STUDIES OF LOSS-OF-COOLANT AND
LOSS-OF-REGULATION ACCIDENTS
Final Report

A research report prepared for the
Atomic Energy Control Board by

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# Table of Contents

List of Figures ............................................................. ii
Nomenclature ........................................................................ iii

1.0 Introduction.................................................................. 1

2.0 Thermal Analysis of a CANDU Fuel Channel Following a LOCA with Impaired ECC Flow
  2.1 Analytical Model ..................................................... 2
  2.2 Results for a Bruce-A Reactor Unit ........................... 5
  2.3 Results for the NPD and Douglas Point Reactors ....... 10
  2.4 Effects of Initial (EOB) Conditions and Bundle Slumping Times .................................................... 17
  2.5 Effects of Variations in Contact Conductance and Contact Strip Width ........................................... 19
  2.6 Implications of Zircaloy-Water Reaction for IMPECC Analysis and Results ....................................... 22
  2.7 Conclusions .............................................................. 29

3.0 Film Cooling and Film Stability on Calandria Tubes .... 32
  3.1 Background ................................................................ 32
  3.2 Film Flow and Heat Transfer Characteristics ............ 32
    3.2.1 Film thickness on horizontal tubes .................... 32
    3.2.2 Film heat transfer coefficients ........................... 37
  3.3 Film Stability and Breakdown .................................... 43
    3.3.1 Falling film breakdown under isothermal conditions ............................................................ 43
    3.3.2 Falling film breakdown under non-isothermal conditions ......................................................... 46
  3.4 Behavior of Rivulet Flow ............................................. 58
  3.5 Preliminary Experiment on Stability of Film Flow on Horizontal Tubes .............................................. 68

4.0 Analysis of Flow Reversal in Vertical Feeder Tubes .... 70

5.0 Miscellaneous Tasks ..................................................... 72

References ........................................................................... 75
Figures ............................................................................... 80
Acknowledgements ............................................................. 95
<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Sagged Pressure Tube - Normal Bundle Configuration</td>
<td>80</td>
</tr>
<tr>
<td>2</td>
<td>Sagged Pressure Tube - Slumped Bundle Configuration</td>
<td>81</td>
</tr>
<tr>
<td>3</td>
<td>Temperatures After EOB with Normal Bundle Configuration - Bruce Reactor</td>
<td>82</td>
</tr>
<tr>
<td>4</td>
<td>Temperatures After EOB with Slumped Bundle Configuration - Bruce Reactor</td>
<td>83</td>
</tr>
<tr>
<td>5</td>
<td>Maximum Local Heat Flux on Calandria Tube After EOB - Bruce Reactor</td>
<td>84</td>
</tr>
<tr>
<td>6</td>
<td>Temperatures After EOB - NPD Reactor</td>
<td>85</td>
</tr>
<tr>
<td>7</td>
<td>Maximum Local Heat Flux on Calandria Tube After EOB - NPD Reactor</td>
<td>86</td>
</tr>
<tr>
<td>8</td>
<td>Maximum Local Heat Flux on Calandria Tube After EOB - Douglas Point Reactor. Effect of Contact Conductance and Strip Width</td>
<td>87</td>
</tr>
<tr>
<td>9</td>
<td>Maximum Fuel-Sheath and Pressure Tube Temperature. LOECC Case. Bruce Reactor</td>
<td>88</td>
</tr>
<tr>
<td>10</td>
<td>Diagram of Falling Film Flow on a Horizontal Tube</td>
<td>89</td>
</tr>
<tr>
<td>11</td>
<td>Non-Dimensional Film Thickness at Isothermal Breakdown and Rivulet Area Coverage. From Mikielewicz and Moszynski (37)</td>
<td>90</td>
</tr>
<tr>
<td>12</td>
<td>Model for Wide, Flat Rivulet Flow</td>
<td>91</td>
</tr>
<tr>
<td>13</td>
<td>Characteristics of Wide, Flat Rivulets. NPD Reactor Conditions</td>
<td>92</td>
</tr>
<tr>
<td>14</td>
<td>Schematic Diagram of Apparatus for Film Stability Experiment</td>
<td>93</td>
</tr>
<tr>
<td>15</td>
<td>Film Flow on Heated Horizontal Tube. Preliminary Results. $q/A = 5.5 \text{ W/cm}^2$, $Re_\Gamma = 490.$</td>
<td>94</td>
</tr>
</tbody>
</table>
**Nomenclature**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>rate constant, cm/sec&lt;sup&gt;1&lt;/sup&gt;</td>
</tr>
<tr>
<td>B</td>
<td>coefficient in equation 34, function of heating surface temperature</td>
</tr>
<tr>
<td>C&lt;sub&gt;R&lt;/sub&gt;</td>
<td>coefficient in equation 32, function of Re&lt;sub&gt;T&lt;/sub&gt;</td>
</tr>
<tr>
<td>C&lt;sub&gt;S&lt;/sub&gt;</td>
<td>bulk concentration of steam in steam-hydrogen mixture, moles/mole</td>
</tr>
<tr>
<td>D&lt;sub&gt;S&lt;/sub&gt;</td>
<td>diffusion coefficient for steam through hydrogen, cm&lt;sup&gt;2&lt;/sup&gt;/sec</td>
</tr>
<tr>
<td>d</td>
<td>tube diameter, cm or m</td>
</tr>
<tr>
<td>ΔE</td>
<td>activation energy for diffusion of O&lt;sub&gt;2&lt;/sub&gt; in ZrO&lt;sub&gt;2&lt;/sub&gt;, J/gm-mole</td>
</tr>
<tr>
<td>f(θ)</td>
<td>related to shape factor of circular-sector rivulet, defined by equation 40</td>
</tr>
<tr>
<td>g</td>
<td>acceleration of gravity, m/s&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>g&lt;sub&gt;c&lt;/sub&gt;</td>
<td>gravitational constant, kg m/Ns&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>ΔH&lt;sub&gt;Z&lt;/sub&gt;</td>
<td>enthalpy of reaction for Zircaloy-water reaction, J/gm (Zr)</td>
</tr>
<tr>
<td>h</td>
<td>heat transfer coefficient, W/m&lt;sup&gt;2&lt;/sup&gt;K</td>
</tr>
<tr>
<td>K&lt;sub&gt;G&lt;/sub&gt;</td>
<td>mass transfer coefficient, gm-mole/sec cm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>k</td>
<td>thermal conductivity, W/m K</td>
</tr>
<tr>
<td>L</td>
<td>length of film in direction of flow, cm or m</td>
</tr>
<tr>
<td>M</td>
<td>molecular weight</td>
</tr>
<tr>
<td>m</td>
<td>mass flow rate, kg/s or gm/s</td>
</tr>
<tr>
<td>m&lt;sub&gt;s&lt;/sub&gt;</td>
<td>steam flow rate per unit area of solid surface to maintain reaction, gm/sec cm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>Pr</td>
<td>Prandtl number</td>
</tr>
<tr>
<td>q&lt;sup&gt;&quot;&lt;/sup&gt;</td>
<td>heat flux, W/cm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>q&lt;sub&gt;Z&lt;/sub&gt;</td>
<td>heat flux resulting from Zircaloy-water reaction, W/cm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
<tr>
<td>R</td>
<td>tube radius, cm or m</td>
</tr>
<tr>
<td>R&lt;sub&gt;0&lt;/sub&gt;</td>
<td>universal gas constant, 8.3202 J/gm-mole K</td>
</tr>
<tr>
<td>R&lt;sub&gt;R&lt;/sub&gt;</td>
<td>radius of circular-sector rivulet, μm or mm</td>
</tr>
</tbody>
</table>
Re

T

TPT

ΔT

ΔT

T

t

td

Uc

u

Vs

w

Xo

y

z

β

Γ

δ

δs

δ+

δc

θ

λ

μ

ρ

σ

Δσ

- Reynolds number of a falling film, defined by equation 19
- rivulet spacing, mm or μm
- temperature, °C or K
- pressure tube temperature, °C
- temperature difference from solid surface to bulk film temperature, °C or K
- temperature difference from solid surface to film surface, °C or K
- time, s or min.
- time required to deplete coolant volume in fuel channel of steam, sec.
- contact conductance over contact strip width between sagged pressure tube and calandria tube, W/cm K
- velocity in direction of film or rivulet flow, m/s or mm/s
- normal coolant volume per unit area of fuel sheath surface in a fuel channel, cm³/cm²
- width, m, mm or μm
- fraction of surface area covered by circular-sector rivulets
- distance normal to direction of film or rivulet flow, mm or μm
- length or thickness of oxide layer, cm.
- contact angle, degrees or radians
- film mass flow rate per unit length of periphery, g/m s
- film thickness or rivulet thickness, mm or μm
- mean distance for steam diffusion through hydrogen to reach solid surface, cm.
- non-dimensional falling film thickness, defined by equation 18
- non-dimensional critical film thickness, reference 37, defined by equation 50.
- angle of inclination of an inclined surface to the horizontal or angular position on a horizontal tube measured from the upper stagnation point, radians
- fundamental wave length of wave motion of film surface, cm or m
- absolute viscosity, kg/m s
- density, kg/m³
- surface tension, N/m
- change in surface tension from solid surface to film surface, N/m
\( \tau_0 \) - shear stress in film at surface, N/m²

\( \phi \) - defined by equation 55

\( \psi \) - angle measured from center-line of circular sector rivulet, degrees or radians

**Subscripts**

b - film breakdown, unless otherwise specified
c - critical, unless otherwise specified
CT - calandria tube
F - fuel or wide, flat rivulet
f - liquid
R - circular sector rivulet
r - contact strip between sagged pressure tube and calandria tube
S - sheath
s - steam
v - falling film on vertical surface
x - at position x
z - Zircaloy

A bar over a quantity designates an average value.
1.0 Introduction

This report is the final report on studies of loss-of-coolant and loss-of-regulation accidents undertaken for the Atomic Energy Control Board under contract serial number OSU 77-00272 over the period July 1, 1977, to June 30, 1979. It describes the nature and results of the major tasks undertaken. Further information and details will be found in the quarterly progress reports on the studies (1-7), as well as in references 8 to 10. In addition, descriptions and results of certain miscellaneous tasks within the scope of the contract have been provided in technical memos which were issued from time to time during the course of the studies. These miscellaneous tasks are listed in this report.
2.0 Thermal Analysis of a CANDU Fuel Channel Following a LOCA with Impaired ECC Flow

A major part of the effort on these studies has been devoted to the thermal analysis of a CANDU fuel channel following a LOCA with impaired ECC performance. The results of an earlier study for AECL in this area are described in reference 11. The progress of the current work has been described in the quarterly progress reports (1-7). The model developed for this analysis has been described in detail by Rogers and Currie (8), who also describe the computer program, IMPECC, used for the analysis as well as providing results for a fuel bundle in a Bruce reactor channel at a power level of 7.5 MW. In reference 9, Rogers summarizes the information given in reference 8 and gives more recent results for a Bruce channel than those given in reference 8. Instructions for using a simplified version of IMPECC are given in reference 10. The application of IMPECC to the NPD and Douglas Point reactors and the sensitivity of predicted results to the variation of certain parameters have been described in recent progress reports (5,6,7). In this report a brief description of the analytical model will be given and major results and conclusions of the studies will be summarized. Further information may be obtained in the various references cited.

2.1 Analytical Model

The model permits a detailed analysis of the transient thermal behavior of the fuel elements and pressure and calandria tubes following a LOCA with no ECC flow. The model allows for decay heat generation in the fuel elements and accounts for heat transfer and heat storage effects. The model also allows for the non-axisymmetric conditions which result from the pressure tube sagging onto the calandria tube and the bundle slumping to the bottom of the pressure tube.

The model considers three periods following the end-of-blowdown (EOB) after a LOCA with delayed ECC flow. These periods are:

a) the period between EOB and the moment at which the pressure tube sags onto the calandria tube. During this period, the fuel bundle, pressure tube and calandria tube are assumed to have a normal configuration;
b) the period between the sagging of the pressure tube onto the calandria tube and the slumping of the fuel bundle to the bottom of the pressure tube. During this period, the fuel bundle itself is assumed to retain its normal configuration, and the pressure tube is assumed to contact the calandria tube as shown in Figure 1;

c) the period following the slumping of the fuel bundle into the bottom of the pressure tube. Since there is no experimental information available on bundle slumping, the approach taken was to assume two configurations as limiting cases: the normal open bundle shown in Figure 1 and the maximum-packing configuration shown in Figure 2. It is expected that the actual thermal behavior of a slumped bundle will be bracketted by the results of these two cases.

While all cases studied to date have assumed the above sequence of periods, the model and the computer program developed to implement it can also consider the case of fuel bundle slumping prior to pressure tube sagging. Some comments on the probable effects of such a modified sequence are given later in this report.

For the thermal analysis of the contacting pressure and calandria tubes, an implicit finite-difference technique is used in a computer program, CONCYL, with variable circumferential and radial node spacings established by the time-temperature variation itself. Analysis of heat transfer between the tubes accounts for contact conductance over a locally-deformed strip and for circumferentially-varying heat transfer rates across the eccentric gas gap between the tubes. Conservative values for contact conductance, based on the work of Yovanovich (12) and Currie (13), are used. Heat transfer across the eccentric gas gap is by gas conduction and radiation. The analysis allows for non-continuum effects in the narrowest portion of the gas gap. Boundary conditions on the outer surface of the calandria tube consist of either natural convection or nucleate boiling heat transfer coefficients, the choice of which is dictated in the program by the instantaneous local conditions.
For its normal configuration, the model represents the fuel bundle by concentric rings containing the same total masses of UO₂ and Zircaloy as in the actual fuel elements in the corresponding rings of the bundle, thus simulating their heat capacities. For the standard CANDU 37-element bundle, four rings are used, consisting of four fuel nodes and seven sheath nodes.

For a 19-element bundle, three rings are used, consisting of three fuel nodes and five sheath nodes. A model for a 28-element bundle has not yet been applied. The model allows for conduction within the UO₂ fuel, contact conductance between the fuel and the sheath, conduction through the sheath and radiation and conduction through stagnant steam between the fuel-element rings. The interface boundary condition between the models of the fuel bundle and the pressure and calandria tubes is represented by a circumferentially-varying, time-dependent thermal resistance resulting from radiation and conduction heat transfer. A comprehensive computer program, IMPECC, has been developed which incorporates both the fuel bundle model and the model of the pressure and calandria tubes.

For the slumped configuration, the actual thermal contacts between the fuel elements were determined by using the program CONCYL to establish appropriate shape factors. Use of CONCYL in this way required the elimination of the fuel sheath nodes, so that the bundle is now represented by the fuel nodes only, although the thermal resistances of the sheath and interface between the sheath and the pellet are accounted for. The interface boundary condition between the bundle and the pressure tube was handled as before except that the actual thermal contacts between the elements and the pressure tube are allowed for, using CONCYL. Also, conservatively, it was assumed that there was no heat transfer between the top fuel elements and the segment of the pressure tube above the slumped bundle. The slumped bundle model has also been incorporated into IMPECC, with the slumped bundle replacing the normal bundle at a specified time after the start of the transient. The specified time is chosen by examining predicted temperatures for the case of no bundle slumping.

In the present study, no allowance is made for heat generated by the Zircaloy-water reaction, based on the argument that with no ECC flow
there will be no steam supply to maintain the reaction. This assumption is discussed later in this report.

Initial conditions used are those for the appropriate EOB state generally predicted using the computer program RODFLOW (14), corrected to allow for radiation from the pressure tube to the calandria tube during blowdown.

2.2 Results for a Bruce-A Reactor Unit

IMPECC has been applied to a CANDU reactor fuel channel with a 37-element bundle with a maximum fuel element rating of 48 W/cm. This rating represents the maximum rating for a channel power of 7.5 MW, corresponding to a maximum-power channel (6.5 MW) with a 15% on-power refuelling ripple in a Bruce-A reactor.

The analysis was done for the EOB conditions following a critical LOCA, a 25% inlet header break, for the low-pressure gravity-controlled ECC system presently used in the Bruce reactors.

Reasonable values of contact strip widths and conductances were used in the analysis. The insensitivity of results to wide variations in these parameters is demonstrated later in this report.

Normal emissivities used in the analysis are 0.8 for the fuel sheaths and pressure tubes and 0.2 for the calandria tube. The latter value is quite conservative for an oxidized Zircaloy tube. The sensitivity of results to variation in the emissivity of the calandria tube is given later in this report.

Figure 3 shows certain temperatures versus time after EOB for the limiting case of no bundle slumping. The pressure tube sags onto the calandria tube approximately 90 seconds after EOB. Figure 4 gives temperatures for the other limiting case of bundle slumping. Slumping is assumed to occur 90 seconds after the pressure tube sags onto the calandria tube (about three minutes after EOB). This choice of bundle slumping time assumes that slumping will begin at a time when sheath temperatures range from about 1300°C to about 1675°C (Figure 3). In any case, the insensitivity of longer-term fuel, sheath and pressure tube temperatures and local peak heat fluxes on the calandria tube to assumed bundle slumping times is demonstrated in reference 8 and later in this report.
Figures 3 and 4 indicate that, irrespective of the actual time of bundle slumping, sheath melting would not be expected to begin on the inner elements until slightly after three minutes after EOB and would not reach the inner portions of the outer fuel elements until about 4.5 minutes after EOB. It is also very probable that the outer portions of the sheaths on the outer fuel ring will not melt at all even with ECC flow delayed indefinitely.

Figure 3 shows that, for the normal bundle configuration, the predicted maximum fuel pellet (center element) temperature will reach about 2540°C about 15 minutes after EOB. For the slumped-bundle case, the predicted maximum fuel pellet temperature is about 2690°C, again about 15 minutes after EOB.*

Of course, fuel elements other than the center element will reach peak temperatures lower than these values. The fuel temperatures of all the elements at the times of the peak fuel temperatures for the center element shown in Figures 3 and 4 are given in Table 1. We see from Table 1 that the peak temperatures of most of the fuel elements in even the highest rated bundle will be significantly below those for the center element for both limiting cases.

Considering that actual fuel temperatures should lie between those predicted for the two limiting cases, that conservative assumptions have been used for the slumped-bundle case, and that most fuel bundles in the core will be at lower power ratings than those assumed in the analysis, there is a very strong possibility that no fuel melting would occur in a highly-rated CANDU fuel channel in a LOCA even if ECC flow is delayed indefinitely, assuming no steam supply to maintain the Zircaloy-water reaction.

Even if some of the inner elements in the highest rated bundles should melt, it is quite likely that the molten fuel would solidify as it moved towards the bottom of the pressure tube. However, this question requires further study before a firm statement can be made.

* While temperatures have not quite reached their peak values at the longest times shown in Figures 3 and 4, mathematical extrapolation shows that the actual peaks are within about 30°C of the maximum values shown and are reached about 18 minutes after EOB.
### TABLE 1

**APPROXIMATE PEAK FUEL TEMPERATURES FROM IMPECC ANALYSIS FOR BRUCE REACTOR**

<table>
<thead>
<tr>
<th>Time</th>
<th>No Bundle Slumping</th>
<th>Bundle Slumping</th>
</tr>
</thead>
<tbody>
<tr>
<td>Elements &amp; Number</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Center, 1, °C</td>
<td>2541</td>
<td>2695</td>
</tr>
<tr>
<td>Inner Ring, 6, °C</td>
<td>2488</td>
<td>2600</td>
</tr>
<tr>
<td>Middle Ring, 12, °C</td>
<td>2290</td>
<td>2397</td>
</tr>
<tr>
<td>Outer Ring, 18, °C</td>
<td>1919</td>
<td>2033</td>
</tr>
</tbody>
</table>
Figures 3 and 4 also show that the maximum local temperature of the pressure tube reaches a predicted peak value of about 1460°C for the case of no bundle slumping and about 1635°C for the slumped bundle case, about 15 minutes after EOB. Although not shown, the predicted pressure tube circumferentially-integrated average temperatures at this time are about 1340°C and 1200°C, respectively. Again, considering that actual temperatures probably lie between the predictions of the two cases and the conservative assumptions used in the slumped-bundle case, we conclude that the pressure tube will not melt even if ECC flow is delayed indefinitely. Although no stress and deformation analysis have been made, reference 8 presents arguments to show that the pressure tube should also maintain reasonable integrity with indefinitely delayed ECC flow. Deformations occurring would tend to reduce the predicted maximum tube temperatures (8). Should some of the inner fuel elements in the highest rated bundles melt and re-solidify, as discussed earlier, it appears very probable that the pressure tube would still maintain its integrity. Again, however, further assessment is required before a definitive statement can be made.

Figures 3 and 4 show that the maximum temperature of the calandria tube outer surface remains almost constant throughout the transient. The predicted maximum local external surface temperature of about 130°C occurs, in both cases, when the pressure tube sags onto the calandria tube. The corresponding maximum local inner surface temperature at this instant is about 460°C. Therefore, it appears that the calandria tube will maintain its strength and integrity throughout the transient even if ECC flow is delayed indefinitely. However, the predicted temperatures are calculated on the basis that critical heat flux (CHF) does not occur on the outer surface of the calandria tube. Figure 5 shows the maximum local heat flux on the calandria tube for the two limiting cases. We see that the peak heat flux increases very suddenly as the pressure tube sags onto the calandria tube and, for a fraction of a second, exceeds the conservative lower-bound sub-cooled CHF value for the bank of calandria tubes (8). Other results show that the momentary peak heat flux exceeds the lower-bound CHF over only two to three degrees of angular position from the line of contact.
We conclude that there is not enough energy transferred near and above the CHF value for critical heat flux actually to occur. Therefore, we conclude that the calandria tube will maintain its strength and integrity in the incident even if ECC flow to the channel is indefinitely delayed.

These results also support the arguments for the maintenance of a coolable geometry following a LOCA, should ECC flow be delayed (8).

The sensitivity of the predicted results for CANDU fuel channels to variations in certain parameters has been examined. The effects of variation in initial (EOB) conditions and of the assumed time of bundle slumping on results are discussed in section 2.4 after presentation of results obtained by application of IMPECC to the NPD and Douglas Point reactors.

Predicted temperatures of the fuel, sheaths and pressure tube are quite sensitive to the emissivity of the calandria tube. An increase in calandria tube emissivity to 0.3, still a conservative value, reduces maximum fuel and sheath temperatures by about 60°C and maximum pressure tube temperatures by about 120°C (5).

The effects on component temperatures and calandria tube heat fluxes of variations in separation distance between the pressure and calandria tubes after contact because of variation in surface roughness has been investigated and found to be quite minor for surface separation distances from 0 to 60 micro-inches (5).

To reduce the costs of running cases with the IMPECC program, a simplified version has been developed which assumes constant and uniform calandria tube outer surface temperature. The validity of this assumption is based on the results obtained with the complete program as discussed above which show that the calandria tube outer temperature varies only slightly during the transient even in the vicinity of the contact region immediately after the pressure tube sags onto the calandria tube. Furthermore, as also discussed above, CHF will not occur on the calandria tube at this time. To confirm the validity of this assumption, two cases were run for Bruce conditions which were identical except for the calandria tube external surface boundary conditions. One used the normal natural convection and sub-cooled nucleate boiling heat transfer coefficients
(8,9), while the other used constant and uniform external surface temperatures. The peak heat fluxes after contact of the pressure tube with the calandria tube differed by about 5% only in the two cases, with the value for the constant temperature boundary condition being higher and thus conservative (6). All fuel, sheath and pressure tube temperatures at any given time after EOB agreed to within 1 or 2 degrees for the two cases.

Instructions for using the simplified version of IMPECC have been prepared (10). These instructions do not incorporate steps to allow for bundle slumping. It is planned to develop instructions for using the complete version of IMPECC, including allowance for actual boundary conditions on the outer surface of the calandria tube and for fuel bundle slumping.

A copy of the instructions for the simplified version of IMPECC, a listing, a set of punched cards and a sample case output have been provided to Ontario Hydro, with the permission of AECL.

2.3 Results for the NPD and Douglas Point Reactors

The IMPECC program has been modified to permit its application to 19-element fuel bundles. The modified version has been used to analyze a LOCA with impaired ECC in the NPD and Douglas Point reactors. The simplified version of IMPECC, with constant and uniform calandria tube outer surface temperature was used for these analyses.

For NPD, runs were made for two cases, RIH ruptures of 153 cm² and 2044 cm², which give maximum sheath temperatures and maximum fuel temperatures at EOB, respectively. A fuel element rating of 27 W/cm² (bundle power ≈ 275 kW), 10% above the license limit, was assumed. It was assumed that the pressure tube sagged onto the calandria tube at 1,000°C, as for Bruce, but it is not known whether this value is realistic. In addition, since the pressure tube may not actually contact the calandria tube because of the spacer spring between them and the relatively low maximum temperatures reached by the pressure tube, runs were made in which the pressure tube was assumed not to sag onto the calandria tube.

Results are shown in Figures 6 and 7.
Figure 6 shows that very long times elapse for both break sizes before the pressure tube reaches 1000°C, the assumed sagging temperature. In both cases, the pressure tube maximum temperatures decrease after contact occurs between the tubes, so that the assumed sagging temperature of 1000°C is the maximum temperature reached by the pressure tube.

For the case of a sagging pressure tube, the maximum fuel temperatures reached are almost the same for both break sizes, about 1390°C, far below the UO₂ melting temperature of 2800°C. The maximum fuel temperature is reached at about 15 1/4 minutes after EOB for the large break and about 2 minutes later for the small break. Similarly, the maximum sheath temperatures reached are almost the same for the two break sizes, about 1380°C, well below the Zircaloy melting temperature of 1700°C. Again, there is about a 2 minute difference between the times at which these peak temperatures are reached for the two break sizes.

As before, heat generated by the Zircaloy-water reaction was ignored in these analyses.

The results also show that the maximum fuel, sheath and pressure tube temperatures reached are not greatly affected by whether the pressure tube sags or not.

No analysis of the effect of bundle slumping has been done for NPD. With the relatively low fuel and sheath temperatures reached it is not evident that bundle slumping will occur. In any event, the effect of bundle slumping on fuel and sheath temperatures has earlier been shown to be relatively small for the Bruce reactor, particularly considering the conservative assumption used in the slumped bundle case of no radiation nor convection heat transfer from the top of the slumped bundle to the region of the pressure tube above it. Therefore, it is not expected that bundle slumping will have a major effect on predicted fuel and sheath temperatures in NPD.

As in the Bruce reactor, the local instantaneous heat flux at the bottom of the outer surface of the calandria tube momentarily reaches very high values just after contact of the pressure tube with the calandria
tube, and then quickly drops back to considerably lower values. In this case, because of the very high thermal conductivity of the aluminum alloy calandria tube, the instantaneous maximum peak heat fluxes reached are considerably higher, about 970 W/cm², and the peak heat fluxes take somewhat longer, about 20 seconds, to drop back below 100 W/cm² than in the Bruce reactor case, as can be seen in Figure 7. As with fuel and sheath temperatures, local peak heat fluxes are essentially independent of break size, i.e., of initial condition. Also, high heat fluxes (those above 100 W/cm²) occur over a somewhat larger angle than before, about 80° on each side of the line of contact. Although the energy transfer associated with these high but short-lived local heat fluxes near the bottom of the calandria tube is still not very great, as in the case of Bruce, it is uncertain whether the momentary high local heat fluxes will disrupt the film flow on the outside of the calandria tube provided by the spray headers. This question requires further study, the results of which may be pertinent also to Pickering A and Douglas Point.

Should no pressure tube sagging occur, or should there be no cooling film disruption if pressure tube sagging does occur, we conclude that fuel integrity and a coolable geometry will be maintained even if ECC flow is delayed indefinitely. However, some fuel element sheath failures, and hence fission product releases, would be expected from the highly-rated elements in NPD, since sheath failure, by oxidation and embrittlement, is expected at about 1050°C (15, Appendix IX). Nevertheless, about 3 1/2 (large break) to 5 (small break) minutes would elapse after EOB before the hottest fuel sheaths would begin to fail. Even in these cases, however, a coolable geometry will be maintained and there will be no sheath ballooning. Thus, the behavior of a fuel channel in NPD following a LOCA will be very insensitive to even considerable delays in the initiation of ECC flow.

Of course, the analysis and conclusions are based on the assumption that film flow is maintained on the outside of the calandria tube throughout the incident, i.e. that the sprays function and the film is not disrupted.
The IMPECC program has also been applied to the Douglas Point Reactor to estimate fuel, sheath and pressure tube temperature histories following a LOCA with delayed ECC flow.

Analyses using RODFLOW of blow-downs of the Douglas Point Reactor primary circuit were not available to establish end-of-blow-down conditions for the initial conditions of the IMPECC analysis. Therefore, initial conditions were estimated using information from the Douglas Point Reactor Safety Report (16), page 5.3:23 and Figure 5.3.5. A difficulty arises since the data given in the safety report are for 100% full power, whereas Douglas Point is now licensed only for 70% full power and the IMPECC analysis was required for the latter condition. However, as we have seen for NPD, maximum temperatures and heat fluxes reached appear to be quite independent of initial (EOB) temperatures, although the times at which events occur are affected by the initial temperatures. Therefore, great accuracy in specifying the initial conditions does not appear to be necessary. Estimates of the initial temperatures were made by comparison of Douglas Point conditions with those for Bruce and NPD. These estimates gave ranges of sheath, fuel and pressure tube initial (EOB) temperatures and EOB times as summarized in Table 2.

Cases were run for EOB temperatures and times as specified in Table 2. The EOB temperatures were assumed to be the same for all fuel elements in the bundle. Again, it was assumed that the pressure tube sags onto the calandria tube at $1000^\circ$C; the validity of this assumption must be confirmed. As for the NPD analysis, bundle slumping was ignored in these cases. Some important results are summarized in Table 3.

Table 3 shows that the pressure tube may sag onto the calandria tube between 3 minutes 20 seconds and 8 minutes 20 seconds after EOB. Over this range, the pressure tube maximum temperatures decrease after contact occurs between the tubes, as was the case for NPD, so that the assumed sagging temperature of $1000^\circ$C is the maximum temperature reached by the pressure tube.
**TABLE 2**

ESTIMATED EOB CONDITIONS FOR
DOUGLAS POINT REACTOR

<table>
<thead>
<tr>
<th>Break size</th>
<th>- 60 in²</th>
</tr>
</thead>
<tbody>
<tr>
<td>Break location</td>
<td>- PSH</td>
</tr>
<tr>
<td>Operating power level</td>
<td>- 70% FP. (Element rating - 28 W/cm)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case</th>
<th>1</th>
<th>2</th>
</tr>
</thead>
<tbody>
<tr>
<td>EOB Temp., °C, Fuel</td>
<td>1090</td>
<td>830</td>
</tr>
<tr>
<td>EOB Temp., °C, Sheath</td>
<td>1060</td>
<td>800</td>
</tr>
<tr>
<td>EOB Temp., °C, Pressure Tube</td>
<td>600</td>
<td>400</td>
</tr>
<tr>
<td>EOB time, secs.</td>
<td>47</td>
<td>54</td>
</tr>
<tr>
<td>Case</td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>-------------------------</td>
<td>---------------</td>
<td>---------------</td>
</tr>
<tr>
<td>Time for PT sag*</td>
<td>3m 20s</td>
<td>8m 20s</td>
</tr>
<tr>
<td>(T_F), max. °C at t*</td>
<td>1458</td>
<td>1410</td>
</tr>
<tr>
<td>(T_S), max. °C at t*</td>
<td>1443</td>
<td>1396</td>
</tr>
<tr>
<td>TPT, max. °C at t*</td>
<td>~1000</td>
<td>~1000</td>
</tr>
<tr>
<td>(sag point)</td>
<td>3m 20s</td>
<td>8m 20s</td>
</tr>
</tbody>
</table>

* Time after EOB
Although the calculated range of pressure tube sagging times is quite wide for the range of assumed EOB conditions, the predicted maximum fuel temperatures are not very different over this range, 1410°C and 1458°C, well below the UO₂ melting temperature of 2800°C, as can be seen from Table 3. These temperatures are reached about 13 minutes and about 9 minutes after EOB, respectively. The maximum sheath temperatures are also not greatly different, 1396°C and 1443°C respectively, occurring a few seconds before the maximum fuel temperatures in each case, both well below the Zircaloy melting temperature of 1700°C.

These results confirm the insensitivity of maximum fuel, sheath and pressure tube temperatures to the initial (EOB) conditions.

Although no cases were run for Douglas Point Reactor without pressure tube sagging, it appears, by comparison with the NPD case, that the maximum fuel, sheath and pressure tube temperatures reached will not be greatly affected by whether the pressure tube sags or not.

Although bundle slumping was not considered for this case, as for the NPD case, it is again not expected that it will have a major effect on fuel and sheath temperatures.

As for the previous IMPECC analyses, heat generated by the Zircaloy-water reaction has been ignored.

As before, the local instantaneous heat fluxes at or near the bottom of the outer surface of the calandria tube momentarily reach high values just after contact of the pressure tube with the calandria tube. Here, because of the low-conductivity Zircaloy calandria tube, the peak heat fluxes reached are similar to those in Bruce, about 310 W/cm², rather than those in NPD with its aluminum calandria tubes. Also, the peak heat fluxes drop back below 100 W/cm² more quickly and extend over a smaller angle than in the NPD case, consistent with the results for the Bruce case. We conclude, as for the Bruce case, that although the momentary peak heat fluxes are somewhat above the conservatively estimated CHF, the energy transferred near and above the CHF point is insufficient to cause actual CHF.

As for the NPD case, should there be no cooling film disruption on the calandria tube, we conclude that fuel integrity and a coolable geometry
will be maintained even if ECC flow is delayed indefinitely. However, assuming that the sheath failure mechanism will be oxidation and embrittlement at about 1050°C, as for NPD, some fuel sheath failures and fission product releases would be expected. At the upper end of the range of assumed EOB temperatures, 1060°C, such failures of the hottest fuel sheaths would be expected about the time of EOB, while at the lower end of the range, such failures would be expected to begin about 2 minutes 20 seconds after EOB. Nevertheless, a coolable geometry will be maintained since there will probably be no sheath ballooning nor bundle slumping. Thus, we conclude that the behavior of a fuel channel in the Douglas Point reactor following a LOCA will be relatively insensitive to delays in the initiation of ECC flow.

The analysis and conclusions are based on the assumption that film flow is maintained on the calandria tube throughout the incident, i.e., that the sprays function and the cooling film is not disrupted.

2.4 Effects of Initial (EOB) Conditions and Bundle Slumping Times

The application of IMPECC to the NPD reactor has clearly shown that the maximum fuel, sheath and pressure tube temperatures are essentially independent of break size, as shown in Figures 6 and 7. That is, these parameters are quite independent of initial (EOB) temperatures of the fuel, sheath and pressure tube. Similarly, the results in Table 3 show the relative insensitivity of component peak temperatures to assumed initial conditions for the Douglas Point reactor. Over the time scales required to reach peak temperatures, initial thermal storage effects have essentially disappeared (e.g., the internal time constant of the fuel elements is of the order of 10 seconds) and quasi steady-state conditions prevail considering the relative slow temperature changes of fuel, sheaths and pressure tube at and near the times of peak temperatures. Therefore, the peak temperatures reached are essentially determined only by the instantaneous heat generation rates and the instantaneous thermal resistances within the fuel elements and between fuel element sheaths, the pressure tube and the
calandria tube at the times of the peak temperatures. Only the times at which the peaks occur are significantly dependent on the initial conditions. It is clear from the foregoing considerations that the maximum temperatures reached by fuel, sheaths and the pressure tube in the accident considered here will be essentially independent of initial (EOB) temperatures in all CANDU reactors, including Bruce.

The same conclusion is reached for the maximum calandria tube outer surface heat fluxes which will be governed mainly by pressure tube temperature at the instant of sagging and the thermal characteristics of the pressure and calandria tubes as well as the heat transfer mechanisms between the tubes, assuming, as discussed earlier, that effective heat transfer from the outer surface of the calandria tube is not disrupted.

Similar conclusions can be drawn as to the effect of the time (i.e., the fuel and sheath temperatures) at which the fuel bundle is assumed to slump. Reference 8 shows that peak temperatures reached by fuel, sheaths and pressure tube are quite independent of the assumed time of bundle slumping.* The generality of this conclusion can be verified from an examination of Figure 4 which shows that rapid rates of change of temperature of bundle components do not result from bundle slumping; conditions following bundle slumping are therefore essentially quasi steady-state so that maximum temperatures reached by bundle components and the pressure tube are dependent essentially on instantaneous conditions and not on prior history. This conclusion will be valid even if bundle slumping occurs before pressure tube sagging, for the reasons given, although no cases have been yet run to demonstrate this fact.

It is obvious that the time of bundle-slumping will not affect the peak heat fluxes on the calandria tube for cases in which the bundle slumps onto the pressure tube after the pressure tube sags onto the calandria tube. However, we may also conclude that the peak calandria tube heat fluxes

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* A simplified model of bundle slumping was used in this earlier analysis. However, from a consideration of the mechanisms involved, it is evident that the same conclusion would result from the use of the current bundle slumping model.
occurring at the moment of pressure tube sagging will not be affected greatly whether or not the bundle slumps onto the pressure prior to the pressure tube sagging onto the calandria tube. This conclusion is reached because the heat fluxes occurring are essentially governed, as mentioned earlier, by the temperature of the pressure tube, i.e., the heat stored in the pressure tube, and the thermal characteristics of heat transfer between the pressure and calandria tubes. Flow of heat from the slumped bundle to the pressure tube (and thence through the outer surfaces of the calandria tube) will be impeded by the thermal resistances between the slumped bundle and the pressure tube so that stored energy in the outer elements of the slumped bundle will not significantly affect the peak instantaneous heat fluxes on the calandria tube at the moment of contact between the pressure and calandria tubes. In other words, the time constant for heat flow from the outer fuel elements to the outer surface of the calandria tube will be considerably longer than that for heat flow from the pressure tube to the outer surface of the calandria tube. Therefore, bundle slumping before pressure tube-calandria tube contact will result in, at the most, a relatively small increase in local peak heat fluxes, and a somewhat slower decay of the local heat fluxes than experienced when bundle slumping follows pressure tube sagging. This conclusion will be confirmed in proposed future work.

2.5 Effects of Variations in Contact Conductance and Contact Strip Width

In reference 8, Appendix 2, it was shown that maximum local calandria tube heat fluxes and pressure tube temperatures for Bruce conditions were very insensitive to variations in the assumed values of contact conductance from 0.57 to 57 W/cm²K and of contact strip width from 0.212 to 0.848 mm. However, this analysis was done for steady-state conditions at the moment of maximum heat flow rate from the pressure tube to the calandria tube. Since the maximum heat flow rate occurs for Bruce conditions about 12 to 13 minutes after EOB when fuel and sheath temperatures have reached their peak values and are changing very slowly, the conditions are quasi-steady-state and the use of the steady-state analysis is justified.
However, there remained the question of the sensitivity of predictions to variations in contact conductance and contact strip width under the very rapid transient conditions which occur as the sagging pressure tube makes contact with the calandria tube.

Using the simplified version of IMPECC, the sensitivity of predicted heat fluxes and temperatures to contact conductance and contact strip width was investigated for Douglas Point Reactor conditions. The ranges of conductance and strip width used were 5.67 to 56.7 W/cm²K* and 0.258 to 1.031 mm. respectively. These and other conditions used and results obtained are given in Table 4.

Table 4 shows that the peak transient heat flux from the calandria tube to the moderator is very insensitive to the large variations in contact conductance and contact strip width examined. An increase in the nominal values (Case 1) of contact conductance by a factor of 10 and contact strip width by a factor of 4 (Case 4) results in an increase of momentary peak heat flux of about 10% only. Also, the peak heat fluxes are reached at exactly the same instant after contact in each case. The variations of maximum calandria tube heat fluxes (i.e. those at the contact strip) with time over a period before and after contact are shown in Figure 8. Again, it can be seen that there are essentially negligible differences among the values for the cases studied. Table 4 shows that there are also negligible differences in maximum fuel, sheath and pressure tube temperatures at the times at which the maximum fuel temperatures are reached, about 8 or 9 minutes after EOB.

Therefore, the insensitivity of predicted maximum fuel, sheath and pressure tube temperatures and calandria tube heat fluxes to wide variations in contact conductance and contact strip width has been conclusively demonstrated.** From a consideration of the mechanisms involved, it is

* For comparison, WNRE has used values of 0.5 and 1.5 W/cm²K for contact conductance in their studies of pressure tube-calandria tube contact (17). Higher values of $U_c$ will result in more severe conditions, i.e., peak heat fluxes following contact of the pressure tube onto the calandria tube will be higher.

** The contact strip widths examined represent a reasonable range considering elastic deformation of the pressure tube. Should large plastic deformation of the pressure tube occur, the peak heat fluxes on contact could be higher than those given here, but it is difficult to see how contact over large strip widths could occur rapidly. However, this question should be investigated further.
TABLE 4
IMPECC ANALYSIS OF DOUGLAS POINT REACTOR
SENSITIVITY OF RESULTS TO CONTACT CONDUCTANCE
AND CONTACT STRIP WIDTH

Break size - 60 in²
Break location - PSH
Operating power level - 70% F.P. (Element rating - 28 W/cm)
EOB time, sec. 47
EOB temp., fuel, °C 1090
EOB temp., sheath, °C 1060
EOB temp., pressure tube, °C 600
Time after EOB for PT sag 3m 20s
Temperatures at PT sag

<table>
<thead>
<tr>
<th>Case</th>
<th>1</th>
<th>2*</th>
<th>3*</th>
<th>4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$U_c$, W/cm²K</td>
<td>5.67</td>
<td>5.67</td>
<td>56.7</td>
<td>56.7</td>
</tr>
<tr>
<td>$w_r$, mm</td>
<td>0.258</td>
<td>1.031</td>
<td>0.258</td>
<td>1.031</td>
</tr>
<tr>
<td>$q_{CT}$ max, W/cm²</td>
<td>305.9</td>
<td>307.5</td>
<td>317.3</td>
<td>336.0</td>
</tr>
<tr>
<td>at t, **sec</td>
<td>0.366</td>
<td>0.366</td>
<td>0.366</td>
<td>0.366</td>
</tr>
<tr>
<td>Max. Temps. at t, ***</td>
<td>9m 12s</td>
<td>9m 10s</td>
<td>9m 3s</td>
<td>8m 13s</td>
</tr>
<tr>
<td>$T_F$ max, °C</td>
<td>1458</td>
<td>1455</td>
<td>1455</td>
<td>1457</td>
</tr>
<tr>
<td>$T_S$ max, °C</td>
<td>1443</td>
<td>1440</td>
<td>1440</td>
<td>1442</td>
</tr>
<tr>
<td>TPT, max, °C</td>
<td>942</td>
<td>940</td>
<td>940</td>
<td>942</td>
</tr>
</tbody>
</table>

* Very slight errors in temperature (~1 or 2 °C) and heat fluxes (< 1%) at a given time after EOB exist because of an error in EOB time assumed for these cases (54 seconds instead of 47 seconds)

** t after PT-CT contact

*** t after EOB.
evident that this conclusion will apply also to other CANDU reactors and initial conditions.

The basic reason for the insensitivity observed is that gas conduction and radiation across the eccentric gap between the contacting pressure and calandria tubes play a major role in transient heat transfer between the tubes. The important role of gas conduction and, by extrapolation, radiation in heat transfer between non-conforming contact surfaces has been demonstrated by Currie (13,18) in research supported, in part, by AECB. This work has also shown the insensitivity of contact conductance between non-conforming surfaces to surface roughness (i.e. microscopic conditions).

2.6 Implications of Zircaloy-Water Reaction for IMPECC Analysis and Results

In the IMPECC analysis, heat generation resulting from the Zircaloy-water reaction has been ignored, based on the argument that, with no ECC flow, there would be no steam supply to maintain the reaction. The implications of this assumption have been examined (6) and the results of this examination are summarized here.

The Zircaloy-water reaction is described by:

\[ \text{Zr} + 2\text{H}_2\text{O} \rightarrow \text{ZrO}_2 + 2\text{H}_2 \]  

(1)

This reaction is exothermic with an enthalpy of reaction, \( \Delta H_z \), of -11.91 kJ/gm (Zr). The reaction rate may be controlled either by the rate of diffusion of oxygen through the previously-formed layer of ZrO\(_2\) or by diffusion of steam in the gas (vapor) phase through the hydrogen gas which will form at the solid surface as the reaction takes place. The reaction rate will be controlled by the slower of these two mechanisms.

For the solid-phase diffusion-controlled reaction, it can be shown from the Fick diffusion equation and the Arrhenius rate equation that, for non-isothermal conditions as experienced in the present problem, the rate of growth of the ZrO\(_2\) layer is given by:
The values of $A$ and $\frac{\Delta E}{2R_o}$, where $\Delta E$ is the activation energy and $R_o$ the universal gas constant, are given by Urbanic and Heidrick (19) as follows:

\[
\frac{dz}{dt} = \frac{d}{dt} \left[ \int_0^t \left( A \exp\left(-\frac{\Delta E}{2R_oT}\right) \right)^2 dt \right]^b
\]

The discontinuity in the Arrhenius rate equation indicated by the two sets of constants results from the change in oxide microstructure to cubic ZrO$_2$ at temperatures greater than $1580^\circ$C.

Although it is generally accepted that, under normal conditions, the Zircaloy-water reaction is not gas-phase diffusion limited (19), Heidrick indicates that the gas-phase diffusion can control the reaction rate at high temperatures, particularly as the concentration of hydrogen in the steam increases (20). It can be shown that, for the reaction rate controlled by mass transfer from the steam-hydrogen mixture to the solid surface, the growth rate of the ZrO$_2$ layer is given by:

\[
\frac{dz}{dt} = \frac{M_z}{2\rho_z} K_G C_s
\]  

Consistent with the stagnant steam IMPECC model, the mass transfer of steam to the solid surface will be governed by the diffusion rate of steam through stagnant hydrogen. Therefore, the mass transfer coefficient is given by:
\[ K_G = D \frac{\rho_s}{M_s \delta_s} \] (4)

Information was not readily available on the diffusion coefficient of steam in hydrogen. However, Eckert and Drake (21) give diffusion coefficients for steam in air and hydrogen in air, as well as an equation for the diffusion coefficient of steam in air. From a comparison of these coefficients it was concluded that the diffusion coefficient of steam in hydrogen would be about 2.6 times that for steam in air. The diffusion coefficient of steam in hydrogen is then given by multiplying the equation of Eckert and Drake for the diffusion of steam in air by 2.6, giving:

\[ D_s = 0.6115 \left( \frac{T_s}{273} \right)^{1.81} \] (5)

where it has been assumed that the pressure is approximately atmospheric.

With the above equations, the growth rate of the ZrO\(_2\) layer by either mechanism can be calculated for any given conditions. Knowing the growth rate of ZrO\(_2\), the rate of consumption of oxygen from the steam and hence, the steam supply rate to maintain the reaction can be established. From the chemical reaction equation, the required steam flow rate per unit area of solid surface is given by:

\[ \dot{m}_s = \frac{36}{40} \rho_z \frac{dz}{dt} \] (6)

The heat flux (per unit area of solid surface) resulting from the Zircaloy-water reaction is given by:

\[ q^* = -\rho_z \Delta H_z \frac{dz}{dt} \] (7)

Using the above equations, the magnitudes of the reaction rates and the resulting steam flow rates required to maintain the reaction for each mechanism were calculated for conditions following EOB with delayed ECC.
flow after the critical LOCA in Bruce as calculated by IMPECC. Of course, if the reactions were actually to occur, the sheath temperature history would, in general, be different from that calculated by IMPECC, but the purpose of the present analysis is simply to establish the orders of magnitude of the reaction rates and steam supply rates.

The results are summarized in Table 5. Comparing the oxide layer growth rates, it is evident that the solid-phase diffusion mechanism will be the controlling mechanism under most conditions. The lowest growth rate given in Table 5 for the gas-phase controlling mechanism occurs only when the steam concentration has fallen to 1%, i.e., only when the steam is essentially depleted from the fuel channel. While this lowest growth rate for the gas-phase controlled mechanism is the same as the lowest growth rate for the solid-phase controlled mechanism, the maximum growth rate for the former is much larger than that for the latter. From the steam flow rates, the times required to deplete the free volume of the fuel channel (the normal coolant volume) of steam so as to sustain the reaction can be estimated from:

$$t_d = \frac{\rho_s v'_s}{\dot{n}_s}$$

where $v'_s$ is the normal coolant volume per unit area of fuel sheath in a fuel channel. For relevant conditions, with the lowest steam flow rates given in Table 5 for either mechanism, the time required for steam depletion in the fuel channel is of the order of 0.12 seconds. That is, to sustain even the slowest reaction rates estimated, all stagnant steam in the fuel channel would be almost instantaneously depleted, and would be replaced by a hydrogen atmosphere at the same pressure, thus stopping the reaction. This steam depletion would occur as soon as the sheath temperatures reached values at which the reaction rates become significant, about 950°C (19). The temperature rise of the fuel sheath caused by this initial rapid steam depletion would be about 4°C, which is negligible.

Therefore, for the reaction to persist, a significant steam flow into the fuel channel would have to be initiated. For the lowest steam flow
<table>
<thead>
<tr>
<th></th>
<th>Oxide Layer Growth Rates, cm/sec.</th>
<th>Steam Flow Rates, gm/cm² sec.</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Solid-phase</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Controlled</td>
<td>0.30 to $2.8 \times 10^{-4}$</td>
<td>2 to $16 \times 10^{-4}$</td>
</tr>
<tr>
<td><strong>Gas-phase</strong> *</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Controlled</td>
<td>0.03 to $6 \times 10^{-3}$</td>
<td>0.02 to $3.4 \times 10^{-2}$</td>
</tr>
</tbody>
</table>

* Based on diffusion under stagnant conditions. With flowing steam, mass transfer rates and, hence gas-phase controlled rates, would be considerably higher.
rates, the steam volume in the channel would in effect, be replaced

\[ \frac{1}{0.12} \approx 8 \text{ times per second, or the volume flow would be about } 160,000 \text{ cm}^3/\text{sec, giving an average velocity of about } 47 \text{ meters/sec.} \]

Since actual steam flow rates would probably be considerably higher than this value, good cooling of the fuel would be provided by almost all foreseeable steam flows. Therefore, since the Zircaloy-water reaction will not persist without a significant steam flow, an allowance for the heat generated by the Zircaloy-water reaction, other than the initial 4°C estimated above, is not consistent with the static model used in the IMPECC analysis, in which no flows are assumed.*

To allow for a persisting Zircaloy-water reaction, a model such as CHAN, as developed by WNRE, must be used to account for the required steam flow, the resulting axial variation of coolant, sheath and fuel conditions and the cooling effect of the steam (and hydrogen) flow as well as the heat generated by the Zircaloy-water reaction. A model of this type has been applied to a LOBCC accident in the Bruce reactor and the important results are summarized in Figure 9, which is Figure 12.4.2-1 of the Bruce A Safety Report (22). Figure 9 shows that sheath and pressure-tube temperatures reach a peak because of the Zircaloy-water reaction at a flow rate of about 1% of the normal ECC flow rate to the channel (1160 gm/sec), in the form of steam, and that the sheath and pressure tube temperatures fall below those for zero flow rate (i.e., those for the conditions assumed for IMPECC) at a flow rate slightly over 2%, as steam, of the normal ECC flow rate, because of the cooling effect of the steam-hydrogen flow. The peak temperatures for both the sheath and the pressure tube are about 200°C above those for zero flow-rate. Therefore, the combined effects of the Zircaloy-water reaction and the steam-hydrogen flow increase peak predicted sheath and pressure tube temperatures about 200°C each at the most, for the flow model used, above those that would be predicted by a no-flow

* The IMPECC model assumes that conduction heat transfer between the fuel elements and to the pressure tube is through steam, rather than hydrogen, which would be more correct, recognizing the existence of the Zircaloy-water reaction. Since hydrogen has a thermal conductivity about 6.5 times higher than that of steam at the temperature levels encountered here, this assumption provides a conservative element to the IMPECC analysis.
model such as IMPECC. Thus, there is only a very small "window" of trickle-like steam-hydrogen flow rates which would result in predicted sheath and pressure tube temperatures higher than the maximums predicted by a no-flow model such as IMPECC, and the greatest increases in predicted peak temperatures are only about 200°C. However, it should be noted that the IMPECC model results do not agree well with the no-flow model results given in Figure 9, with the predicted maximum sheath temperatures for Bruce by IMPECC, about 2500°C for no bundle slumping and about 2680°C for bundle slumping, being considerably higher than the value of about 2100°C in Figure 9, while the predicted maximum pressure tube temperatures by IMPECC, about 1460°C for no bundle slumping and about 1630°C for bundle slumping, are considerably lower than the value of about 2000°C in Figure 9.

Considering the differences in the models used (see Reference 22), these differences in predictions are not surprising. Nevertheless, it is evident that the Zircaloy-water reaction would begin long before the sheath and pressure tube temperatures reached the maximum predicted by IMPECC, which would require steam flow to initiate at much lower temperatures to sustain the reactions so that the peak sheath and pressure tube temperatures with steam-hydrogen flow may well be even below those predicted by IMPECC for no-flow conditions.

Obviously, this question cannot be investigated using IMPECC itself. Considering the efforts being expended by WNRE in developing an improved version of CHAN, CHAN-2 (17), which will incorporate a multi-element model of the fuel bundle, as in IMPECC, rather than the single-element model used in CHAN and will allow for pressure-tube sagging, as in IMPECC, it does not appear to be advisable to expend time and effort on attempting to duplicate the development of CHAN-2 to assess the effect of the Zircaloy-water reaction more precisely. Rather, the development of CHAN-2 should be followed closely, so as to be in a position to assess the results predicted by it for the effect of the Zircaloy-water reaction on the predicted fuel, sheath and pressure tube temperatures following a LOCA with impaired ECC flow.
2.7 Conclusions

The major conclusions of the study of the behavior of a CANDU fuel channel following a LOCA with impaired ECC flow are summarized below. Conclusions (a) to (i) are valid provided no steam is available to maintain the Zircaloy-water reaction.

(a) The most highly-rated fuel elements in a Bruce reactor will probably not melt even if ECC flow is delayed indefinitely. Lower-rated fuel elements will almost certainly not melt.

(b) Sheath melting will probably begin on the maximum-rated center elements of a Bruce reactor about three minutes after EOB and will spread to the inner portions of the sheaths on the outer ring of fuel elements about 4 1/2 minutes after EOB. The outer portions of the sheaths on the outer fuel ring will probably not melt.

(c) The pressure tube in a maximum power Bruce reactor channel probably will not melt, even if ECC flow is delayed indefinitely. The results, with arguments presented in reference 8, suggest that the pressure tubes should retain reasonable integrity.

(d) The calandria tubes in a Bruce reactor will remain well cooled throughout the transient. Critical heat flux will not occur on the calandria tubes.

(e) Provided stable film cooling of the calandria tubes is maintained by the calandria sprays, even the most highly-rated fuel elements will not melt in either the NPD or the Douglas Point reactor (with the latter at 70% rated power), nor will sheath melting occur, even if ECC flow is delayed indefinitely. These conclusions hold whether or not the pressure tubes sag onto the calandria tubes.

(f) Provided stable film cooling of the calandria tubes is maintained by the calandria sprays, the pressure and calandria tubes in NPD and Douglas Point (at 70% rated power) will not melt and will maintain their integrity even if ECC flow is delayed indefinitely and whether or not the pressure tubes sag onto the calandria tubes.
(g) Maximum temperatures occurring in fuel, sheaths or pressure tubes in any CANDU reactor with indefinitely delayed ECC flow are quite insensitive to initial (EOB) conditions and to the time at which bundle slumping occurs. These maximum temperatures are not reached until about 10 to 15 minutes or more after EOB.

(h) Peak local heat flux behavior on the calandria tubes is essentially independent of initial (EOB) conditions and will not be affected greatly by whether bundle slumping occurs before or after the pressure tube sags onto the calandria tube.

(i) Maximum component temperatures and calandria tube peak heat fluxes are quite insensitive to contact strip width and conductance between the pressure tube and calandria tube.

(j) While the IMPECC model cannot handle three-dimensional effects which would be necessary to account for the Zircaloy-water reaction, it is apparent that realistic allowance for the Zircaloy-water reaction by such codes as CHAN-2 will result in maximum predicted fuel sheath temperatures no more than 100°-200°C above those predicted by IMPECC because of the cooling effects of the steam flows required to maintain the reaction. These maximum temperatures will only occur for a narrow window of steam flow rates over the range of about 1% to 2% of normal ECC flow rates.

In general, we may conclude that the moderator is an effective heat sink following a LOCA with delayed ECC flow in a CANDU reactor for those reactors without moderator dump. For reactors with moderator dump, the calandria sprays provide an effective heat sink provided that they are not disrupted. The thermal behavior of a highly-rated CANDU fuel channel will not be very sensitive to delays in ECC flow because of the relatively long periods before fuel sheaths reach melting temperatures. The results also support the contention that a coolable geometry will be maintained, even for long delays in ECC flow.
A number of important questions remain to be answered, and further analytical and experimental work at Whiteshell Nuclear Research Establishment (23) will provide much of the needed information in the long run. In the meantime, the IMPECC model can be used for additional assessments of the thermal behavior of a CANDU fuel channel following a LOCA with delayed ECC flow.
3.0 Film Cooling and Film Stability on Calandria Tubes

3.1 Background

The NPD, Douglas Point and Pickering A reactors use moderator dump as one of the shut-down systems. Therefore, following a LOCA in these reactors, since the moderator is dumped, the calandria tubes are no longer submerged in the moderator. However, cooling is maintained by spray headers which are fed by pumps taking heavy water from the dump tank. The sprays produce liquid films flowing around the periphery of the calandria tubes (e.g., 24). Very little information is available on flow characteristics of and heat transfer rates to films flowing around horizontal cylindrical tubes under conditions relevant to the present cases.

An investigation has been undertaken of the effectiveness of film flows produced by the calandria sprays in providing adequate cooling of the calandria tubes following a LOCA in reactors with moderator dump. This investigation has covered analyses of film flow rates and heat transfer coefficients, film flow stability, rivulet flow and the design and construction of an apparatus for preliminary, scoping experiments on film flow characteristics.

This section of the report describes these studies.

3.2 Film Flow and Heat Transfer Characteristics

3.2.1 Film thickness on horizontal tubes

The thickness of a fully-developed laminar falling film on a vertical surface for conditions of no shear between the film and gas or vapor beyond it can be calculated by the long-established Nusselt equation, as found in most heat transfer text-books (e.g., 25, p.190):

\[ \delta_v = \left( \frac{3\Gamma \mu_f}{g \rho_f \nu} \right)^{1/3} \]

The Nusselt analysis can be adapted to laminar falling film flow over a horizontal tube. Strictly speaking, the film is
never fully-developed under these conditions, since the effective
gravitational force on the film varies as the film flows around the tube.
However, we will assume that momentum variations resulting from
variations in film thickness and velocity are small and can be ignored.

To establish the velocity profile in the film, the conservation
of momentum equation was written in two dimensions. Assuming that the
film thickness is small with respect to the tube radius, the momentum
equation becomes:

\[
\frac{\partial}{\partial R} \frac{1}{\rho_f} \frac{\partial}{\partial \theta} (u^2) = \frac{\rho_f}{\rho_c} R \sin \theta + \frac{\mu_f}{\rho_c} \frac{\partial^2 u}{\partial y^2}
\]  

In equation 10, the left-hand side represents the rate of change
of momentum, the first term on the right-hand side represents the gravity
force and the second term represents the viscous drag force. In
establishing equation 10, we have assumed that the density of the steam
vapor or helium gas beyond the water film is negligible with respect to
the water density.

As previously mentioned, we ignore momentum variation, so
that the left-hand side of equation 10 disappears. We are left with:

\[
\frac{d^2 u}{dy^2} = - \frac{\rho_f g}{\mu_f} \sin \theta ,
\]  

which is now an ordinary differential equation.

Equation 11 was integrated subject to the following boundary
conditions:

\[
\begin{align*}
y = 0 & , & u = 0 \\
y = \delta & , & \frac{du}{dy} = 0 
\end{align*}
\]  

(12)
The first condition indicates that there is no slip at the tube surface and the second condition results from the assumption that there is no shear between the surface of the film and the surrounding vapor or gas. The latter assumption will be quite good for conditions in the calandria where there is no significant gas flow following moderator dump.

The solution of equation 11, subject to the conditions of equation 12, yields for the velocity profile:

\[ u = \frac{\rho_f g}{\mu_f} \left[ \delta y - \frac{y^2}{2} \right] \sin \theta \]  

(13)

where \( \theta \) is the angular position around the tube, measured from the upper stagnation point (12 o'clock) on the tube.

Equation 13 is the same as the velocity profile generally used for laminar falling films on vertical surfaces except for the \( \sin \theta \) term.

The film flow rate per unit tube length, on each side of a horizontal tube, is:

\[ \Gamma = \rho_f \bar{u} \delta \]  

(14)

where, by definition,

\[ \bar{u} = \frac{1}{\delta} \int_0^\delta u \, dy \]  

(15)

Combining equations 13, 14 and 15, we derive, for the film thickness at any angle \( \theta \):

\[ \delta = \left( \frac{3 \Gamma \mu_f}{g \rho_f^2 \sin \theta} \right)^{1/3} \]  

(16)
Confirmation of this equation for laminar film thickness on a horizontal tube under similar assumptions has been obtained by modifying the results of Moalem and Sideman (26) for a falling, evaporating film on a horizontal tube.

Equation 16 predicts infinite film thickness at $\theta = 0$ and at $\theta = \pi$, the upper and lower stagnation points on the tube. These predictions realistically represent the film approach to, and departure from the tube, assuming that the film does not prematurely separate from the bottom portion of the tube. (See Figure 10) A minimum film thickness is predicted at $\theta = \frac{\pi}{2}$, i.e., at the horizontal plane passing through the center line of the tube (3 or 9 o'clock). The predicted film thickness at this position is identical to that predicted by equation 9 for a laminar falling film on a vertical surface.

Of course, even stable laminar films will tend to have wavy surfaces, in general, as pointed out later, so that the film thicknesses predicted by equation 21 and other equations given here should be considered as time-average film thicknesses at a given location.

An analysis of a turbulent falling film on a horizontal tube has not yet been developed, but it is assumed that the thickness of such a turbulent film can be related to that of a turbulent film on a vertical surface in the same manner as that for laminar films, i.e., a minimum thickness occurs at $\theta = \frac{\pi}{2}$ equal to that for a fully-developed turbulent film on a vertical surface. The thickness of a fully-developed turbulent falling film on a vertical surface, assuming that the universal velocity profile represents the velocity distribution in the film, can be given approximately by the equation of Fujita and Ueda (27):

$$\delta^+_v = 0.102 \text{Re}^{0.80} \Gamma$$

(17)

where $\delta^+_v$ is a non-dimensional film thickness defined by:
and $Re_\tau$ is the film Reynolds number, defined by:

$$Re_\tau = \frac{4 \Gamma}{\mu_f}$$  \hspace{1cm} (19)

Equation 17, using equations 18 and 19, can be rewritten in a form similar to that for a laminar film on a vertical surface, equation 9:

$$\delta_v = \left( \frac{1.6 \mu_f^{0.4} \left( \frac{\tau_o}{g \rho_f^2} \right)} {0.0956} \right)^{1/3}$$  \hspace{1cm} (20)

Equation 20 can be used to predict the minimum thickness of a turbulent film on a horizontal tube, under the assumption given earlier.

The minimum thicknesses of falling films on the calandria tubes in NPD were calculated using the above equations. The conditions used in the calculations are summarized below (28):

<table>
<thead>
<tr>
<th>Condition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total spray flow rate</td>
<td>270 Igpm</td>
</tr>
<tr>
<td>Calandria tube length</td>
<td>425 cm</td>
</tr>
<tr>
<td>Calandria tube O.D.</td>
<td>10.42 cm</td>
</tr>
<tr>
<td>Average film temperature</td>
<td>80 °C</td>
</tr>
</tbody>
</table>

To establish the nominal film flow rate for normal cooling of the calandria tubes, it was assumed that the spray flow was uniformly distributed over the length and both sides of each calandria tube in any given horizontal row of calandria tubes. The maximum number of tubes in any horizontal row is 12. The resulting nominal flow rate per unit length of wetted perimeter is about 195 g/ms, which is equivalent to a film Reynolds number of about
2240, which indicates a nominally turbulent film* (27). Allowing arbitrarily for non-uniformity of spray flow distribution by dividing the flow rate by a factor of two, the resulting film flow rate per unit length of perimeter is about 95 g/ms and the film Reynolds number is about 1120, which indicates a nominally laminar film* (27).

The predicted minimum film thicknesses for NPD conditions obtained from the foregoing equations for the nominal and the assumed minimum flow values are listed in Table 6.

3.2.2 **Film heat transfer coefficients**

Just as the falling film thickness on a horizontal tube will vary with angular position on the tube, the heat transfer coefficient to the film will also vary with angular position. In addition to the obvious effect of the variation of film thickness on the heat transfer coefficient, a thermal boundary layer will develop in the film as it comes onto the

* The actual flow conditions in a falling film are relatively complex, so that the classification into laminar and turbulent films is approximate. Brumfield and Theofanous (29) show that flow conditions in falling films are governed by a wave structure super-imposed on a base film. At low Reynolds numbers both base film and waves are laminar, at higher Reynolds numbers the base film is laminar and the waves are turbulent while at still higher Reynolds numbers both the base film and the waves are turbulent. Even under these last conditions there are, of course, a laminar sub-layer and a buffer layer next to the solid surface.
TABLE 6

FILM FLOW CONDITIONS
CALANDRIA TUBE SPRAYS
NPD REACTOR

<table>
<thead>
<tr>
<th></th>
<th>Nominal* Values</th>
<th>Assumed** Minimum Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \Gamma, \text{ g/m s} )</td>
<td>195</td>
<td>97</td>
</tr>
<tr>
<td>( \text{Re}_\Gamma )</td>
<td>2240</td>
<td>1120</td>
</tr>
<tr>
<td>( \delta_{\text{min}}, \mu m )</td>
<td>315</td>
<td>222</td>
</tr>
</tbody>
</table>

* For a horizontal row of 12 calandria tubes 4.25 metres long with uniform film flow rates over them.
** Film flow rate = \( \frac{1}{2} \) nominal film flow rate.
tube, which will result in an additional cause for variation in the film heat transfer coefficient with position.

However, it has been customary to predict heat transfer coefficients for falling films on horizontal tubes by using equations for falling films on vertical surfaces. This practice appears to have arisen in the prediction of heat transfer coefficients for condensing films, where it would have greater validity, since the film flow rate and, hence, thickness, under condensing conditions, will continue to grow generally no matter whether the film flows down a vertical surface or over a bank of horizontal tubes. In the present case, of course, the film flow rate remains constant over each tube, with the thickness varying, according to equation 16, in a similar manner on each tube.

The development of appropriate equations for the prediction of heat transfer coefficients for falling films on horizontal tubes has not yet been undertaken, but will be done in a forthcoming study.

In the meantime, conventional practice will be followed in applying equations for heat transfer coefficients for falling films on vertical surfaces to the present cases. A justification for this approximate approach is that heat transfer coefficients with falling films tend to be quite high, so that their resulting thermal resistances do not constitute the governing thermal resistance in the flow of heat from the fuel to the moderator in the present case.

It is assumed that all heat transferred from the tube to the liquid film remains in the film, that is, that there is no heat transfer from the film to the surrounding gas or vapor. This is a valid assumption for well sub-cooled films.

For a laminar falling film, based on the Nusselt film thickness, equation 9, Hewitt and Hall-Taylor (25, eq. 10.18) give an equation for the heat transfer coefficient for fully-developed conditions, which may be written as:

$$h = 1.377 \left( \frac{g \rho_f^2 k_f}{\mu_f \Gamma} \right)^{1/3}$$

(21)
This equation may be expressed in non-dimensional terms as:

\[
\frac{h d}{k_f} = 2.186 \left( \frac{g \rho_f \nu_f^2 \delta^3}{\mu_f^2} \right)^{1/3} Re_T^{1/3} \quad (22)
\]

The equation may also be written in another non-dimensional form often used for film flow heat transfer coefficients:

\[
\frac{h}{k_f} \left( \frac{\nu_f^2}{g \rho_f^2} \right)^{1/3} = 2.186 Re_T^{1/3} \quad (23)
\]

Fujita and Ueda (27) give an equation for heat transfer to a fully-developed laminar falling film on a vertical surface, for conditions of no heat transfer from the film to the surrounding gas which is identical to equation 23 except that the coefficient is 2.27, a difference of only 3.5%.

For turbulent falling films, the analytical results of Dukler for fully-developed conditions with no interfacial shear on a vertical surface (30) can be used. Dukler expresses his results in plots of the non-dimensional heat transfer coefficient given on the left-hand side of equation 23 versus film Reynolds number, for a given fluid Prandtl number. Also, turbulent falling film heat transfer coefficients on a vertical surface can be predicted by the empirical correlations of Wilke (31) using the film thickness relationship of Brauer (32). The resulting equations are:

\[
\frac{h}{k_f} \left( \frac{\nu_f^2}{g \rho_f^2} \right)^{1/3} = 0.06102 \ Re_T^{0.667} \ pr^{0.344} \quad (24)
\]

for \(1600 < Re_T < 3200\)

\[
\frac{h}{k_f} \left( \frac{\nu_f^2}{g \rho_f^2} \right)^{1/3} = 0.00871 \ Re_T^{0.4} \ pr^{0.344} \quad (25)
\]

for \(Re_T \geq 3200\)
Fully-developed falling film heat transfer coefficients for vertical surfaces for the flow and property conditions of NPD were calculated by the foregoing methods. Results are given in Table 7.

As shown in Table 7, the predicted heat transfer coefficients are quite high, being considerably larger than the natural convection coefficients which would occur following a LOCA with delayed ECC over much of the outer surface of the calandria tube for CANDU reactors in which moderator dump is not a shut-down mechanism. These predicted coefficients are also of the order of one-half of the nucleate boiling coefficients which occur near the bottom of submerged calandria tubes at the time of sagging of the pressure tube onto the calandria tube following a LOCA with delayed ECC flow. Of course, nucleate boiling can occur also in the films in a similar accident in a reactor with moderator dump, so that heat transfer coefficients near the bottom of the calandria will be of the same magnitude as those for submerged calandria tubes. These conclusions confirm that the use of the simplified IMPECC program, which assumes uniform calandria tube outer surface temperature, i.e., high heat transfer coefficients, is valid for the analyses of the thermal behavior of NPD and Douglas Point following a LOCA with delayed ECC flow (See Sections 2.2 and 2.3 of this report.).

It is interesting to note that there is no significant difference between the coefficients predicted for the nominal film flow (turbulent) conditions and those for the assumed minimum film flow (laminar) conditions.

This result reflects the compensating effects of increased turbulent diffusivity and greater film thickness as the flow transition from laminar to turbulent conditions occurs. The relative insensitivity of falling film heat transfer coefficients to film flow rates (Reynolds numbers) for low Prandtl number fluids over the Reynolds number range encountered here is evident from an examination of the plots of Dukler (30).

Of course, as pointed out earlier, the calculation of heat transfer coefficients for falling films on horizontal tubes by the foregoing methods is only approximate. The errors involved in this approach will be examined in a forthcoming study. However, as explained earlier, the use of heat transfer coefficients calculated by these approximate equations should not introduce significant errors because of the high values of the coefficients,
### TABLE 7

FALLING FILM HEAT TRANSFER COEFFICIENTS
CALANDRIA TUBE SPRAYS
NPD CONDITIONS

<table>
<thead>
<tr>
<th>Description</th>
<th>Assumed Minimum Film Flow Rate (Laminar Conditions)</th>
<th>Nominal Film Flow Rate (Turbulent Conditions)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \Gamma, \text{ g/m s} )</td>
<td>97</td>
<td>195</td>
</tr>
<tr>
<td>( \text{Re}_\Gamma )</td>
<td>1120</td>
<td>2240</td>
</tr>
<tr>
<td>( h, \text{ Btu/hr sq.ft.}^\circ F )</td>
<td>Hewitt, Hall-Taylor (Eq. 23)</td>
<td>1060</td>
</tr>
</tbody>
</table>

* Interpolated for \( Pr = 2.16 \) from plots for \( Pr = 1.0 \) and 10, as recommended by Hewitt and Hall-Taylor (25).
which also justify the use of the simplified IMPECC method for the analyses of NPD and Douglas Point.

3.3 Film Stability and Breakdown

Falling films on vertical surfaces and horizontal tubes from which heat is being transferred to the film can break down and form dry patches under certain conditions. Because of the resulting poor heat transfer rates at the dry patches, the surfaces may overheat and eventually fail. Obviously, the question of the stability of falling films on horizontal circular tubes is of considerable concern in evaluating the behavior of fuel channels and calandria tubes in CANDU reactors with moderator dump, following a LOCA with delayed ECC flow.

This question has been investigated and the results of the investigation are given in this section of the report.

3.3.1 Falling film breakdown under isothermal conditions

Conventional approaches to falling film breakdown on vertical surfaces assume isothermal conditions at any point on the film for the evaluation of property values. Such approaches include that of Hartley and Murgatroyd (33) and that of the adiabatic case of Fujita and Ueda (27).

Hartley and Murgatroyd developed two separate criteria for film breakdown, a force criterion and a power criterion. Fujita and Ueda employed a critical film Weber number of 0.29, derived from the work of Hartley and Murgatroyd, as the criterion of film breakdown. It is important to recognize that the actual mechanism of film breakdown is not considered in the development of these criteria; they represent conditions under which a dry patch, once formed, cannot be re-wet. These criteria are also concerned with conditions in the film only and not with dry-patch surface temperature effects associated with the Leidenfrost phenomenon which leads to the prevention of re-wetting under film boiling conditions.

Criteria for falling film breakdown on horizontal tubes have not been found in the literature. However, the foregoing criteria for vertical
surfaces have been modified to apply to falling films on horizontal circular tubes.

The modification of the force criterion of Hartley and Murgatroyd will be described here. This criterion states that a dry patch in a falling film on a vertical surface will be stable if the dynamic force resulting from the deceleration of the film above the patch is balanced by the surface tension force on the film at the upper edge of the dry patch. The criterion can be expressed by (33):

\[ \sigma (1 - \cos \beta) = \int_{0}^{\delta_c} \frac{\rho_f u^2}{2g_c} \, dy \]  

(26)

The application of this criterion to a falling film on a horizontal tube shows that it can still be expressed by equation 26, since the dynamic force and the resolved surface tension force are collinear at any angular position on the tube. However, in applying equation 26 to a horizontal tube, the appropriate equation for film velocity on a horizontal tube, equation 13, obviously must be used.

Substituting equation 13 into equation 26, integrating and solving for \( \delta_c \), the critical film thickness, we obtain:

\[ \delta_c = 1.72 \left[ \sigma g_c (1 - \cos \beta) \right]^{0.2} \left( \frac{\mu_f^2}{\rho_f g_c} \right)^{0.2} \frac{1}{(\sin \theta)^{0.4}} \]  

(27)

This equation is the same as that given by Hartley and Murgatroyd for the critical film thickness on a vertical surface, except for the term \( (\sin \theta)^{0.4} \). Therefore:

\[ \delta_c = \frac{\delta_{cv}}{(\sin \theta)^{0.4}} \]  

(28)

where \( \delta_{cv} \) is the critical film thickness on a vertical surface by the Hartley and Murgatroyd force criterion.
Since, by equation 28, the critical film thickness varies around the tube, increasing as one moves away from the horizontal mid-plane of the tube, the governing critical thickness must be established by comparing the actual thickness, equation 16, to the critical thickness at any point on the tube. Dividing equation 16 by equation 28, we obtain:

\[ \frac{\delta}{\delta C} = \frac{\delta V}{\delta CV} (\sin \theta)^{-0.0667} \]  

Equation 29 indicates that the film will become less stable (\(\frac{\delta}{\delta C}\) lower) as \(\theta\) varies in either direction from \(\theta = \frac{\pi}{2}\), i.e., from the horizontal mid-plane. However, the variation with position is very weak.

It must be recognized that the modified Hartley and Murgatroyd criterion is a static criterion and does not account for the actual mechanism of film breakdown, which will depend on dynamic effects, such as wave growth rates. Such dynamic effects can be expected to be very important for a film on a horizontal tube where the flow is never fully-developed. Therefore, the validity of the Hartley and Murgatroyd and other similar criteria for film flow on a horizontal tube is rather suspect. Thus, to allow for the slight dependence on \(\theta\) predicted by equation 29 is not very meaningful, particularly as \(\theta\) approaches 0 or \(\pi\) where momentum change effects, which have been ignored in deriving equation 16, become more significant.

Therefore, the Hartley and Murgatroyd and similar criteria can only provide a rough indication of film stability on horizontal tubes. Considering this fact, the static criteria for fully-developed film flow on vertical or inclined plates were applied here at \(\theta = \frac{\pi}{2}\) as rough indicators only of film stability. However, it is probable that the rough criteria established in this way are conservative, since the actual dynamic film breakdown mechanism will require considerable flow length to develop (36), and film flow lengths are quite short on the actual calendria tubes.

For the Hartley and Murgatroyd force criterion, the contact angle was taken as 45°. A range of contact angles from 20° to 45° for water flowing down vertical steel, stainless steel and copper surfaces was deduced by Hartley and Murgatroyd by applying their force criterion to experimental
results obtained by others. The maximum value in this range was used here to be conservative. The results are given in Table 8.

A comparison of the results given in Table 8 with those given in Table 6 suggests that film flows under nominal conditions may be stable considering that the static criteria used will probably be conservative for the actual dynamic film breakdown mechanism.

However, as pointed out earlier, the above approaches to film stability assume isothermal conditions, that is, they ignore the dependence of property values on temperature. While this approach is acceptable for most property values, it may not be so for surface tension. We will consider the effect of the variation of surface tension with temperature on film stability in the next section.

3.3.2 Falling film breakdown under non-isothermal conditions

As we have seen, conventional approaches to film stability which, assuming isothermal conditions, ignore the dependence of property values on temperature, suggest that adequate margins to film breakdown are provided in NPD, and by inference in other CANDU reactors with moderator dump. However, under the actual conditions of concern here, that is, subcooled falling films undergoing heat transfer from a solid surface to the film, premature breakdown of the film may occur by Marangoni instability, as explained below.

With a perturbation of sub-cooled film thickness resulting, for example, from waves, the outer surface of a thinner portion of the film becomes hotter than that of the thicker portion causing a decrease in surface tension in the thinner portion which results in fluid being drawn
<table>
<thead>
<tr>
<th>Method</th>
<th>$\delta_C$, $\mu$m</th>
<th>Re$\Gamma_C$</th>
<th>$\Gamma_C$, g/m s</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>206</td>
<td>897</td>
<td>77.8</td>
</tr>
<tr>
<td>2*</td>
<td>207</td>
<td>910</td>
<td>78.9</td>
</tr>
<tr>
<td>3</td>
<td>206</td>
<td>897</td>
<td>77.8</td>
</tr>
<tr>
<td>4</td>
<td>194</td>
<td>908</td>
<td>78.8</td>
</tr>
</tbody>
</table>

Explanation of Methods:

1. Hartley and Murgatroyd, power criterion (33)
2. Hartley and Murgatroyd, force criterion* (33)
3. Fujita and Ueda, based on laminar film (27)
4. Fujita and Ueda, based on turbulent film (27)

* Based on contact angle = 45°.
from the thinner portion to the thicker portion of the film. Thus, the thinner portion becomes thinner still and eventually, a dry patch can result (e.g., 25, Section 7.3, 34). Once a dry patch has been formed, it may remain stable or actually propagate upstream since the stabilizing force is now proportional to the absolute value of the surface tension and not simply its difference across the film. Several investigators have shown that a film flow rate as much as 10 times that for the case of no heat flow to the film is required to prevent sub-cooled film breakdown (25). As saturation conditions are reached, this premature film breakdown effect disappears since the film surface temperature becomes essentially uniform at the saturation temperature.

A search of the literature has produced three empirical methods for predicting the conditions for premature film breakdown by this mechanism, those of Fujita and Ueda (27), Hsu, et al., (35) and Hallett (36). All of these methods apply to the breakdown of sub-cooled heated films on vertical surfaces and, hence, their applicability to films on horizontal tubes is uncertain. Other restrictions as mentioned later apply to these methods, particularly those of Hsu, et al., and Hallett.

In the most general method, that of Fujita and Ueda, a dimensionless distortion parameter, $K$, the ratio of the temperature-gradient induced surface tension difference to the dynamic force, is introduced. For fully-developed falling films on vertical surfaces with constant surface heat fluxes appropriate substitutions are made for either laminar or turbulent velocity and temperature profiles to establish expressions for the laminar and turbulent film distortion parameters as functions of film Reynolds number, surface heat flux and surface tension gradient with temperature as well as other film property values. Experimental results obtained by Fujita and Ueda for film flows on long (0.6 m and 1.0 m) vertical surfaces were correlated using these expressions to determine separate constant values of $K$ for laminar and turbulent films for both initial and stable dry-patch formation.

For the 0.6 m long surface, the heat flux for stable dry-patch formation is given by:
Laminar regime:

\[ q''_b = 7.5 \times 10^{-4} \frac{k_f}{(-\frac{\partial \sigma}{\partial T})} \frac{\rho_f g}{g_c} \left( \frac{\mu_f^2}{\rho_f^2 g} \right)^{1/3} \text{Re}_T^{4/3} \]  

Turbulent regime:

\[ q''_b = 7.5 \times 10^{-7} \frac{k_f}{(-\frac{\partial \sigma}{\partial T})} \frac{\rho_f g}{g_c} \left( \frac{\mu_f^2}{\rho_f^2 g} \right)^{1/3} \text{Re}_T^{2.12} \text{Pr}_f^{0.344} \]  

Fujita and Ueda also give similar equations for stable dry-patch formation on the 1.0 m long surface which predict lower breakdown heat fluxes than those for the 0.6 m long surface. This result indicates that longer surfaces will have lower breakdown heat fluxes than shorter surfaces since instabilities have a greater opportunity to develop on the longer surfaces.

Hsu, et al. (35), investigated film breakdown on 13-inch long vertical heated surfaces and found that, for a given film Reynolds number, the total energy transferred to the film up to the breakdown point was a constant, i.e., that the amount of work required for film breakdown for a given film Reynolds number is constant.

The results of Hsu, et al., were correlated by equations of the form:

\[ q''_b = C R \text{ Btu/sec.in} \]  

where \( C_R \) is a constant for a given film Reynolds number.

The author has correlated the values of \( C_R \) given by Hsu, et al., as a function of film Reynolds number over a range of Reynolds numbers from 87.5 to 1810 by linear regression. This procedure resulted in the following equation:

\[ q''_b L = 0.531 \text{ Re}_T^{0.65} \text{ W/cm} \]  

In equations 32 and 33, L is the length from the beginning of the heated film to the breakdown point. The results of Hsu, et al., are in agreement with those of Fujita and Ueda in that they show that longer surfaces will have lower breakdown heat fluxes than shorter surfaces. To apply this equation approximately to a horizontal tube rather than a vertical surface, L was taken as the circumferential distance from the top of the tube to the horizontal center plane, i.e. \( \frac{\pi d}{4} \), since the minimum film thickness and assumed point of minimum isothermal film stability occur at this position.

Hallett (36) analyzed the stability of waves on a thin isothermal laminar film on a vertical surface and derived an equation for the critical wave number. He used the isothermal critical wave number in a dimensional analysis for the breakdown of heated films on vertical surfaces and developed the following correlation based on experimental results for a five-foot long vertical surface:

\[
B \left( \frac{\lambda}{\delta_v} \right)^{\frac{\Delta \gamma}{\sigma}} = \text{Re}^{-0.68}_T \tag{34}
\]

where B is an empirical factor which depends on the relative value of the film heat transfer coefficient and the heat transfer coefficient of the flowing hot water which was used as the heat source in his experiments. Because the value of B determined by Hallett depends on the specific nature of the method of providing heat used in his experiments, equation 34 lacks generality. However, it appears evident from Hallett's paper that B will be mainly a function of the surface temperature of the heating surface. For a mean heating surface temperature of 80°C, representative of the conditions of interest, B is 0.0205.

* B varies from 0.0289 for 60°C to 0.0165 for 90°C, so that predicted film breakdown conditions, although thus somewhat a function of surface temperature are not too strongly dependent on it in the temperature range of interest.
### Table 9

**Predicted Non-Isothermal Film Breakdown Conditions**

**NPD Conditions**

<table>
<thead>
<tr>
<th>Method</th>
<th>( \text{Re}_T )</th>
<th>( q''_b, \text{ W/cm}^2 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1120</td>
<td>0.70</td>
</tr>
<tr>
<td>2</td>
<td>2240</td>
<td>1.00</td>
</tr>
<tr>
<td>3</td>
<td>1120</td>
<td>6.2</td>
</tr>
<tr>
<td>4</td>
<td>2240</td>
<td>9.7</td>
</tr>
<tr>
<td>5</td>
<td>1120</td>
<td>11.9</td>
</tr>
<tr>
<td>6</td>
<td>2240</td>
<td>10.5</td>
</tr>
</tbody>
</table>

**Explanation of Methods.**

1. Fujita and Ueda (27), laminar film, equation 30
2. Fujita and Ueda (27), turbulent film, equation 31
3. Hsu, et al. (35), modified, equation 33
4. Hsu, et al. (35), modified, equation 33
5. Hallett (36), modified, equation 38
6. Hallett (36), modified, equation 38 (Not really valid for turbulent flow)
Equation 34 can be used to predict the critical value of $\Delta \sigma / \sigma$ at film breakdown. Since this is not a convenient parameter for analysis purposes, Hallett's correlation has been modified in the following manner. The value of $\Delta \sigma$, the change in surface tension from the wall temperature to the film surface temperature, can be expressed as:

$$\Delta \sigma = \left(-\frac{\partial \sigma}{\partial T}\right) \Delta T_\delta$$  \hspace{1cm} (35)

For a laminar falling film, the relationship between the film surface temperature and the film bulk temperature can be shown* to be given by:

$$\Delta T_{b} = 0.762 \Delta T_\delta$$  \hspace{1cm} (36)

From equations 35 and 36, and the definition of heat flux, the heat flux for film breakdown is given by:

$$q''_{b} = 0.762 \frac{h \Delta \sigma}{\left(-\frac{\partial \sigma}{\partial T}\right)}$$  \hspace{1cm} (37)

Substituting for $h$ from equation 23 and $\Delta \sigma_c$ from equation 34, for $B = 0.0205$, we obtain, after some manipulation:

$$q''_{b} = 4.80 \frac{k_f}{\left(-\frac{\partial \sigma}{\partial T}\right)} \left(\frac{\sigma_f \rho_f}{\sigma_C}\right)^{1/2} Re_f^{0.18}$$  \hspace{1cm} (38)

While the foregoing equations for film breakdown heat flux apply only to developed falling films on vertical surfaces, they have been applied to the nominal and assumed minimum film flows for the NPD conditions considered earlier in this report. The results are given in Table 9.

* This relationship will be demonstrated in a forthcoming report.
The breakdown heat fluxes given in Table 9 are quite low in general,* particularly those predicted by the equations of Fujita and Ueda. The IMPECC analyses for NPD (and Douglas Point) described in section 2.0 of this report and the accompanying discussion showed that it was probable that the pressure tube would not sag onto the calandria tube following a LOCA with delayed ECC flow, even if the ECC flow were delayed indefinitely.

For such cases for NPD, the maximum heat fluxes from the calandria tube to the cooling film, which will be relatively uniform around the calandria tube, are given in Table 10, as predicted by the IMPECC analysis, as 3.67 W/cm² and 3.77 W/cm² for the small and large break cases respectively. If the results for film breakdown heat fluxes given in Table 9 should be valid, those from the equations of Fujita and Ueda indicate that the cooling films on the calandria tubes would be disrupted and dry patches would develop, while those of Hsu, et al., and Hallett, both as modified by the author, indicate that the cooling films would be stable.

For cases for NPD in which the pressure tube sags onto the calandria tube, which, as shown in section 2, will probably not occur until 12 to 15 minutes after EOB (See Figure 7 of this report), the maximum local heat fluxes at $\theta = 90^\circ$, which, as pointed out earlier, is the assumed location of maximum film instability on the calandria tube circumference, are, as shown in Table 10, about the same as the uniform values for no pressure tube sagging, so that similar conclusions to those given above can be drawn. However, with pressure tube sagging, very much higher local heat fluxes occur over a short period of time over a small angle near the bottom of the calandria tube, as shown in Figure 7. In these cases, film disruption in this region may occur, at least momentarily, by a critical heat flux mechanism or, more likely, by bubble nucleation causing ejection of droplets from the film (38). Preliminary estimates, modifying Fujita and Ueda's steady-state analysis for saturated boiling (38) to sub-cooled boiling in the film indicate that complete film ejection would occur at steady-state heat fluxes equal to the space-integrated momentary local

* The heat flux to the moderator from the calandria tubes under normal operating conditions for the NPD and Douglas Point reactors is about 0.2 W/cm².
TABLE 10

PREDICTED MAXIMUM HEAT FLUXES
FROM CALANDRIA TUBES
IMPECC ANALYSIS
NPD CONDITIONS

<table>
<thead>
<tr>
<th>Case</th>
<th>$q''$, W/cm²</th>
<th>Location</th>
</tr>
</thead>
<tbody>
<tr>
<td>Small break, no PT sagging</td>
<td>3.67</td>
<td>Uniform</td>
</tr>
<tr>
<td>Large break, no PT sagging</td>
<td>3.77</td>
<td>Uniform</td>
</tr>
<tr>
<td>Small break, PT sags</td>
<td>3.58</td>
<td>$\theta = 90^\circ$</td>
</tr>
<tr>
<td>Large break, PT sags</td>
<td>3.59</td>
<td>$\theta = 90^\circ$</td>
</tr>
</tbody>
</table>
heat fluxes near $\theta = \pi$ as the pressure tube contacts the calandria tube under NPD conditions.

Whether momentary film disruption by droplet ejection would lead to a stable dry patch is unknown, since the films will be very thick in this region, approaching infinity at $\theta = 180^\circ$, and it is not certain that film disruption by droplet ejection will occur because of the very rapid nature of the quite localized heat flux transient. However, it should be noted that relatively high heat fluxes (20-60 W/cm$^2$) persist locally (over angles of about $10^\circ$ on each side of the 6 o'clock position) for a considerable time after the initial rapid transient which occurs when the pressure tube contacts the calandria tube. See Figures 7 and 8 of this report for NPD and Douglas Point reactor conditions. Further work is obviously necessary, and is planned, on this question.

Thus, the results of this study are somewhat inconclusive, although it appears probable that film disruption will not occur if the pressure tube does not sag onto the calandria tube for NPD (and Douglas Point).

Of course, the validity of the prediction methods for the NPD conditions is uncertain, as has been pointed out. The main reasons for this uncertainty are reviewed and summarized below. In addition other factors which may affect cooling film instability and its consequences are also discussed.

a) The prediction methods are for fully-developed falling film flows on vertical surfaces and their applicability to film flows on horizontal tubes is questionable. It would appear, however, that falling films on horizontal tubes of diameters of interest here should be more stable than fully-developed films on vertical surfaces, considering the dynamic nature of film breakdown and the relatively short film flow lengths.

b) The results of Fujita and Ueda (27) and Hsu, et al. (35) show that the longer the vertical surface, the lower the breakdown heat flux, other things being equal, since instabilities have a greater opportunity to develop. A comparison of the results of Hallett (36), obtained on a five-foot long vertical surface, with those of Norman and McIntyre (34), obtained on a 3\frac{1}{2}-inch long vertical surface, as given in Figure 9 of Hallett's paper
shows that minimum wetting rates are larger for the longer surfaces, i.e., that films are less stable on long than short vertical heated surfaces, in agreement with the results of Fujita and Ueda and of Hsu. The relatively short effective lengths of the flowing films on the calandria tubes, particularly if the critical location is at \( \theta = 90^\circ \), as discussed earlier, (effective length = 8.19 cm. for NPD), would tend to produce relatively high breakdown heat fluxes compared to those predicted for long surfaces, for example by the equations of Fujita and Ueda.

c) None of the equations used here to predict heated film breakdown conditions include an effect of liquid contact angle, which would seem to be an important parameter in film stability (37).

d) The concept of critical film wave-length used in the correlation of Hallett has been shown by Stainthorp and Allen (46) to be a significant parameter at low film flow rates only, \( \text{Re}_f < 50 \). Over the range of \( \text{Re}_f \) of interest here, a complicated flow pattern exists,* so that the parameter \( \frac{\lambda}{a_0} \) probably does not have any real physical significance under conditions of interest here.

e) As shown by Fujita and Ueda (27,38), Marangoni-type instability disappears as saturation conditions are reached since the film surface temperature becomes essentially uniform at the saturation temperature. They also show that as film subcooling decreases, i.e., as saturation conditions are approached, the tendency for Marangoni instability decreases significantly. None of the equations used here allows explicitly for the increase of film stability as film subcooling decreases. Specific data points of Fujita and Ueda

* See photo of preliminary results with experimental apparatus in section 3.5 of this report (Figure 15).
for higher film temperatures (\(^{\sim}80^\circ\text{C}\), as assumed in the present study) show considerably higher film breakdown heat fluxes than those predicted by their equations, equations 30 and 31 of this report.

f) Splashing of the moderator sprays from other tubes would tend to rewet dry patches that might form. The work of Sideman, et al. (26), shows that for the vertical spacing of the calandria tubes in these reactors, the film falling from a tube would tend to break up into columns and drops which would tend to maintain splashing effects to the bottom tubes of the core. This effect is evident in the photos of reference 24. Of course, the critical regions for rewetting dry patches may be on the underside of the calandria tube and splashing may not be of much help in this region.

g) Any D\(_2\)O vapor generated near dry patches will tend to flow downwards, rather than upwards, because the helium gas filling the calandria is considerably lighter than the vapor. This effect will tend to prevent flooding effects on the film which would otherwise occur and which would tend to increase the tendency for film disruption (38).

From the foregoing results and discussions, it would appear that cooling film instability because of Marangoni effects is very unlikely to occur under NPD (and, by extension, Douglas Point*) conditions if the pressure tube does not sag onto the calandria tube following a LOCA with indefinitely delayed ECC flow.

The results of the study are inconclusive, however, for NPD (and Douglas Point) conditions for cases in which the pressure tube sags onto the calandria tube. Following contact of the pressure tube on the calandria tube, maximum local heat fluxes in the vicinity of \(\theta = 90^\circ\), which is assumed

* Nothing can be said at this time about Pickering A, since this case has not been analyzed.
to be the critical position for isothermal or Marangoni-type film instability are essentially the same as those for the cases of no pressure tube sagging. However, very large rapid transient heat fluxes occur over small angles near the contact region. These may momentarily, at least, disrupt the films over a narrow region by rapid bubble nucleation (38). It is uncertain whether any resulting dry patches will be stable because of the relatively thick films in this region, but relatively high (20-60 W/cm²) heat fluxes persist for some time in the vicinity of the contact point, for about ±10° on each side of the 6 o'clock position.

Because of these uncertainties, further work was undertaken on this topic. This work consisted of an analytical assessment of the behavior of rivulet flows which would follow any film breakdown and the design and installation of an experimental apparatus to investigate film behavior on heated horizontal tubes. This work is described in the following sections of this report.

3.4 Behavior of Rivulet Flow

If film breakdown occurs, a rivulet type of flow will result, with the rivulets being thicker than the original film and providing excellent heat transfer from those portions of a calandria tube over which they flow. If the average rivulet spacing is small, i.e., the dry patches are narrow, calandria tube temperatures between the rivulets may not be very high (particularly for NPD with its aluminum calandria tubes). Also, the rivulets may not maintain a stable position but may move randomly on the calandria tubes, periodically rewetting any dry patches that form, although the rewetting flow rate required may be higher than that of the rivulet.

Information in the literature on rivulet flow is rather limited and is confined to rivulet flows on inclined or vertical flat surfaces (e.g., 33, 37, 39, 40, 41).

Although Marangoni effects may govern the disruption of the normal sub-cooled film, the behavior of a dry patch after disruption is insensitive to such effects, with the absolute value of the surface tension rather than its gradient with temperature governing the growth of the dry patch (27). It can be expected that rivulet behavior is also quite insensitive
to Marangoni effects, so that the information in the literature on rivulet flow which does not consider Marangoni effects, may be utilized for the problem in question.

Towel and Rothfeld (39) developed an analysis of a steady flow, fully-developed single laminar isothermal rivulet down an inclined plane at an angle \( \theta \) to the horizontal. They derived generalized equations and identified two limiting cases, a small circular-sector rivulet and a wide, flat rivulet.

For a small circular-sector rivulet, the width is given by:

\[
W_R = \left( \frac{192}{f(\beta)} \frac{\dot{m}_R}{g \rho_f^2 \sin \theta} \right)^{1/4}
\]

(39)

In equation 39, the factor \( 192/f(\beta) \) is a shape factor for the circular-sector cross-section which is a function of contact angle \( \beta \), only:

\[
f(\beta) = \frac{12\beta \cos^2 \beta + 3\beta - 2\cos^3 \beta \sin \beta - 13 \sin \beta \cos \beta}{\sin^4 \beta}
\]

(40)

Equations 39 and 40 show that the rivulet width for a small circular-sector rivulet is, somewhat surprisingly, independent of surface tension. As Towell and Rothfeld (39) and Bankoff (40) show, the width is a strong function of contact angle.

For a wide, flat isothermal rivulet, Towell and Rothfeld give for the width:

\[
W_F = \frac{3}{8} \frac{\dot{m}_F \nu_f \cot \theta}{\rho_f g \rho_c \sin^3 (\beta) \left( \frac{\rho_f g \cos \theta \sigma_c}{\rho_c} \right)^{1/2}}
\]

(41)
The rivulet width is still very sensitive to contact angle, but is now also a strong function of surface tension.

Equation 41 predicts that a fully-developed, laminar, wide, flat rivulet cannot exist on a vertical surface since \( w_F \) goes to zero for \( \theta = 90^\circ \). However, this result arises because of the model used, in which, on an inclined surface, the force of gravity acts to flatten the central portion of a circular-sector rivulet as the rivulet radius grows. Thus, on a vertical surface, with the force of gravity acting parallel to the surface of the rivulet, there would be no tendency to flatten a circular-sector rivulet so that no wide, flat rivulet would form as the rivulet radius increased. Nevertheless, laminar flow films (infinitely wide, flat rivulets) can exist on vertical surfaces. Indeed, if the equation for the maximum thickness of a wide, flat rivulet, as given by Towell and Rothfeld,

\[
\delta_F = 2 \left( \frac{\sigma g_c}{\rho_f g \cos \theta} \right)^{1/2} \sin \left( \frac{\delta}{2} \right),
\]

is substituted into equation 41, we obtain for the film width, after some manipulation

\[
w_F = \frac{3 \dot{m}_F \mu_f}{g \rho_f^2 \delta_F^3 \sin \theta}
\]

which, for a vertical surface, becomes:

\[
w_F = \frac{3 \dot{m}_F \mu_f}{g \rho_f^2 \delta_F^3}
\]

Equation 44 agrees with the equation of Hartley and Murgatroyd (33) for the width of a steady laminar-flow film on a vertical surface.
Rewritten in terms of the film thickness as a function of mass flow rate per unit width, equation 44 agrees with the classic Nusselt equation for a laminar film falling under gravity on a vertical surface, equation 9.

Of course, any rivulets formed because of Marangoni-type film instability in the actual reactor case will be flowing on the outer periphery of the calandria tubes rather than on a vertical surface. As with film flow on a horizontal tube, conditions will never be fully developed since the magnitude of the gravity force will vary as the rivulets flow from the top of the tube around to the bottom. The laminar flow behavior of a small, circular-sector rivulet around a horizontal tube, has been analyzed ignoring momentum change effects. For small rivulets, this assumption will be quite valid. For simplicity, we assume that the circular sector cross-section is maintained over the entire tube periphery.

The flow rate per rivulet is then given by:

$$\dot{m}_R = 2 \int_0^\frac{\delta_x}{2} \int_0^\frac{\delta_x}{2} \rho_f u \, dx \, dy$$  \hspace{1cm} (45)$$

where $\delta_x$ is the local thickness of the rivulet at a lateral position $x$.

Following Bankoff (40) and Mikielewicz and Moszynski (37), we assume that the velocity profile in the rivulet at any lateral position $x$ is given by an equation of the form of equation 13, written for the local conditions at $x$:

$$u = \frac{\rho_f g}{\mu_f} \left[ \delta_x \gamma - \frac{\nu^2}{2} \right] \sin \theta$$  \hspace{1cm} (46)
Inserting equation 46 into equation 45 and integrating over y, we obtain:

\[
\hat{m}_R = \frac{2}{3} \frac{\rho_f}{\mu_f} g \frac{W_R}{2} \sin\theta \int_0^\infty \delta^3 \, dx
\]  

(47)

Replacing the variable x by ψ, where ψ is the angle measured from the center-line of the rivulet, substituting for δ in terms of ψ and changing the limits, we obtain:

\[
\hat{m}_R = \frac{2}{3} \frac{\rho_f}{\mu_f} g \int_0^\beta R^4 \sin\theta \sin^4\beta \subset \subset (\cos\psi - \cos\beta)^3 \cos\psi \, d\psi
\]  

(48)

The integral in equation 48 can be shown to be given by:

\[
\frac{f(\beta)}{8} \sin^4\beta
\]

where \( f(\beta) \) is defined by equation 40.

Therefore, the circular-sector, isothermal rivulet flow rate is given by

\[
\hat{m}_R = \frac{1}{12} \frac{\rho_f}{\mu_f} g \int_0^\beta R^4 \sin\theta \sin^4\beta
\]  

(49)

Since \( \hat{m}_R \) must be constant with \( \theta \) and since the property values and contact angle are not functions of \( \theta \), R must vary as the rivulet flows around the tube. Equation 49 shows that the radius, and hence the maximum thickness and the width of circular-sector rivulets will decrease as the rivulets flow around the upper part of the tube (with the mean velocity increasing to compensate), reaching a minimum at \( \theta = \frac{\pi}{2} \), and increasing beyond this
point. Preliminary, qualitative observations of rivulet flows on a horizontal tube, as described later in this report, support this predicted behavior. At $\theta = \frac{\pi}{2}$, i.e., at the 3 and 9 o'clock positions, the rivulet radius is the same as that of a fully-developed rivulet on a vertical surface (e.g., 37). In this respect, the behavior of a laminar circular-sector rivulet on a horizontal tube is similar to that of a laminar film, as might be expected.

We may conclude from this analysis that information on the size and spacing of circular-sector rivulets on vertical surfaces may be used to predict the characteristics at the assumed critical position, $\theta = \frac{\pi}{2}$, of circular-sector rivulets on horizontal tubes.

For wide, flat isothermal rivulets, we may, for now, assume that the minimum width also occurs at the 3 or 9 o'clock positions and use equation 43 with $\theta = \frac{\pi}{2}$ to determine its value. A corollary of this assumption is that wide, flat rivulets, like circular-sector rivulets will become narrower as they flow from the top of the tube around to the 3 or 9 o'clock positions. There will also be a tendency for wide, flat rivulets to approach a circular-sector shape as they flow towards the 3 or 9 o'clock positions. The preliminary experimental observations referred to earlier also support this postulated behavior of wide, flat rivulets.

Experiments show that rippling or wavy flow will occur at very low Reynolds numbers under laminar conditions, in which cases the rivulet widths on inclined or vertical planes would be about twice the values predicted by the foregoing equations (39). Furthermore, should nominal film flow conditions, as given in Table 6, prevail, the films would be essentially turbulent so that rivulet flows resulting from the breakdown of such films may be turbulent also.* Therefore, quantitative values of rivulet widths calculated from the above equations may not be very realistic.

Bankoff (40) developed a simplified analysis for the breakdown of an isothermal laminar falling film on an inclined plane into circular-sector rivulets by equating energies and mass flow rates in the film and

* Should very small rivulets form, turbulence effects may be damped out, so that very small rivulets formed from turbulent films may be laminar.
the rivulets. Implicit in his analysis is the assumption that the rivulets, when formed, still cover the entire surface of the plane, i.e., that the surface continues to be covered by liquid flow with no dry patches. The film is, in effect, now a "corrugated" film. It can be shown that the analysis of Bankoff leads to exactly the same expression for a circular-sector rivulet width as that given by Towell and Rothfeld for a single circular-sector rivulet, equation 39.

Bankoff's predictions for the critical isothermal film thickness which results in film instability and breakdown into rivulets are considerably lower than critical film thicknesses predicted by others. For example, for water at 100°C on a vertical plate he predicts a critical film thickness of about 36 μm, which can be compared to the values of about 200 μm given in Table 8 for water at 80°C from the predictions of Hartley and Murgatroyd (33) and Fujita and Ueda (27). However, Mikielewicz and Moszynski (37) point out that the implicit assumption of Bankoff that the rivulets cover the entire surface cannot be valid and that the only reason that Bankoff obtained results, albeit inexplicably low ones, for the critical film thickness was because of a numerical error in the integration of the energy equation for a circular-sector rivulet.* Mikielewicz and Moszynski developed an analysis for the breakdown of an isothermal film into circular-sector rivulets on a vertical surface which allows for the spacing between rivulets by assuming, following Hobler (42), that the energy of the rivulet system would have a minimum at this spacing. They also allowed for surface energy at the liquid-solid interface and the gas-solid interface.

Their results for the non-dimensional critical isothermal laminar film thickness and for the relative width of the vertical surface covered by circular-sector rivulets after film breakdown are given as functions of contact angle in Figure 11, which is taken from Figure 1 of their paper. The non-dimensional critical film thickness is defined by:

\[ \delta^+ = \delta_c \left( \frac{g^2 \rho_f^3}{15 \mu_l^2 \sigma g_c} \right)^{0.2} \]  

(50)

* Note, however, that the prediction of Bankoff for the width of a single circular-sector rivulet for a given rivulet flow rate is not affected by this error.
Also shown in Figure 11 are the non-dimensional critical film thickness predictions of Hartley and Murgatroyd (33) and Hobler (42). Figure 12 shows that the approach of Mikielewicz and Moszynski predicts a somewhat more stable film than that predicted by the other methods. For a contact angle of $45^\circ$, as used previously, the non-dimensional critical film thickness from Figure 11 is $\delta_c^+ = 0.62$, which results in an actual critical film thickness of 164 $\mu$m, which is less than the values given in Table 8, as expected. Figure 11 also shows that the relative surface covered by rivulets is quite insensitive to contact angle. For $\beta = 45^\circ$, this value is:

$$X_o = \frac{w_R}{s_R} = 0.38$$

From these values for $\delta_c^+$ and $X_o$ and from equations given by Mikielewicz and Moszynski, the isothermal rivulet radius, width, spacing and maximum thickness can be readily determined. For a contact angle of $\beta = 45^\circ$, these are:

- $R_R = 984$ $\mu$m
- $w_R = 1392$ $\mu$m
- $s_R = 3664$ $\mu$m
- $\delta_R = 288$ $\mu$m

These results indicate that the rivulets following isothermal film breakdown will be quite closely spaced, about 3.7 mm center to center, and the rivulets themselves will be quite narrow, about 1.4 mm wide. These results imply that, if film breakdown does occur, the calandria tube would still be well-cooled (particularly NPD with its aluminum calandria tubes).

However, the analysis does not take into account such effects as the meandering of rivulet flows, rewetting of dry patches, coalescence of rivulets, etc. Also, based on the behavior of dry patches, we have postulated that rivulet flow characteristics are not very sensitive to
Marangoni effects. However, with Marangoni effects leading to premature film breakdown, the resulting rivulet flow pattern may be quite different than that predicted above. Indeed, because of the high rivulet flow rates resulting from this premature breakdown, we would expect from the work of Towell and Rothfeld that the rivulets would tend to be wide, flat ones rather than circular-sector ones. Certainly, the flow on a vertical tube resulting from premature film breakdown caused by Marangoni effects appears to resemble more the flow of wide, flat rivulets than that of circular-sector ones, as shown by Fujita and Ueda (27). Therefore an investigation of the possible breakdown behavior of a laminar falling film on a vertical surface was undertaken. In this investigation, it was assumed that a given laminar film flow on a vertical surface breaks down into a series of wide, flat rivulets. Following the approach of Mikielewicz and Moszynski (37), and Bankoff (40), mass and energy balances were written for the original film and the resulting flat rivulets. The simplified method of analysis ignores the transition region from the film flow to the fully-developed rivulet flow and also ignores edge-effects in the rivulets (i.e., contact angle effects at the lateral edges of the rivulets).

For the flow configuration shown in Figure 13, a mass balance on the film and rivulet flows yields:

\[ s_F \int_0^{\delta_F} \rho_f u \, dy = w_F \int_0^{\delta_F} \rho_f u \, dy \quad (51) \]

Similarly, an energy balance yields:

\[ s_F \int_0^{\delta_F} \rho_f u^2 \, dy + \sigma s_F = w_F \int_0^{\delta_F} \rho_f u^2 \, dy + \sigma w_F \quad (52) \]

Using equation 13 for the velocity distribution in a laminar falling film on a vertical surface (which satisfies conservation of momentum), we
obtain from equation 51:

\[ \delta_p = \delta \left( \frac{s_p}{w_F} \right)^{1/3} \]  \hspace{1cm} (53)

Substituting equation 13 for a vertical surface into equation 52 and using equation 53, we obtain the following equation for \( s_F/w_F \):

\[ \left( \frac{s_F}{w_F} \right)^{5/3} - \frac{\phi + \sigma}{\phi} \left( \frac{s_F}{w_F} \right) + \frac{\sigma}{\phi} = 0 \]  \hspace{1cm} (54)

where \[ \phi = \frac{\rho_f}{15} \left( \frac{\rho_f}{\mu_f} \right)^2 \frac{\delta_5}{g_c} \]  \hspace{1cm} (55)

Iterative procedures were used to obtain physically-real, non-trivial solutions to equation 54 for \( w_F/s_F \), the ratio of the rivulet width to spacing, for a range of initial film thicknesses. The results are shown in Figure 13 for the relevant NPD conditions.

Figure 13 indicates that there would not be a stable configuration of wide, flat rivulets for initial film thicknesses greater than about 287 \( \mu m \). Because of the approximate nature of the analysis and the many uncertainties in the application of the analysis to horizontal tubes under NPD conditions, we cannot know whether this result is valid. However, it does suggest that there may be an initial film thickness above which breakdown of the film into a stable flat rivulet pattern would not occur. If confirmed, this suggests that while Marangoni effects will tend to lead to film breakdown, for film thicknesses above a certain critical value the resulting rivulet flow would not be stable and there would be a tendency to re-form the film. Note that this predicted critical film thickness for NPD conditions is less than that for the nominal operating conditions, although above that for the arbitrarily assumed minimum conditions.
However, it should be recognized that should a dry patch of sufficient width form momentarily, its temperature rise may be high enough to prevent effective re-wetting.

For initial film thicknesses less than this critical value, the predicted stable rivulet width-to-spacing ratio drops off rapidly, and is about 0.20 for the assumed minimum initial film thickness of 222 microns. Whether cooling of the calandria tube would be adequate with the dry patch fraction of the surface about 80% is dubious, particularly since the resulting surface temperature rise may exceed the rewetting temperature, but, of course, for the reasons given earlier, the validity of the analysis is quite uncertain. Of course, as the initial film thickness decreases, there would be a tendency for the formation of small circular-sector rivulets on breakdown of the film rather than wide, flat rivulets as assumed. Also, the simple model used will become less and less valid as the ratio $w_{f}/s_{f}$ decreases because of the neglect of rivulet edge effects. Therefore, the predictions of the model for small initial film thicknesses are certainly not valid.

In concluding this assessment of rivulet flow, it is apparent that analytical results obtained so far are inconclusive and speculative. Further development of analytical procedures is being undertaken and experiments with a simple test section are in progress, as described in the next section of this report.

3.5 Preliminary Experiment on Stability of Film Flow on Horizontal Tubes

Because of the many uncertainties concerning the stability of film flows on horizontal tubes under non-isothermal conditions, as described in the foregoing sections of this report, a simple experiment has been designed to help define the scope of the problem and to establish whether a more comprehensive experiment is required and, if so, to provide a basis for the design of such an experiment. The apparatus has been installed in the heat transfer laboratory at Carleton University and some initial commissioning tests have been run.
The apparatus as shown in Figure 14, consists of a flow distribution header, a dummy tube, a heated tube and another dummy tube all mounted vertically, in-line. The heater tube is a conventional 1000 W electric resistance heater, 3/4" diameter by 12" long, which provides a maximum heat flux of about 5.5 W/cm². While it would have been preferable to have used a larger diameter heater with a higher maximum heat flux, recognizing the scoping nature of the experiment the availability of an off-the-shelf electrical heater and the avoidance of power supply problems dictated the choice made.

The dummy tubes above and below the heated tube are the same diameter as that of the heater tube, 3/4". The vertical spacing between the tubes was established to provide the same spacing-diameter ratio as that for the calandria tubes in a typical CANDU reactor with dumpable moderator. The function of the dummy tube above the heated tube is to provide a coolant flow onto the heated tube which has characteristics similar to those of actual flows onto calandria tubes. The function of the dummy tube below the heated tube is to permit an assessment of the effects of any film instability, should it occur, on the flow distribution onto a lower tube.

Instrumentation is provided to measure the total cooling film flow rate, the heater power, which is controlled by a rheostat, and heater surface temperatures at several locations* and the water inlet temperature, which can be controlled over a range from 60°F to about 150°F. It is proposed to use high-speed still photographs and movies to study film flow behavior.

Preliminary experiments have been run which have provided some indication of film flow behavior. Typical results are shown in Figure 15, a photograph of the film flow behavior for a heat flux of 5.5 W/cm² and film Reynolds number of 490. In Figure 15, the top tube is the flow distribution header, the second tube is the upper dummy tube, the third tube is the heated tube and the fourth or lowest tube is the lower dummy tube.

The preliminary experiments have shown the need to re-design the flow distribution header to provide a more uniform flow distribution onto the upper dummy tube over the entire flow range. This re-design is now proceeding.

* Thermocouples to provide surface temperature readings have not yet been installed, but their design is proceeding.
4.0 Analysis of Flow Reversal in Vertical Feeder Tubes

Following the initiation of emergency coolant flow into the headers of a CANDU reactor following a LOCA, the emergency coolant must flow into feeder tubes which are essentially vertically-oriented and steam-filled.

A computer program, FESTGEN, was developed to analyze this problem, i.e., the injection of cold emergency coolant into a hot steam-filled vertical feeder tube. The subsequent thermo-hydraulic behavior of the fluids in the feeder is analyzed using a drift-flux model, which permits analysis of counter-current steam-water flows and prediction of flooding and flow reversal. Results obtained with FESTGEN showed the need to improve the model to allow more realistically for liquid and vapor transient mass storage at the different axial position nodes (4).

The model has now been revised to account for these effects, and a modified computer program, FESTGEN-2, has been developed to perform the analysis. The basic logic used in FESTGEN of following a packet of injected liquid down the feeder, i.e. a Lagrangian approach, has been retained in the revised program.

The major modifications incorporated into FESTGEN-2 consist of:

a) The time increment is an independent input variable rather than a dependent variable fixed by the space increment and the volumetric flux density of the liquid. This change permits different volumetric flux densities for the liquid and the vapor.

b) Calculation of the liquid and vapor hold-ups at each node is made by a re-iterative process. The hold-ups are determined by assuming that the relative velocity between the phases is equal to the liquid droplet terminal velocity and is, hence, independent of the phase concentration (43, p.383). The droplet terminal velocity is calculated from Levich's equation for distorted flat drops (44, p.431) based on the maximum radius for stable drops (43, p.381).

c) From the liquid and vapor hold-ups established at each node, the nodal liquid and vapor velocities are established.
d) In the energy balance on the liquid in the node, the energy storage resulting from the change in liquid hold-up from the previous time increment is allowed for.

The new analysis uses the steady-state void fraction relationship of Wallis for droplet type flow in the iteration process. Further work is required to account properly for hold-up effects at the nodes where flow reversal occurs.

Work is now proceeding to complete the FESTGEN-2 program and to de-bug it. A description of the program and typical results obtained by it will be given in a forthcoming topical report.
5.0 Miscellaneous Tasks

A number of miscellaneous tasks within the scope of the contract were undertaken. Descriptions of these tasks, results obtained, conclusions drawn and recommendations made are given in the quarterly progress reports and in various technical memos issued, as referenced in the progress reports. These tasks will merely be listed here, with appropriate references to progress reports given. For further information, see the referenced progress reports and the technical memos cited therein.

a) Assessment of ECI Alternatives (1,4,5,6,7)

b) Assessment of ECI in Existing Reactors and Reactors Under Construction (1,4,5,6,7)

c) Investigation of Long-term ECC (2,4,5,7)
   This investigation led to a proposal submitted to AECB for experimental work in this area. (45)

d) Evaluation of CANDU Fuel Channel Coolability Following a LOCA (2,4)

e) Applicability of LWR ECI Research to CANDU Emergency Cooling (7)

f) Assessment of Three Mile Island Accident with Respect to CANDU ECC Systems

g) Investigation of Criteria for Fuel Break-up in a LOCA or LOR (3,4)

h) Assessment of Propagation of Pressure Tube Failures Following an In-core LOCA (1,2,3,7)

i) Evaluation of Piping Restraint Requirements to Minimize Common Mode Failures Following a Pipe Rupture (3,4,5)

j) Analysis of Thrust Loads of Two-phase Jets Following a Pipe Rupture (3)

k) Evaluation of Effectiveness of Dousing Following a LOCA (3)

l) Development of Analytical Methods for Reactor Building Pressurization Following a LOCA (5,6)
m) Application of 37-element CHF Analysis to Loss of Regulation Accidents (2,3, and additional work in second quarter, 1979)

n) Analysis of Bruce Booster Power Limitations and Regional Overpower Trip Settings (2,3)

o) Assessment of IOWG Approach and Probabilistic Approaches to CANDU Reactor Safety (1,3,4)

p) Reviews of Research and Development Work at WNRE and CRNL Relevant to LOC and LOR Accidents (1,6,7)

q) Attendance at Meetings and Conferences to Present Papers on AECB Work and to Exchange Information on this Work (3,5,6, and CNA Seminar on Radiological Safety in the Nuclear Fuel Cycle, May 1 & 2, 1979, and CNA International Conference, June 11-13, 1979)
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FIG. 1: SAGGED PRESSURE TUBE - NORMAL BUNDEL CONFIGURATION
FIG. 2: SAGGED PRESSURE TUBE - SLUMPED BUNDLE CONFIGURA.
FIG. 4: TEMPERATURES AFTER EOB WITH SLIPPED BUNDLE CONFIGURATION - RENGEN REACTOR.
FIG. 5: MAXIMUM LOCAL HEAT FLUX ON CALANDRIA TUBE AFTER EOB - BRUCE REACTOR
NO PT. SAGGING (SMALL BREAK)

2044 cm² BREAK

T_{FI}

T_{SI}

153 cm² BREAK

T_{PT,MAX.}

T_{PT,MIN.}

T_{CT}

TIME AFTER EOB, MIN.
(q/A) $972.7 \text{ W/cm}^2_{\text{MAX.}}$

LARGE
BREAK
2044 cm$^2$

(q/A) $= 971.7 \text{ W/cm}^2_{\text{MAX.}}$

SMALL
BREAK
153 cm$^2$

TIME AFTER EOB, MIN.

FIG. 7: MAXIMI
FIG. 8: MAXIMUM LOCAL HEAT FLUX ON CALANDRIA TUBE AFTER EOB - DOUGLAS POINT REACTOR. EFFECT OF CONTACT CONDUCTANCE AND STRIP WIDTH
FIG. 9: MAXIMUM FUEL-SHEATH AND PRESSURE TUBE TEMPERATURE. LOECC CASE. BRUCE REACTOR
FIG. 10: DIAGRAM OF FALLING FILM FLOW ON A HORIZONTAL TUBE
FIG. 11: NON-DIMENSIONAL FILM THICKNESS AT ISOTHERMAL BREAKDOWN AND RIVULET AREA COVERAGE. FROM MIKIELEWICZ AND MOSZYNski (37)
FIG. 12: MODEL FOR WIDE, FLAT RIVULET FLOW
FIG. 14: SCHEMATIC DIAGRAM OF APPARATUS FOR FILM STABILITY EXPERIMENT
FIG. 15: FILM FLOW ON HEATED HORIZONTAL TUBE. PRELIMINARY RESULTS.

\[ q/A = 5.5 \, \text{W/cm}^2, \, Re_T = 490 \]
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