by a thermocouple, and the interface conductance was deduced from the shape of the temperature-time curve. Confirmation of the results from the heat pulse equipment was obtained by carrying out cross-checks with more conventional linear heat flow equipment.

Very great care was taken to characterise the surface topography of the specimens, and Ref. 81 contains an excellent account of the difficulties inherent in defining gap width. The centre-line average surface roughnesses of the UO₂ specimens ranged from 0.25 ± 0.05 to 14.4 ± 2.8 μm. The roughnesses of the Zircaloy-4 specimens ranged from 0.17 ± 0.05 to 4.5 ± 0.4 μm. To remove any stresses induced by machining and polishing, samples were annealed for 1 h at 1273K in gettered helium prior to surface macrowaviness measurements. The Zircaloy specimens were given an extra anneal at 573K for several hours to ensure that only the alpha phase was present. The UO₂ and Zircaloy specimens were combined to form three specimen pairs as shown below:

**Centre-line average (CLA) roughnesses**

<table>
<thead>
<tr>
<th>Designator</th>
<th>Roughness, μm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>UO₂</td>
</tr>
<tr>
<td>ISM I</td>
<td>14.4 ± 2.8</td>
</tr>
<tr>
<td>ISM II</td>
<td>1.6 ± 0.7</td>
</tr>
<tr>
<td>ISM III</td>
<td>0.25 ± 0.05</td>
</tr>
</tbody>
</table>

Garnier and Begej characterise the separation of the two surfaces in terms of a mean plane separation, Dₘₚ, which approximates to d, the gap width. The dependence of gap conductance on gap width for three surface roughness pairs is illustrated in Fig. 6 for helium at 873K. It is clear that the importance of surface roughness becomes progressively greater the smaller the value of Dₘₚ. The effect of gas composition between the limits of pure helium and pure argon, and including one helium/xenon mixture (each at a total pressure of 0.103 MPa), was studied.

The authors also report the effect of temperature on h₋ over the range 293 to 873K for a number of gases, specimen separations and surface morphologies. For large gaps the conductance would be expected to increase with increasing
temperature and this is indeed observed, but for small helium-filled gaps where 
\((s_\tau + s_p) > d\) a consideration of equations (11) and (13) together leads to the 
prediction that \(h_g \propto T_g^{-\frac{1}{2}}\). In the analysis of their results, Garnier and 
Begej used temperature jump distances calculated from accommodation coefficients 
determined by Ullman et al. (7) Figure 7 shows measured gap conductances for the 
smooth surface pair, ISM III, in helium, as a function of temperature. The 
estimated gap separation is only 2.7 \(\mu m\), so the helium jump distance, which 
was calculated to be between 6.56 and 11.82 \(\mu m\) at 673K, dominates the calculated 
conductance. For this specific, but not totally atypical, condition the 
predicted trend of conductance is not matched by the data. For gases of 
lower conductivity the dependence of gap conductance on temperature is in 
accord with prediction. The discrepancy found for helium is not a serious 
safety concern, but suggests that the classical conductance equations are 
not adequate for describing heat transfer across a narrow gap such as this 
even though the gap width is many times the mean free path.

The effect of interfacial pressure on the contact conductance was also 
examined using the ISM I and ISM II surface pairs, and the results were compared 
with those obtained by Ross and Stoule, (17) Dean (79) and Rapier et al. (80). Most 
of these measurements were carried out under vacuum, but argon at 0.172 MPa was 
used in some tests to provide a comparison with Dean’s work - with which good 
agreement was found. At high contact pressures (above about 10 MPa) there was 
also agreement with Ross and Stoule. The only significant difference was with 
the results of Rapier et al, with Garnier and Begej measuring contact conductances 
between 20 and 50% of Rapier et al's values. This difference has been 
attributed to differences in mating error of form, which determines the number 
of initial contact spots and hence the conductance. Assuming the contact 
conductance to be proportional to some power of the interfacial pressure, the 
results show a transition in the solid conductance from a \(P_a^{\frac{2}{3}}\) to a \(P_a^{\frac{1}{3}}\) dependence 
at about 4 MPa interfacial pressure, which is about half the value proposed by 
Jacobs and Todreas. (83)

In the second part of their work, (82) Garnier and Begej studied the effect 
of gas pressure on gap conductance for one of their specimen pairs, ISM II. 
Figure 8 shows gap conductance data at 473K as a function of pressure for a 
number of compositions. The expected increase to a constant maximum is 
borne out generally, but the relative magnitude of the increase is about the 
same for all the gases. The increase in conductance was found to be under-
estimated by the standard expression with the temperature jump distance 
calculated by Kennard’s expression, equation (13). This result holds for all 
the gas compositions, and again emphasises the inadequacy of the standard 
expression for jump distance and conduction at small gaps.

Figure 8 also shows the expected decrease in gap conductance with 
decreasing helium content at any given gas pressure, a dependence on gas 
pressure over the range 0.1-1 MPa and, for two of the gas compositions, a 
decreasing value of \(h_g\) between 1 and 7 MPa gas pressure. Increasing the gap 
separation, \(D_{mp}\), to 21.3 \(\mu m\) eliminated the ‘hump’ in the helium curve, though 
the general features of an increase in conductance at gas pressures below 
\(\sim 1\) MPa with a slow fall at higher pressures were retained.

9. IN-REACTOR GAP CONDUCTANCE DATA

Until relatively recently, predictions of gap conductance models based 
on out-of-reactor measurements could only be compared to in-reactor conditions
by extrapolation from a temperature measured remote from the fuel surface, \(^{(84-86)}\) or from temperature markers such as fuel structural changes. \(^{(87)}\) This procedure is subject to large uncertainties inherent in extrapolation when:

(a) the thermocouples are located at the fuel centre - because of uncertainty in the effective fuel thermal conductivity;

(b) the thermocouples are located near the fuel surface - because of uncertainties as to their position in the very steep temperature gradient, or

(c) a marker is used - because of uncertainty in the temperature of the marker (e.g. grain growth).

Campbell et al.\(^{(88)}\) have measured fuel-to-cladding heat transfer in an instrumented fuel rod irradiated in the X-2 loop of NNX. The cladding was Type 304L stainless steel and the rod was fitted with three chromel/alumel thermocouples embedded in the fuel close to the pellet periphery, with three active and three dummy strain gauges attached to the sheath, and also with a gas line by means of which the internal gas pressure and composition could be varied.

Figure 9 shows a typical set of results. The rod was first evacuated until the temperatures of the fuel thermocouples stabilised, and then the internal pressure was increased in steps to approximately 14 MPa, using helium. Initially (Point A), essentially all heat flow is through points of solid/solid contact and the fuel and sheath are in firm contact. As gas is added the overall heat transfer coefficient rises and the fuel temperature decreases, causing the fuel to shrink and hence reduce the elastic strain imposed on the sheath until, eventually, the temperature is lowered to the point where the interfacial pressure becomes zero and the solid component of heat transfer disappears (Point B). Further increases in gas pressure continue to reduce the fuel temperature, because the temperature jump term in the gap conductance expression is still dominant. However, as the gas pressure is further increased, the gap (increasing as a result of both sheath strain and fuel shrinkage) gradually takes over as the dominant term. At the point where the decreases in the jump term are just compensated for by increases in the gap term, a minimum in the temperature curve is observed.

In Fig. 10 the results of an argon pressure cycle are compared with those of a helium cycle at the same power. The fuel is cooler throughout the helium run, although at low pressures both runs approach nearly the same temperature. The temperature minimum in argon is less pronounced and occurs at a lower pressure, as a result of the smaller temperature jump distance of argon compared with helium. Because fuel temperatures are higher in argon, fuel/sheath contact is maintained up to higher pressures.

Interpretation of the results is centred on the ability to identify a distinct point of separation of fuel and sheath (Point B in Fig. 9). This provides a datum to which the relative movement of fuel and sheath can be referred, enabling absolute values of the interfacial pressure or gap width to be calculated at any point in the experiment.

The temperature jump distance was derived by finding the value of \(g\) in equation (21) which fitted most closely the observed pressure dependence of the fuel temperature. Seven pressure cycles were analysed in this way to
give a mean temperature jump distance for helium at 273K and 0.1013 MPa of 5.2 μm, which may be compared with 5 μm calculated from Ross and Stoute's data when these are corrected to the same conditions of temperature and pressure. For argon a value of 0.57 μm was calculated.

In analysing the results which had been obtained under conditions of fuel/cladding contact, large differences were found between these results and predictions based on the Ross and Stoute model. The predictions were also extremely sensitive to the values of surface roughness which were assumed. Ross and Stoute found that the solid component of heat transfer could be described by a linear dependence on interfacial pressure. However other workers have determined that, at lower interfacial pressures, there is a square root dependence on interfacial pressure. (80) Jacobs and Todreas (83) examined data on metal/ceramic pairs and noted a transition from square root to linear dependence at ~10 MPa. To explain this, Mikic and co-workers (89) have postulated that at low pressures the contact spots deform elastically and the number of contact spots remains essentially constant; at high pressures, plastic flow occurs allowing more contact spots to be formed. Since the maximum interfacial pressures reached in this experiment were calculated to be about 10 MPa, the data were analysed on this basis and an expression for \( h_s \) for the UO2/stainless steel interface was found as follows:

\[
h_s = \frac{k_{\text{gas}}}{\frac{P_a}{8.6 \times 10^{-3} R^T H}} \text{kw.m}^{-2} \text{K}^{-1}
\]  

\[\text{... (45)}\]

For the constant \( A \) in equation (16) the authors used a value of 1.5, identical to that used by Ross and Stoute at moderate and high pressures.

More recently Ainscough and Hobbs have published a preliminary report on the first of two in-reactor experiments to investigate the thermal conductance of the UO2/Zircaloy interface. (90) Their rig consisted of three identical capsules arranged in line. All three capsules were fitted with gas lines to allow pressurisation or a change of composition during irradiation. The fuel was 25 mm o.d. and 5 mm i.d., surrounded by a thick Zircaloy cladding which was itself surrounded by an electrical resistance heater separated from the outer containment by a helium-filled gas gap. This geometry allowed eight chromel/alumel thermocouples to be inserted into the middle pellet in each capsule, together with a further six in the inner cladding/heater assembly and two in the outer cladding. This large number of fuel thermocouples was installed so that the fuel temperature distribution could be measured and used to calculate the fuel surface temperature without making assumptions regarding the thermal conductivity of the UO2 itself. The as-assembled clearances were small so that fuel/cladding contact would be achieved at power. The contact pressure could not be measured but, at the ratings attained at start of life, 30-32 kw.m\(^{-1}\), was estimated to be close to 30 MPa; it became very low later when the rating was reduced.

The experiment was used to determine temperature jump distances by varying the gas pressure whilst operating at constant power, and analysing the results in terms of equation (21). The pressure range covered was 0.007-0.584 MPa for He and Kr and 0.101-0.466 MPa for Xe. The weighted mean value of \( g \) for helium at 700K and 0.1013 MPa was 9.78 μm, equivalent to 3.15 μm at 273K. The corresponding, much smaller, values for Kr are 0.46 and 0.14 μm and for Xe 1.00 and 0.32 μm. The helium value is somewhat smaller than that obtained by Ross and Stoute (17) or Campbell et al. (88)
In the early stages of the experiment the interface conductance fell as
the irradiation proceeded, contrary to what would be expected on the basis of
a contact pressure which was relaxing as a consequence of in-reactor
densification. This suggests that the fuel and cladding remained in contact,
with the effective surface roughnesses diminishing with burn-up. The end
result was that the ratio of the interface conductances between krypton- or
xenon-filled capsules on the one hand, and a helium-filled capsule on the
other, was only 20–25% of that expected on the basis on the surface roughnesses
measured prior to irradiation. The gas in the helium-filled capsule was
diluted with xenon during operation and vice-versa. When plotted as functions
of gas composition the conductances showed no significant discontinuities,
indicating that the interface morphologies were comparable.

There are now a number of fairly reliable in-reactor experimental values
for the combined temperature jump distance for helium, and these are listed
in Table 5 as values corrected to s.t.p. The value quoted by Miller and
Appelhans(91) is a preliminary estimate from an analysis of the TPA-430 irradia-
tion in HBWR. There are fewer reliable values for the heavier gases, especially
krypton and xenon. However, it is clear from the available information that
the combined jump distances are less than 1 μm and probably about 5–10% of
the helium values.

To conclude this Section it is worth mentioning briefly the power
oscillation or thermal oscillator method for estimating gap conductance.
This relates the fuel-to-cladding gap conductance to the phase relationship
between a sinusoidal fuel rod power oscillation and the corresponding cladding
surface temperature change. During a power oscillation, the two signals will
oscillate at the same frequency, but with a phase lag between the power and
the cladding surface temperature. The gap conductance can, theoretically, be
determined from the measured phase lag. The main attraction of the method is
that it does not require the fuel pellets to be instrumented and so can be
applied to small-diameter power reactor rods.

The power oscillation method has been compared with steady-state gap
conductance measurements as part of the EG & G gap conductance tests.(92)
The agreement between the two methods was relatively poor for the BWR-type
rods used in the Series-2 tests. On the other hand, comparison made with
FWR-type rods as part of the Power-Coolant Mismatch Programme showed good
agreement between the two methods.

It is even possible to dispense with the cladding thermocouples, and
Webster(93) has used the time delay between an initiating power perturbation
and the subsequent response of a thermocouple placed in the liquid metal
coolant to give a smeared gap conductance for a fast reactor fuel rod.

A major drawback to the power oscillation technique is that it requires
the thermal conductivity and the specific heat of the fuel and cladding as
input to the calculation of gap conductance. In addition, if cladding
thermocouples are not used, the cladding-coolant heat transfer coefficient
is also needed.

Another, rather similar, technique which has been used by the Halden
Project is the analysis of noise signals from instrumented fuel rods.(94)
Operating at constant load the Halden BWR exhibits power fluctuations
around the steady-state value. The power noise is assumed to be proportional
to the heat generation fluctuations within the fuel, which in turn cause
variations of the fuel temperature about the steady-state value. By analysis
of the fuel thermocouple signal, together with the neutron detector signal, the heat transfer function for the fuel rod can be determined experimentally. From this can be obtained physical parameters such as the time constants for heat transfer.

10. THE GAP WIDTH

In calculating the heat transfer across an open gap it is necessary to know the width of that gap. The work of, in particular, Garnier and Begej(81,82) has shown the importance and also the difficulty of defining the effective gap width which arises from the roughness of the fuel and cladding surfaces. It is necessary now to consider the gap width itself, i.e. the separation which would exist in-reactor between fuel and cladding if the surfaces were completely smooth. Many of the modelling codes cited earlier assume that a UO₂ pellet will crack in operation, but that these cracks will be purely radial in character and that the thermal expansion of the UO₂ can be calculated accordingly. Thus the gap is the original cold radial clearance miming the differential expansion of the fuel and the cladding after allowance has been made for densification and swelling of the fuel and for creep of the cladding. It is often assumed that the fuel and the cladding remain concentric so that the gap is azimuthally uniform and no outward movement of the fuel fragments (relocation) takes place.

From an analysis of the early stages of the IFA-431 irradiation in the Halden BWR, Williford and Hann(95) showed that pellet eccentricity in the cladding could raise the gap conductance appreciably in the case of high thermal gradients across the gap. This was demonstrated with an instrumented xenon-filled rod in which eccentricity could be maintained over part of the length. The calculated gap conductance was estimated to be 25-50% greater than in the concentric section. Ignoring azimuthal effects and using instead the concentric approximation produced a 7-24% under-estimate of the gap conductance and of the thermal time constant of the rod in a transient.

Eccentricity of the fuel within the cladding will produce these effects even if the fuel is not cracked. However, post-irradiation examination of irradiated fuel rods almost always shows the fuel to be cracked. A purely radial cracking pattern will allow outward movement, i.e. radial relocation of the fuel, to take place without impeding radial heat flow. Should this occur the net result will be lower fuel temperatures, lower stored energy and also a higher thermal diffusivity across the gap in a LOCA. In the real situation the fuel is not only cracked but, more importantly, the cracking is random so that many of the cracks have a circumferential component. This is the basis for a new interpretation of in-reactor data which is beginning to appear in fuel modelling codes.(22,28) In these codes fuel cracking is assumed to result if drastic, if not total, fuel relocation, accompanied by simultaneous degradation of the fuel thermal conductivity due to the resistance offered to radial heat flow by cracks with circumferential components. There are several more or less well-documented examples of relocation in the literature; only three will be cited here. Appelhans et al(96) measured the fuel centre temperature changes during the first four upward ramps of the IFA-430 assembly in the Halden BWR and concluded that relocation begins as fuel rating is increased during the first power ramp and continues up to a limiting power at which it is essentially complete. No significant change was seen in the next three ramps, which all occurred within a period of 11 days from first start-up. Cunningham et al(4) have interpreted the pellet bore temperature changes which occurred in the xenon-filled rod of IFA-431 as also
being due to radial relocation of the fuel. In this rod, though, relocation is postulated as continuing to a burn-up of \( \sim 3000 \text{ MWD/tU} \).

Ainscough et al(97) studied the temperature history of an instrumented \( \text{UO}_2 \) fuel rod to a burn-up of about 2600 MWD/tU, during which time it underwent five shut-down/start-up sequences in addition to the initial start-up ramp. Discontinuous negative temperature changes occurred at each and produced a total reduction in pellet bore temperature of more than 70K at a temperature of 1270–1320K. It was not possible to separate the temperature effects due to outward movement from those due to the thermal barriers formed by circumferential cracks.

The effect of cracking and relocation on the effective thermal conductivity of the fuel has been demonstrated by Garner et al(92) in the EG & G Series-2 gap conductance test. The effective thermal conductivity was deduced from measurements made on rods fitted with one central and three near-peripheral thermocouples, and was found to be significantly less than the values currently used for uncracked pellet fuel and to be a function of rod power, with the effective conductivity at a given temperature falling with reducing power. This would be expected if the cracks within the fuel were opening as a result of thermal contraction and possibly, too, of the removal of the fuel/cladding interfacial pressure. There were no apparent changes in effective thermal conductivity with time, suggesting that cracking and relocation were probably completed during the first power ramp. The results also suggest that pellet fragments move further in a radial direction the larger the initial pellet/cladding gap. As a consequence they do not fit back together so well on power reductions and the effective fuel conductivity is thus a function of the initial gap size. An empirical model of the effects of fuel cracking on the thermal conductivity of LWR \( \text{UO}_2 \) pellet fuel has been developed from this work.(98)

The two views, radially cracked pellet versus randomly cracked and fully relocated fuel, result in a different apportioning of thermal resistance between fuel and gap. As a result, feedback between thermal resistance and fuel temperature also differs. In the conventional model, the gap resistance falls with increasing fuel temperature because of gap closure, whereas the fuel resistance rises because of the decrease of intrinsic \( \text{UO}_2 \) conductivity with temperature. In a typical helium-filled LWR rod, these two effects more or less balance and total resistance as a function of power is almost constant. In the cracked fuel model, the gap resistance is much lower, especially at low temperatures, and nearly constant as a function of fuel temperature. Conversely, the effective fuel resistance is relatively higher, especially at low temperatures, and is also relatively constant because crack closure counterbalances the intrinsic conductivity.

Lanning et al(99) have used the response of fuel centre thermocouples to changes in rod power to determine whether circumferential fuel cracking has occurred. From an analysis of the behaviour of rods irradiated in the Halden IP/432 experiment, they conclude that significant thermal effects are probably produced as a result of cracking within the fuel.

So far as steady-state operation is concerned, the major consequence of interpreting fuel centre-line temperature data by means of a cracked and relocated pellet model is a change in the stored energy of the fuel. Because of relocation there are lower temperatures in the outer regions of the fuel, which comprise most of the fuel volume. Consequently, the stored energy is
lower for the cracked fuel model than for the conventional model. In a transient such as a LOCA, not only will the cracked fuel model give lower stored energy, but this stored energy will be lost relatively faster following reactor shut-down than in a conventional pellet model prediction. The reason, again, has to do with the gap conductance. The predicted gap conductance is lower in the conventional than in the cracked fuel model and, in addition, the conductance in the former decreases with decreasing fuel temperature after shut-down, significantly inhibiting the flow of heat to the cladding. The cracked fuel model conductance is more constant, offering less hindrance to transient heat flow.

A number of more-or-less empirical expressions have been proposed to take account of eccentricity and outward movement of pellet fragments on the effective gap for fuel/cladding heat transfer. Kjaerheim and Rolstad proposed the expression:

\[ h = F h_s + (1 - F) h_g \]  \hspace{1cm} (46)

where \( F \) is an empirical factor which represents the fraction of the pellet circumference in contact with the cladding. Other, more complex, models exist but often with basically the same formulation, i.e., a combination of a gas conductance and a solid/solid conductance term.\(^{101,102}\)

11. CONCLUSIONS

This Report has attempted to review briefly the state of the theory of fuel-to-cladding heat transfer insofar as it is relevant to steady-state operation and to transient conditions in LMRs, and also the state of knowledge of some of the more important parameters which are required as input to the heat transfer equations. Whilst the equations differ between the steady-state and the transient situations the majority of the input is common to both. The specific heats of the fuel and the cladding, particularly the former, and also the emissivities of the two materials, enter into the transient calculations whilst being at most of minor importance in the steady state. Clearly in the transient situation where conditions are changing rapidly, particularly with respect to heat transfer from the outer surface of the cladding and possibly also across the fuel-to-cladding gap, very short computing timesteps are needed. This is however only a matter of efficient programming and in principle the theory appears adequate for transient calculations.

So far as the data are concerned, an accurate measure of the rod power prior to a LOCA transient is important and errors here have been shown to have a very significant effect on the temperature distribution and hence on the stored energy in the fuel. The specific heat of UO\(_2\) at least up to about 2670K is reasonably well defined, as are all the properties of Zircaloy which have been considered and also the thermal conductivities of the pure noble gases; provided the gas composition is known, the bulk conductivity of any mixture can be calculated. The physical property subject to most uncertainty is the thermal conductivity of the UO\(_2\) itself. A major cause of this is the uncertainty in the correction which has to be made for the porosity, which will vary both in volume and morphology with time and with temperature. The literature shows a wide range of 'correction factors' and theory supports the idea that the effect of a given pore volume fraction on the thermal conductivity will be markedly dependent on the morphology of the pores. In making these remarks about the thermal conductivity, we have implicitly been considering
uncracked UO₂. The cracking of UO₂, particularly circumferentially leading to the formation of additional thermal barriers, coupled with the ability of the pellet fragments to relocate, makes the precise calculation of the temperature profile and hence the stored energy difficult. If a fuel/cladding gap is predicted to exist at power this gap may redistribute itself, but, in addition, extra effective gaps will be formed at each circumferential crack because of the roughnesses of the new surfaces, the asperities of which may not match, and also the temperature jump distances. If no gap exists, the cracks will still provide thermal barriers but may sinter during prolonged periods of high-power operation, allowing the temperature and the stored energy to fall with time. Despite a fairly large amount of experimental work, attempts to allow for cracking and relocation in real power reactor fuel rods, which do not have the benefit of fuel thermocouples, is still in a very empirical state.

The temperature jump distance for helium, as determined from in-reactor experiments, is fairly well defined; whilst jump distances for heavier gases, such as the noble fission gases krypton and xenon, are known less precisely, it is clear that they are small, probably <1 μm in an operating fuel rod. However there are indications from some of the in-reactor experiments described, and even more from the out-of-reactor work of Garnier and Begej, that the theory as currently used in performance modelling codes may be inadequate to deal with very small gaps.

12. ACKNOWLEDGEMENTS

This Report was originally intended to form one chapter of a State-of-the-Art Review of Fuel Behaviour in a LOCA. That Review will not now be produced and the present Report is being issued separately as a review of Gap Conductance in Zircaloy-clad LWR Fuel Rods.

The author wishes to thank Mr D D Lanning of Battelle, Pacific Northwest Laboratories who provided an extensive contribution to the original concept; large sections of this have been used in the present Report. He also thanks Mr M J F Notley of AECL, Dr J A Turnbull of CEGB Berkeley Nuclear Laboratories and many colleagues throughout the UKAEA, especially Mr D G Bridge and Dr D G Martin for their help in providing references and suggesting topics for inclusion. Finally, he thanks Mr M J F Notley and Dr J E Garnier of Battelle, Pacific Northwest Laboratories for permission to reproduce figures from their published papers and also Mr M L Picklesimer of the United States Nuclear Regulatory Commission for his helpful comments on the draft.
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## Notation

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
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<tbody>
<tr>
<td>a</td>
<td>accommodation coefficient</td>
</tr>
<tr>
<td>$C_f$, $C_c$</td>
<td>fuel and cladding specific heats</td>
</tr>
<tr>
<td>$C_p$, $C_v$</td>
<td>specific heats at constant pressure and constant volume</td>
</tr>
<tr>
<td>d</td>
<td>gap width</td>
</tr>
<tr>
<td>$d_{\text{min}}$</td>
<td>gap width existing under fuel/cladding contact conditions</td>
</tr>
<tr>
<td>$d_{\text{ox}}$</td>
<td>oxide film thickness on cladding</td>
</tr>
<tr>
<td>$E_s$</td>
<td>stored energy in the fuel</td>
</tr>
<tr>
<td>g</td>
<td>combined temperature jump distance ($g_f + g_c$)</td>
</tr>
<tr>
<td>$g_f$, $g_c$</td>
<td>temperature jump distances at the fuel and cladding surfaces</td>
</tr>
<tr>
<td>H</td>
<td>Meyer hardness of cladding</td>
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<td>h</td>
<td>total conductance across the gap</td>
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<tr>
<td>$h_{\text{film}}$</td>
<td>film conductance</td>
</tr>
<tr>
<td>$h_g$</td>
<td>conductance through the gas</td>
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<tr>
<td>$h_r$</td>
<td>conductance due to radiative heat transfer</td>
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<tr>
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<td>conductance across solid/solid contacts</td>
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<tr>
<td>$k_f$, $k_c$</td>
<td>fuel and cladding thermal conductivities</td>
</tr>
<tr>
<td>$k_f'$</td>
<td>thermal conductivity of fuel at some density less than theoretical</td>
</tr>
<tr>
<td>$k_{\text{gas}}$</td>
<td>thermal conductivity of a gas</td>
</tr>
<tr>
<td>$k_m$</td>
<td>geometric mean conductivity of fuel and cladding</td>
</tr>
<tr>
<td>$l$</td>
<td>mean free path</td>
</tr>
<tr>
<td>M</td>
<td>molecular weight</td>
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<tr>
<td>P</td>
<td>linear power</td>
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<tr>
<td>$P_a$</td>
<td>fuel/cladding contact pressure</td>
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<tr>
<td>$P_{\text{gas}}$</td>
<td>gas pressure</td>
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<td>$P_0$</td>
<td>reference gas pressure (0.1013 MPa)</td>
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<tr>
<td>$Q_f$, $Q_c$</td>
<td>volumetric heat generation rates in fuel and cladding</td>
</tr>
</tbody>
</table>
$R_f, R_c$  surface roughnesses of fuel and cladding

$R_T$  combined surface roughness

$r_{ci}, r_{cs}$  cladding inner and outer radii

$r_{fi}, r_{fs}$  fuel inner and outer radii

$T_f, T_c$  fuel and cladding temperatures (may be followed by subscript $i$ or $s$ to denote inner and outer surface)

$T_{cool}$  coolant temperature

$T_{gas}$  temperature of gas

$T_o$  reference temperature ($273K$)

t  time

$V$  void fraction (porosity)

$x$  mole fraction

$\alpha_f, \alpha_c$  fuel and cladding thermal diffusivities

$\gamma$  ratio of specific heats, $\gamma = C_p/C_v$

$\delta$  Stefan's constant

$\varepsilon_f, \varepsilon_c$  emissivities of fuel and cladding

$\eta$  viscosity of gas

$\rho_f, \rho_c$  fuel and cladding densities
### TABLE 1

**Estimated uncertainties at the beginning of life of an LWR fuel rod (from Ref. 26)**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Relative uncertainty % (± 3 σ)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel and cladding dimensions</td>
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<tr>
<td>Cladding thermal conductivity</td>
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</tr>
<tr>
<td>Coolant temperature</td>
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</tr>
<tr>
<td>Water film heat transfer coefficient</td>
<td>15</td>
</tr>
<tr>
<td>Heat capacity</td>
<td>5</td>
</tr>
<tr>
<td>Fuel thermal conductivity</td>
<td>15</td>
</tr>
<tr>
<td>Gas thermal conductivity</td>
<td>5</td>
</tr>
<tr>
<td>Flux depression</td>
<td>5</td>
</tr>
<tr>
<td>Increase in fuel cross-section area due to thermal expansion</td>
<td>0.3</td>
</tr>
<tr>
<td>Local linear heat rate</td>
<td>15</td>
</tr>
</tbody>
</table>

### TABLE 2

**Effect of hyperstoichiometry on UO₂ conductivity**

<table>
<thead>
<tr>
<th>O/U ratio</th>
<th>Percentage of stoichiometric conductivity</th>
<th>Goldsmith &amp; Douglas (43)</th>
<th>Howard &amp; Gulvin (48)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>773K 1023K 1273K</td>
<td>1193K</td>
<td></td>
</tr>
<tr>
<td>2.00</td>
<td>100 100 100</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>2.01</td>
<td>86  89  91</td>
<td>92</td>
<td>92</td>
</tr>
<tr>
<td>2.02</td>
<td>75  81  84</td>
<td>86</td>
<td>86</td>
</tr>
<tr>
<td>2.05</td>
<td>56  63  69</td>
<td>71</td>
<td>71</td>
</tr>
<tr>
<td>2.10</td>
<td>43  51  60</td>
<td>65</td>
<td>65</td>
</tr>
</tbody>
</table>
TABLE 3

Constants in the expression for calculating conductivities of the rare gases

<table>
<thead>
<tr>
<th>Gas</th>
<th>Ref. 52</th>
<th>Ref. 75</th>
<th>Ref. 8</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>A</td>
<td>S</td>
<td>A</td>
</tr>
<tr>
<td>He</td>
<td>$3.366 \times 10^{-3}$</td>
<td>0.668</td>
<td>$2.836 \times 10^{-3}$</td>
</tr>
<tr>
<td>Kr</td>
<td>$4.029 \times 10^{-5}$</td>
<td>0.872</td>
<td>$1.772 \times 10^{-4}$</td>
</tr>
<tr>
<td>Xe</td>
<td>$4.726 \times 10^{-5}$</td>
<td>0.923</td>
<td>$7.993 \times 10^{-5}$</td>
</tr>
</tbody>
</table>

These constants give $k$ in W.m$^{-1}$K$^{-1}$ for temperatures in K

TABLE 4

Normalised fission rate distributions as a function of radius and burn-up in a 2.6% enriched LWR fuel rod

<table>
<thead>
<tr>
<th>Burn-up Mwd/tU</th>
<th>Radius, cm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.0667</td>
</tr>
<tr>
<td>0</td>
<td>0.979</td>
</tr>
<tr>
<td>125</td>
<td>0.978</td>
</tr>
<tr>
<td>6240</td>
<td>0.941</td>
</tr>
<tr>
<td>16224</td>
<td>0.906</td>
</tr>
<tr>
<td>31200</td>
<td>0.876</td>
</tr>
<tr>
<td>45380</td>
<td>0.856</td>
</tr>
<tr>
<td>47536</td>
<td>0.853</td>
</tr>
</tbody>
</table>

*Outer surface

TABLE 5

Combined temperature jump distances for helium at s.t.p. from in-reactor experiments

<table>
<thead>
<tr>
<th>Source</th>
<th>$\delta_0$, $\mu$m</th>
</tr>
</thead>
<tbody>
<tr>
<td>Campbell et al (88)</td>
<td>5.2</td>
</tr>
<tr>
<td>Ross and Stoute (17)*</td>
<td>5.0</td>
</tr>
<tr>
<td>Ainscough and Hobbs (90)</td>
<td>3.2</td>
</tr>
<tr>
<td>Miller and Appelhans (91)</td>
<td>4.0</td>
</tr>
</tbody>
</table>

*Out-of-reactor result
FIG. 1. IDEALISED TEMPERATURE PROFILE ACROSS GAP
FIG. 2. HEAT FLOW AT AN IDEALISED CONTACT SPOT
FIG. 3. UNCERTAINTY IN CENTRE-LINE TEMPERATURE FOR A PWR ROD, AS A FUNCTION OF UNCERTAINTY IN LINEAR HEAT RATING. THE CURVES CORRESPOND TO THE 3 DIFFERENT COMBINATIONS OF UNCERTAINTIES, AS NOTED, AND APPLY AT BEGINNING OF LIFE.
FIG. 4. UNCERTAINTY IN STORED ENERGY (ABOVE COOLANT TEMPERATURE) FOR THE PWR ROD AS A FUNCTION OF UNCERTAINTY IN LINEAR HEAT RATE. THE CURVES CORRESPOND TO THE SAME THREE CASES AS IN FIG. 3, AND ALL APPLY AT BEGINNING OF LIFE.
FIG. 6. GAP CONDUCTANCE, $h_g$, VERSUS MEAN PLANE SEPARATION, $D_{mp}$ (AFTER GARNIER AND BEGEJ, REF. 81)
FIG. 7 GAP CONDUCTANCE vs TEMPERATURE FOR SMOOTH-SURFACE PAIR IN LIGHT CONTACT, FOR He AT 0.1 MPa (AFTER GARNIER AND BEGEJ, REF. 81)
FIG. 8. GAP CONDUCTANCE vs GAS PRESSURE AT 473 K FOR VARIOUS GASES (AFTER GARNIER AND BEGEJ, REF. 82)
FIG. 9. EXPERIMENTAL RESULTS OF HELIUM PRESSURE CYCLE AT 56 kW.m⁻¹
(AFTER CAMPBELL et al, REF. 88)
FIG. 10. COMPARISON OF RESULTS OF HELIUM AND ARGON PRESSURE CYCLES (AFTER CAMPBELL et al., REF. 88)