Thermal Aspects of High Efficiency Channel with
Conventional and Alternative Fuels in
SuperCritical Water–cooled Reactor (SCWR) Applications

by

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ABSTRACT

Chosen as one of six Generation–IV nuclear-reactor concepts, SuperCritical Water-cooled Reactors (SCWRs) are expected to have high thermal efficiencies within the range of 45 – 50% owing to reactor’s high outlet temperatures. A generic pressure-channel (or pressure-tube)SCWR operates at a pressure of 25 MPa with inlet- and outlet-coolant temperatures of 350°C and 625°C. Consequently, the sheath and fuel centerline temperatures are higher in SCWRs than those of the current nuclear reactors. Previous studies have shown that the sheath and fuel centerline temperatures could exceed the design and industry accepted limits of 850°C and 1850°C, respectively. These studies correspond to UO$_2$ enclosed in a 43-element fuel bundle at an average thermal power per channel of 8.5 MW$_{th}$.

Additionally, these high operating conditions in the range of 350 - 625°C lead to high heat losses from the coolant to the moderator, which in turn reduces the overall thermal efficiency of the Nuclear Power Plant (NPP). Therefore, there is a need for alternative fuels or fuel bundles for future use in SCWRs. Hence, it is also necessary to determine the amount of heat losses from a number of fuel-channel designs for SCWRs.

The objectives of this study are to investigate the possibility of using alternative fuels and to determine the heat losses from a fuel-channel design at SCWR conditions. The investigated fuels are categorized as low thermal-conductivity (e.g., UO$_2$, MOX, and ThO$_2$), high thermal-conductivity (e.g., UC, UC$_2$, UN), and enhanced thermal-conductivity (e.g., UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO) fuels. Additionally, the examined fuel channel is the High Efficiency Channel (HEC), which has been designed by the Atomic Energy of Canada Limited (AECL) for the proposed CANDU SCWR.

In order to achieve the objectives of this study, a steady-state one-dimensional heat-transfer analysis was conducted. The MATLAB© and NIST REFPROP© software were used for programming and retrieving thermophysical properties of a light-water coolant, respectively. The fuel centerline temperature was calculated for the fuel channels with the maximum thermal power, i.e., +15% above average channel power. Results of this analysis showed that the fuel centerline temperatures of low thermal-conductivity fuels exceed the industry limit; therefore, either a fuel with a higher thermal conductivity should be used or the fuel bundle geometry must be modified. Among the high thermal-
conductivity fuels, UC has been shown to be a candidate for future use in SCWRs. However, the chemical compatibility of UC with water at high operating temperatures of SCWRs remains ambiguous. Therefore, further studies are required before selecting UC. In regards to enhanced thermal-conductivity fuels, UO$_2$–BeO is the most suitable candidate; however, its mechanical and neutronic properties must be thoroughly studied before any decision is made with regards to the selection of a fuel.

In regards to the heat losses from the examined fuel channel, the heat loss was between 70 kW and 110 kW per fuel channel based on an average thermal power per channel of 8.5 MW$_{th}$ and a moderator pressure of 0.1 MPa at 80°C. A sensitivity analysis of the fuel channel shows that the heat loss can be reduced by increasing the operating pressure of the moderator, which in turn allows for increasing the operating temperature of the moderator. Higher operating temperatures of the moderator result in smaller temperature differences between the coolant and the moderator, which leads to lower heat losses. Therefore, either the thickness of the insulator or the pressure of the moderator should be increased in order to reduce the heat losses from the fuel channel.

Keywords: Supercritical, SCWR, Fuel Centerline Temperature, Enhanced Thermal-Conductivity Fuels, High Thermal-Conductivity Fuels, High Efficiency Channel, Heat Loss.
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SUMMARY

SuperCritical Water-cooled Reactors (SCWRs) will have higher thermal efficiencies compared to the current conventional nuclear reactors due to higher reactor outlet temperatures. The outlet temperature of the coolant in SCWRs will be approximately twice (i.e., 625°C) as high as those of the current light water reactors (i.e., 315°C). Consequently, the sheath and fuel centerline temperatures will be higher in SCWRs. Therefore the possibility of using various fuel options and fuel bundles in SCWRs must be investigated. Additionally, these high operating temperatures impose challenges on the design of a new fuel channel in terms of the operating temperatures of the components of the fuel channel, the material choice, and the heat loss from the fuel channel to the moderator. As a result, it is necessary to analyze the primary fuel channel design in terms of the operating temperatures of its components and the heat loss from the fuel channel to the moderator.

This research focuses on the thermal aspects of the High Efficiency Channel (HEC) design with conventional and alternative fuels in SuperCritical Water-cooled Reactor (SCWR) applications. The possibility of using three categories of fuels was investigated. The examined fuels are low thermal-conductivity (e.g., UO$_2$, MOX, and ThO$_2$), high thermal-conductivity (e.g., UC, UC$_2$, UN), and enhanced thermal-conductivity (e.g., UO$_2$–SiC, UO$_2$–C, and UO$_2$–Be) fuels.

There are two temperature limits that a fuel and a fuel bundle design must comply with. Firstly, the fuel centerline temperature must be below the industry accepted limit of 1850°C under all normal operating conditions. Secondly, the sheath temperature must be below a design temperature limit of 850°C. As a result, the sheath and fuel centerline temperatures were calculated at four Axial Heat Flux Profiles (AHFPs), namely, upstream-skewed cosine, cosine, downstream-skewed cosine, and uniform. These heat flux profiles were determined based on a 43-element fuel bundle (i.e., Variant-20) and a maximum thermal power per channel of 9.8 MW$_{th}$, which is 15% above the average thermal power per channel.

In order to meet the objectives of this study, a steady-state one-dimensional heat-transfer analysis was conducted to calculate the sheath and fuel centerline temperatures. The
MATLAB and NIST REFPROP software were used for programming and retrieving the thermophysical properties of a light-water coolant, respectively.

The thermal-hydraulic analysis presented in this study can be adopted for analyzing both pressure vessel and pressure channel reactors. In other words, the results are applicable to different reactor types. The design independency of the aforementioned methodology and analysis has been made possible because the calculations have been conducted based on the hydraulic diameter, mass flux of the coolant, and thermal power per fuel assembly or fuel bundle. Additionally, the heat transfer coefficient has been calculated based on a correlation, which has been developed based on data obtained from experiments in a bare tube. As a result, the correlation is independent of any bundle design. Furthermore, the effects of the appendages within the fuel assembly or fuel bundle, which results in the turbulization of the coolant, have not been taken into consideration. Moreover, the developed thermal-hydraulics code takes into account the effects of the fuel-sheath gap on the fuel centerline temperature. Thus, this methodology can be considered as a conservative approach for the calculation of the sheath and fuel centerline temperatures of various reactor types.

Results of this analysis showed that the fuel centerline temperatures of low thermal-conductivity fuels exceed the industry limit. The maximum fuel centerline temperatures were 2719°C, 2767°C, and 2433°C for UO$_2$, MOX, and ThO$_2$, respectively, when a fuel-sheath gap of 20 μm was taken into account. Additionally, the sheath temperature reached a maximum of approximately 900°C at the downstream-skewed. Therefore, either a fuel with a higher thermal conductivity should be used or the fuel bundle geometry must be modified.

All the examined high thermal-conductivity fuels showed significantly lower fuel centerline temperatures than the low thermal-conductivity fuels. As a result, other factors such as the volumetric swelling, chemical stability, and thermal-shock resistance of these high thermal-conductivity fuels were considered in order to determine the best fuel options. The comparison showed that UC is a promising candidate for future use in SCWRs. However, the chemical compatibility of UC with water at high operating temperatures of SCWRs remains ambiguous. Therefore, further studies are required.
before selecting UC. In regards to enhanced thermal-conductivity fuels, UO$_2$–BeO is the most suitable candidate. However, it should be noted that enhanced thermal-conductivity fuels are under development and there is not enough information available in regards to their properties and behaviour under irradiation. Therefore, the mechanical and neutronic properties of UO$_2$–BeO must be thoroughly studied before the final decision is made in regards to the selection of a fuel.

Additionally, the thermal aspects of the High Efficiency Channel design, which consists of a pressure tube, ceramic insulator, and liner tube, was analyzed. For instance, the heat loss from the fuel channel to the moderator and the operating temperatures of its components were determined. The heat loss from the HEC design was between 70 and 110 kW per fuel channel based on an average thermal power per channel of 8.5 MW$_{th}$ and a moderator pressure of 0.1 MPa. Furthermore, the effects of the insulator thickness and the pressure of the moderator on the heat loss were investigated. The results showed that the heat loss can be reduced by increasing the thickness of the insulator or the operating pressure of the moderator. Higher operating pressures will allow operation of the moderator at higher temperatures while preventing occurrence of boiling. Consequently, higher moderator temperatures will results in a lower temperature difference between the coolant and the moderator, hence reducing the heat sink from the coolant to the moderator. Therefore, either the thickness of the insulator or the pressure of the moderator should be increased in order to reduce the heat losses from the fuel channel.
# Table of Contents

1  **INTRODUCTION** ......................................................................................................................... 1

2  **LITERATURE SURVEY** .............................................................................................................. 12

   2.1  Heat Transfer at Supercritical Conditions .............................................................................. 12

   2.2  Heat-Transfer Correlations ..................................................................................................... 18

   2.3  Specifications of Generic 1200-MW<sub>e</sub> PT SCWR ............................................................ 27

   2.4  Thermal Cycles ......................................................................................................................... 28

   2.5  Fuel Channel Designs ................................................................................................................ 30

       2.5.1  High-Efficiency Fuel Channel ......................................................................................... 30

       2.5.2  Re-Entrant Fuel Channels ............................................................................................... 31

   2.6  Fuel-Bundle Designs .................................................................................................................. 33

   2.7  Nuclear Fuels ............................................................................................................................ 36

       2.7.1  Low Thermal-Conductivity Fuels: UO<sub>2</sub>, MOX, and ThO<sub>2</sub> ..................................... 39

       2.7.2  High Thermal-Conductivity Fuels: UC, UC<sub>2</sub>, and UN ............................................. 43

       2.7.3  Composite Fuels with Enhanced Thermal-Conductivity .................................................. 65

3  **METHODS** ................................................................................................................................... 71

   3.1  Fuel Centerline Temperature Calculations ............................................................................... 71

       3.1.1  Bulk-Fluid Temperature Profile ......................................................................................... 73

       3.1.2  Sheath Temperature ............................................................................................................ 75

       3.1.3  Gap Conductance ................................................................................................................ 80

       3.1.4  Fuel Centerline Temperature .............................................................................................. 83

   3.2  Heat-Loss Calculations .............................................................................................................. 83

       3.2.1  Thermal Resistance of Coolant .......................................................................................... 84

       3.2.2  Thermal Resistance of Ceramic Insulator ......................................................................... 85

       3.2.3  Thermal Resistance of Pressure Tube ............................................................................... 89

       3.2.4  Thermal Resistance of Moderator ..................................................................................... 90

4  **RESULTS** ..................................................................................................................................... 91

   4.1  Fuel Centerline and Sheath Temperatures ............................................................................... 91

   4.2  Heat Loss from High Efficiency Channel .............................................................................. 100

5  **DISCUSSION** .............................................................................................................................. 105
5.1 Fuel Centerline and Sheath Temperatures .......................................................... 105
5.2 Heat Loss from High Efficiency Channel .......................................................... 114
6 CONCLUSION AND RECOMMENDATIONS ....................................................... 117
7 FUTURE STUDIES ................................................................................................. 120
REFERENCES ........................................................................................................... 121
Appendix A – Modeling Fluids .................................................................................. 131
Appendix B – Effect of Lanthanum (La) on Thermal Conductivity of ThO₂ .............. 132
Appendix C – MATLAB Code for Fuel Centerline Calculation ................................. 133
Appendix D – Verification of MATLAB Code ............................................................ 144
Appendix F – Fuel Centerline Temperature Profiles ................................................. 152
Appendix G – W. Peiman Publications .................................................................... 171
Appendix H – W. Peiman Conference Attendance ..................................................... 175
Appendix I – W. Peiman Awards and Honours ......................................................... 176
LIST OF FIGURES

<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>Schematic diagram of Gas-Cooled Fast Reactor.</td>
<td>2</td>
</tr>
<tr>
<td>1.2</td>
<td>Schematic diagram of Molten Salt Reactor.</td>
<td>3</td>
</tr>
<tr>
<td>1.3</td>
<td>Schematic diagram of Lead-Cooled Fast Reactor.</td>
<td>4</td>
</tr>
<tr>
<td>1.4</td>
<td>Schematic diagram of Sodium-Cooled Reactor.</td>
<td>5</td>
</tr>
<tr>
<td>1.5</td>
<td>Schematic diagram of Very High Temperature Reactor.</td>
<td>6</td>
</tr>
<tr>
<td>1.6</td>
<td>Schematic diagram of PV SCWR.</td>
<td>7</td>
</tr>
<tr>
<td>1.7</td>
<td>Schematic diagram of SCW CANDU.</td>
<td>9</td>
</tr>
<tr>
<td>2.1</td>
<td>Specific heat of water as a function of pressure and temperature.</td>
<td>12</td>
</tr>
<tr>
<td>2.2</td>
<td>Density of water at 22.064 and 25 MPa.</td>
<td>13</td>
</tr>
<tr>
<td>2.3</td>
<td>Specific heat of water at 22.064 and 25 MPa.</td>
<td>14</td>
</tr>
<tr>
<td>2.4</td>
<td>Enthalpy of water at 22.064 and 25 MPa.</td>
<td>14</td>
</tr>
<tr>
<td>2.5</td>
<td>Thermal conductivity of water at 22.064 and 25 MPa.</td>
<td>15</td>
</tr>
<tr>
<td>2.6</td>
<td>Dynamic viscosity of water at 22.064 and 25 MPa.</td>
<td>15</td>
</tr>
<tr>
<td>2.7</td>
<td>Density of Water at 7, 11, and 15 MPa.</td>
<td>16</td>
</tr>
<tr>
<td>2.8</td>
<td>Operating parameters of several reactors.</td>
<td>18</td>
</tr>
<tr>
<td>2.9</td>
<td>Generic channel layout of a 1200-MWe PT SCWR.</td>
<td>28</td>
</tr>
<tr>
<td>2.10</td>
<td>Single-reheat cycle for SCW NPP.</td>
<td>29</td>
</tr>
<tr>
<td>2.11</td>
<td>3-Dimensional view of High Efficiency fuel channel.</td>
<td>31</td>
</tr>
<tr>
<td>2.12</td>
<td>3-Dimensional view of Re-Entrant fuel channel.</td>
<td>32</td>
</tr>
<tr>
<td>2.13</td>
<td>3-Dimensional view of Re-Entrant fuel channel with gaseous</td>
<td>32</td>
</tr>
<tr>
<td></td>
<td>insulator.</td>
<td></td>
</tr>
<tr>
<td>2.14</td>
<td>3-Dimensional view of Re-Entrant fuel channel with ceramic</td>
<td>33</td>
</tr>
<tr>
<td></td>
<td>insulator.</td>
<td></td>
</tr>
<tr>
<td>2.15</td>
<td>Comparison of fuel-bundle geometries: (a) 37-element, (b) 43-element, (c) 34</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Variant-18, and (d) Variant-20.</td>
<td></td>
</tr>
<tr>
<td>2.16</td>
<td>A 3-Dimensional view of the Variant-20 fuel bundle design.</td>
<td>36</td>
</tr>
<tr>
<td>2.17</td>
<td>Thermal conductivity of several fuels.</td>
<td>39</td>
</tr>
<tr>
<td>2.18</td>
<td>Thermal conductivity of ThO$_2$.</td>
<td>43</td>
</tr>
<tr>
<td>2.19</td>
<td>Thermal conductivity of UC resulted from various correlations.</td>
<td>46</td>
</tr>
<tr>
<td>2.20</td>
<td>Thermodynamic Properties of UC as Function of Temperature.</td>
<td>47</td>
</tr>
</tbody>
</table>
Figure 2.21: Volumetric Swelling of UC as Function of Temperature and Burn-up. ......48
Figure 2.22: Creep Rate of UC as Function of Temperature. ........................................49
Figure 2.23: Thermal conductivity for stoichiometric, slightly hypo-stoichiometric, and hypo-stoichiometric UC₂ as function of temperature.................................................................52
Figure 2.24: Thermal diffusivity for stoichiometric, slightly hypo-stoichiometric, and hypo-stoichiometric UC₂ as function of temperature..............................52
Figure 2.25: Specific heat of UC₂ as a function of temperature. ..................................54
Figure 2.26: Vapor pressure of nitrogen as function of temperature. ............................56
Figure 2.27: Vapor pressure of uranium as function of temperature. .............................56
Figure 2.28: Melting point of UN as function of partial pressure of nitrogen. ..............57
Figure 2.29: Thermal conductivity of 95%TD UN fuel..................................................58
Figure 2.30: Thermodynamic properties of UN as function of temperature. ...............60
Figure 2.31: Young's and Shear moduli of UN with 95% TD as function of temperature .................................................................................................................................61
Figure 2.32: Linear thermal expansion of UN as function of temperature. .................63
Figure 2.33: Percent volumetric swelling of UN as function of burn-up and temperature ..........................................................................................................................63
Figure 2.34: Steady-State Creep Rate of UN at 25MPa stress as function of temperature ..........................................................................................................................64
Figure 2.35: Thermal conductivity of UO₂ as function of graphite fibber volume percent .........................................................................................................................68
Figure 2.36: Thermal conductivity of UO₂–BeO as function of temperature.................70
Figure 3.1: Fuel centerline temperature calculations..........................................................72
Figure 3.2: Power ratios for fuel channel with average channel power......................74
Figure 3.3: AHFPs for fuel channel with maximum power............................................75
Figure 3.4: Uncertainty in predicting HTC for the Mokry et al. correlation. ..............76
Figure 3.5: Uncertainty in predicting wall temperature using the Mokry et al. correlation. ..................................................................................................................77
Figure 3.6: Thermophysical properties of light-water coolant as function of temperature.....................................................................................................................79
Figure 3.7: Sheath thickness as function of temperature. ..................................................80
Figure 3.8: Thermal resistance network of High Efficiency Channel. ..........................84
Figure 3.9: Methodology for calculation of heat loss from High Efficiency Channel. .................................................................................................................................84
Figure 3.10: Thermal conductivity of solid YSZ as function of temperature..............86
Figure 3.11: Effective thermal conductivity of 70% porous YSZ as function of temperature.................................................................................................................................89
Figure 4.1: Temperature and HTC profiles for UO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................93
Figure 4.2: Temperature and HTC profiles for MOX fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................93
Figure 4.3: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................94
Figure 4.4: Temperature and HTC profiles for UC fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................94
Figure 4.5: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................95
Figure 4.6: Temperature and HTC profiles for UN fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................95
Figure 4.7: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................96
Figure 4.8: Temperature and HTC profiles for UO$_2$–C fuel at maximum channel power with downstream-skewed cosine AHFP. .................................................................96
Figure 4.9: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with downstream-skewed cosine AHFP.........................................................97
Figure 4.10: Maximum sheath and fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO based on a 20–μm fuel-sheath gap width.........................................................................................97
Figure 4.11: HTC and sheath-wall temperature profiles as function of AHFP. ..........98
Figure 4.12: Maximum fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO as functions of fuel-sheath gap width.................................................................99

4.13: Heat loss profile along heated length of High Efficiency Channel with 7-mm of 3 mole % YSZ with 70% porosity .................................................................101

Figure 4.14: 3-D view of a CANDU-6 type fuel channel .............................................103

Figure 4.15: Heat loss profile of modified CANDU-6 fuel channel at SCWR conditions .........................................................................................................................103

Figure 4.16: Optimum thickness of ceramic insulator .....................................................104

Figure 4.17: Heat loss profile of modified CANDU-6 fuel channel at SCWR conditions ....103

Figure 5.1: Heat loss profiles of High Efficiency Channel as function of insulator thickness .................................................................................................................114

Figure 5.2: Heat loss profile from High Efficiency Channel as function of moderator pressure .................................................................116

Figure 5.3: Temperature differences and absolute temperatures of ceramic insulator and pressure tube ...........................................................................................................116

Figure B.1: Thermal conductivity of ThO$_2$ as a function of percent lanthanum. ..........132

Figure D.1: Comparison of coolant, sheath, and fuel centerline temperature profiles of MATLAB and EXCEL calculations .........................................................144

Figure F.1: Temperature and HTC profiles for UO$_2$ Fuel at maximum channel power with uniform AHFP ......................................................................................152

Figure F.2: Temperature and HTC profiles for UO$_2$ fuel at maximum channel power with upstream-skewed cosine AHFP .................................................152

Figure F.3: Temperature and HTC profiles for UO$_2$ fuel at maximum channel power with cosine AHFP .................................................................153

Figure F.4: Temperature and HTC profiles for UO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP ........................................153

Figure F.5: Temperature and HTC profiles for MOX fuel at maximum channel power with uniform AHFP .................................................................154

Figure F.6: Temperature and HTC profiles for MOX fuel at maximum channel power with upstream-skewed cosine AHFP ........................................154
Figure F.7: Temperature and HTC profiles for MOX fuel at maximum channel power with cosine AHFP ................................................................. 155
Figure F.8: Temperature and HTC profiles for MOX fuel at maximum channel power with downstream-skewed cosine AHFP. ................................. 155
Figure F.9: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with uniform AHFP......................................................... 156
Figure F.10: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with upstream-skewed cosine AHFP. ................................. 156
Figure F.11: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with cosine AHFP. ......................................................... 157
Figure F.12: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP. ................................. 157
Figure F.13: Temperature and HTC profiles for UC fuel at maximum channel power with uniform AHFP......................................................... 158
Figure F.14: Temperature and HTC profiles for UC fuel at maximum channel power with upstream-skewed cosine AHFP. ................................. 158
Figure F.15: Temperature and HTC profiles for UC fuel at Maximum Channel Power with Cosine AHFP. ................................................................. 159
Figure F.16: Temperature and HTC profiles for UC fuel at maximum channel power with downstream-skewed cosine AHFP. ................................. 159
Figure F.17: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with uniform AHFP......................................................... 160
Figure F.18: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with upstream-skewed cosine AHFP. ................................. 160
Figure F.19: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with cosine AHFP. ......................................................... 161
Figure F.20: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP. ................................. 161
Figure F.21: Temperature and HTC profiles for UN fuel at maximum channel power with uniform AHFP......................................................... 162
Figure F.22: Temperature and HTC profiles for UN fuel at maximum channel power with upstream-skewed cosine AHFP ................................................................. 162

Figure F.23: Temperature and HTC profiles for UN fuel at maximum channel power with cosine AHFP ...................................................................................... 163

Figure F.24: Temperature and HTC profiles for UN fuel at maximum channel power with downstream-skewed cosine AHFP ............................................. 163

Figure F.25: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with uniform AHFP .......................................................... 164

Figure F.26: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with upstream-skewed cosine AHFP ................................. 164

Figure F.27: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with cosine AHFP ........................................................................ 165

Figure F.28: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with downstream-skewed cosine AHFP ................................ 165

Figure F.29: Temperature and HTC profiles for UO$_2$–C fuel at maximum channel power with uniform AHFP ......................................................... 166

Figure F.30: Temperature and HTC profiles for UO$_2$–C fuel at maximum channel power with upstream-skewed cosine AHFP ................................. 166

Figure F.31: Temperature and HTC profiles for UO$_2$–C fuel at maximum channel power with cosine AHFP ............................................................... 167

Figure F.32: Temperature and HTC profiles for UO$_2$–C fuel at maximum channel power with downstream-skewed cosine AHFP ................................. 167

Figure F.33: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with uniform AHFP ..................................................... 168

Figure F.34: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with upstream-skewed cosine AHFP ................................. 168

Figure F.35: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with cosine AHFP .......................................................... 169

Figure F.36: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with downstream-skewed cosine AHFP ................................. 169
LIST OF TABLES

Table 1.1: Key design parameters of Gas-Cooled Fast Reactor. ........................................... 2
Table 1.2: Key design parameters of Molten Salt Reactor. ..................................................... 3
Table 1.3: Key design parameters of Lead-Cooled Fast Reactor. ........................................... 4
Table 1.4: Key design parameters of Sodium Cooled Reactor. .............................................. 5
Table 1.5: Key design parameters of Very High Temperature Reactor. ............................. 6
Table 1.6: Key design parameters of SuperCritical Water-cooled Reactor. ......................... 8
Table 2.1: Average and RMS errors in predicting HTC in supercritical sub-regions. ......... 20
Table 2.2: List of Nusselt number correlations in Table 2.1................................................... 22
Table 2.3: Validity ranges of Nusselt number correlations. .................................................... 25
Table 2.4: Operating parameters of generic PT SCWR.......................................................... 27
Table 2.5: Summary of element diameters in bundle designs. ............................................. 34
Table 2.6: Experimental conditions on a full-scale R-134a................................................... 35
Table 2.7: Basic properties of selected fuels at 0.1 MPa and 25°C. ..................................... 38
Table 4.1: Maximum fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO with fuel-sheath gap widths of 0, 20 $\mu$m, and 36 $\mu$m. .................................................................................................................. 100
Table 4.2: Total heat loss per fuel channel based on examined equations. ......................... 101
Table 5.1: Neutronic properties of UO$_2$, UC, and UN......................................................... 112
Table 5.2: Issues related to UO$_2$, UN, and UC fuels........................................................... 113
Table A.1: Scaling parameters for fluid-to-fluid modeling at supercritical conditions . .......................................................... 131
Table F.1: Maximum sheath and fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO based on a 20-$\mu$m fuel-sheath gap width .................................................................................................................. 170
**NOMENCLATURE**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$A$</td>
<td>cross-sectional area, m$^2$</td>
</tr>
<tr>
<td>$A_{fl}$</td>
<td>flow area, m$^2$</td>
</tr>
<tr>
<td>$B$</td>
<td>burn-up, MW day/Mg(U)</td>
</tr>
<tr>
<td>$c_p$</td>
<td>specific heat at constant pressure, J/kg K</td>
</tr>
<tr>
<td>$\bar{c}_p$</td>
<td>average specific heat, $(\frac{H_w-H_b}{T_w-T_b})$, J/kg K</td>
</tr>
<tr>
<td>$D$</td>
<td>diameter, m</td>
</tr>
<tr>
<td>$D_{hy}$</td>
<td>hydraulic-equivalent diameter, m</td>
</tr>
<tr>
<td>$E$</td>
<td>Young’s modulus, MPa</td>
</tr>
<tr>
<td>$f$</td>
<td>friction factor</td>
</tr>
<tr>
<td>$G$</td>
<td>Gibb’s free energy, J/kg</td>
</tr>
<tr>
<td>$G$</td>
<td>mass flux, (m/$A_{fl}$), kg/m$^2$s</td>
</tr>
<tr>
<td>$H$</td>
<td>enthalpy, J/kg</td>
</tr>
<tr>
<td>$H$</td>
<td>Mayer hardness, MPa</td>
</tr>
<tr>
<td>$h$</td>
<td>heat transfer coefficient, W/m$^2$K</td>
</tr>
<tr>
<td>$k$</td>
<td>thermal conductivity, W/m K</td>
</tr>
<tr>
<td>$L$</td>
<td>length, m</td>
</tr>
<tr>
<td>$m$</td>
<td>mass flow rate, kg/s</td>
</tr>
<tr>
<td>$P$</td>
<td>pressure, Pa</td>
</tr>
<tr>
<td>$P$</td>
<td>porosity, %</td>
</tr>
<tr>
<td>$p$</td>
<td>heated perimeter, m</td>
</tr>
<tr>
<td>$Q$</td>
<td>heat transfer rate, W</td>
</tr>
<tr>
<td>$q$</td>
<td>heat flux, W/m$^2$</td>
</tr>
<tr>
<td>$q^*$</td>
<td>non-dimensional heat flux number, $(q/G) (\beta/c_p)$</td>
</tr>
<tr>
<td>$Q_{gen}$</td>
<td>volumetric heat generation, W/m$^3$</td>
</tr>
<tr>
<td>$R$</td>
<td>gas constant, cal/K mol</td>
</tr>
<tr>
<td>$R$</td>
<td>thermal resistance, W/K</td>
</tr>
<tr>
<td>$R'$</td>
<td>thermal-shock resistance, W/m</td>
</tr>
<tr>
<td>$T$</td>
<td>temperature, K</td>
</tr>
</tbody>
</table>
\( t \)  
temperature, °C

**Greek symbols**

\( \alpha \)  
coefficient of thermal expansion, 1/K

\( \alpha \)  
thermal diffusivity, \((k/\rho c_p)\), m\(^2\)/s

\( \beta \)  
thermal expansion coefficient, 1/K

\( \dot{\varepsilon} \)  
creep rate, 1/h

\( \mu \)  
dynamic viscosity, kg/m s

\( v \)  
kinematic viscosity, m\(^2\)/s

\( \nu \)  

\( \nu \)  

\( \rho \)  
density, kg/m\(^3\)

\( \sigma \)  
stress, Pa

\( \sigma_F \)  
modulus of rupture, MPa

**Non-dimensional numbers**

\( \text{Gr}^* \)  
modified Grashof number based on \( q \), \( \text{Gr}^* = \frac{g\beta qD_h^4}{\nu^2} \)

\( \text{Nu}_D \)  
Nusselt number, \( \text{Nu}_D = \frac{hD}{k} \)

\( \text{Pr} \)  
Prandtl Number, \( \text{Pr} = \frac{\mu c_p}{k} \)

\( \overline{\text{Pr}} \)  
average Prandtl Number, \( \overline{\text{Pr}} = \frac{\mu \bar{c}_p}{k} \)

\( \text{Ra}_D \)  
Rayleigh number, \( \text{Re}_D = \frac{g\beta(T_w-T_b)D_h^3}{\nu} \)

\( \text{Re}_D \)  
Reynolds number, \( \text{Re}_D = \frac{G D_{hy}}{\mu} \)

**Subscripts**

atm  

atmospheric

b  

properties calculated at bulk fluid temperature

cond  

conduction

conv  

convection
eff  effective
i  inner
in  inlet
m  melting
o  outer
pc  pseudocritical point
vol  volume
w  properties calculated at wall temperature
wt  weight

Abbreviations

AECL  Atomic Energy of Canada Limited
AGR  Advanced Gas-cooled Reactor
AHFP  Axial Heat Flux Profile
BCT  Body-Centered Tetragonal
BWR  Boiling Water Reactor
CANDU  CANada Deuterium Uranium
CLT  Centerline Temperature
FCC  Face-Centered Cubic
GFR  Gas-cooled Fast Reactor
GIF  Generation IV International Forum
HTC  Heat Transfer Coefficient
HTR  High Temperature Reactor
IAEA  International Atomic Energy Agency
LFR  Lead-cooled Fast Reactor
LOCA  Loss Of Coolant Accident
LWR  Light Water Reactor
MSR  Molten Salt Reactor
NIST  National Institute of Standards and Technology (USA)
NPP  Nuclear Power Plant
PCh  Pressure Channel
PT Pressure Tube
PV Pressure Vessel
RBMK Russian Acronym for Reactor of High-Power Channel-type
RTD Resistance Temperature Detector
SCW SuperCritical Water
SCWR SuperCritical Water-cooled Reactor
SFR Sodium-cooled Fast Reactor
SHR Steam Re-Heat
TD Theoretical Density
UC Uranium Carbide
UC₂ Uranium dicarbide
UN Uranium Nitride
UO₂ Uranium dioxide
UO₂–BeO Uranium dioxide composed of beryllium oxide
UO₂–C Uranium dioxide composed of graphite fibbers
UO₂–SiC Uranium dioxide composed of silicon carbide
VHTR Very High Temperature Reactor
GLOSSARY

The followings are the definitions of selected terms and expressions related to critical and supercritical regions:

*Compressed fluid* is a fluid at a pressure above the critical pressure, but at a temperature below the critical temperature.

*Critical point* (also called a critical state) is a point in which the distinction between the liquid and gas (or vapour) phases disappears, i.e., both phases have the same temperature, pressure and volume or density. The critical point is characterized by the phase-state parameters $T_{cr}$, $P_{cr}$ and $V_{cr}$, which have unique values for each pure substance.

*Deteriorated Heat Transfer* is characterized with lower values of the wall heat transfer coefficient compared to those at the normal heat transfer; and hence has higher values of wall temperature within some part of a test section or within the entire test section.

*Improved Heat Transfer* is characterized with higher values of the wall heat transfer coefficient compared to those at the normal heat transfer; and hence lower values of wall temperature within some part of a test section or within the entire test section. In our opinion, the improved heat-transfer regime or mode includes peaks or “humps” in the heat transfer coefficient near the critical or pseudocritical points.

*Near-critical point* is actually a narrow region around the critical point, where all thermophysical properties of a pure fluid exhibit rapid variations.

*Normal Heat Transfer* can be characterized in general with wall heat transfer coefficients similar to those of subcritical convective heat transfer far from the critical or pseudocritical regions, when are calculated according to the conventional single-phase Dittus-Boelter-type correlations (e.i. $\text{Nu} = 0.0023 \text{Re}^{0.8}\text{Pr}^{0.4}$).

*Pseudocritical point* (characterized with $P_{pc}$ and $T_{pc}$) is a point at a pressure above the critical pressure and at a temperature ($T_{pc} > T_{cr}$) corresponding to the maximum value of the specific heat at this particular pressure.

---

1Based on the book by Pioro and Duffey (2007).
Supercritical fluid is a fluid at pressures and temperatures that are higher than the critical pressure and critical temperature. However, in the present chapter, a term supercritical fluid includes both terms – a supercritical fluid and compressed fluid.

Supercritical steam is actually supercritical water, because at supercritical pressures fluid is considered as a single-phase substance. However, this term is widely (and incorrectly) used in the literature in relation to supercritical “steam” generators and turbines.

Superheated steam is a steam at pressures below the critical pressure, but at temperatures above the critical temperature.
1 INTRODUCTION

The demand for clean, non-fossil based electricity is growing; therefore, the world needs to develop new nuclear reactors with higher thermal efficiency in order to increase electricity generation and decrease the detrimental effects on the environment. The current fleet of nuclear power plants is classified as Generation III or less. However, these models are not as energy efficient as they should be because the operating temperatures are relatively low. Currently, a group of countries, including Canada, have initiated an international collaboration to develop the next generation of nuclear reactors called Generation IV. The ultimate goal of developing such reactors is to increase the thermal efficiency from what is currently in the range of 30 - 35% to 45 - 50%. This increase in thermal efficiency would result in a higher production of electricity compared to current Pressurized Water Reactor (PWR) or Boiling Water Reactor (BWR) technologies.

The Generation IV International Forum (GIF) Program has narrowed design options of the nuclear reactors to six concepts. These concepts are Gas-cooled Fast Reactor (GFR), Very High Temperature Reactor (VHTR), Sodium-cooled Fast Reactor (SFR), Lead-cooled Fast Reactor (LFR), Molten Salt Reactor (MSR), and SuperCritical Water-cooled Reactor (SCWR). Figures 1.1 thorough 1.6 show schematic images of these concepts. These nuclear-reactor concepts differ in their design in aspects such as the neutron spectrum, coolant, moderator, and operating temperature. A brief description of each nuclear-reactor concept has been provided in the following pages.
Gas-cooled Fast Reactor (GFR) is a fast-neutron-spectrum reactor, which can be used for the production of electricity and co-generation of hydrogen through thermochemical processes. The coolant is helium with inlet and outlet temperatures of 490 and 850°C, respectively. The net plant efficiency is 48% with a direct Brayton cycle. Table 1.1 lists a summary of design parameters for GFR (US DOE, 2002).

![Schematic diagram of Gas-Cooled Fast Reactor](image)

**Figure 1.1: Schematic diagram of Gas-Cooled Fast Reactor (US DOE, 2002).**

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>Reference Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Power</td>
<td>MW(_{th})</td>
<td>600</td>
</tr>
<tr>
<td>Coolant Inlet/Outlet Temperature</td>
<td>°C</td>
<td>490/850</td>
</tr>
<tr>
<td>Pressure</td>
<td>MPa</td>
<td>9</td>
</tr>
<tr>
<td>Core Inlet/Outlet Pressure</td>
<td>MPa</td>
<td>Dependent on process</td>
</tr>
<tr>
<td>Coolant Mass Flow Rate</td>
<td>kg/s</td>
<td>320</td>
</tr>
<tr>
<td>Average Power Density</td>
<td>MW(_{th})/m(^3)</td>
<td>100</td>
</tr>
<tr>
<td>Reference Fuel Compound</td>
<td>-</td>
<td>UPuC/SiC(70/30%) with about 20% Pu</td>
</tr>
<tr>
<td>Volume fraction, Fuel, Gas, SiC</td>
<td>%</td>
<td>50/40/10</td>
</tr>
<tr>
<td>Net Plant Efficiency</td>
<td>%</td>
<td>48</td>
</tr>
</tbody>
</table>
Molten Salt Reactor (MSR) is a thermal-neutron-spectrum reactor, which uses a molten fluoride salt with dissolved uranium while the moderator is made of graphite. The inlet temperature of the coolant (e.g., fuel-salt mixture) is 565°C while the outlet temperature reaches 700°C. However, the outlet temperature of the fuel-salt mixture can even increase to 850°C when co-generation of hydrogen is considered as an option. The thermal efficiency of the plant is between 45 and 50%. Table 1.2 lists the design parameters of MSR (US DOE, 2002).

Table 1.2: Key design parameters of Molten Salt Reactor.

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>Reference Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Power</td>
<td>MW&lt;sub&gt;e&lt;/sub&gt;</td>
<td>1000</td>
</tr>
<tr>
<td>Net thermal Efficiency</td>
<td>%</td>
<td>44 - 50</td>
</tr>
<tr>
<td>Average Power Density</td>
<td>MW&lt;sub&gt;e&lt;/sub&gt;/m&lt;sup&gt;3&lt;/sup&gt;</td>
<td>22</td>
</tr>
<tr>
<td>Fuel-Salt Inlet/ Outlet Temperature</td>
<td>ºC</td>
<td>565/700</td>
</tr>
<tr>
<td>Vapor Pressure</td>
<td>kPa</td>
<td>~0.7</td>
</tr>
<tr>
<td>Moderator</td>
<td>-</td>
<td>Graphite</td>
</tr>
<tr>
<td>Neutron Spectrum Burner</td>
<td>-</td>
<td>Thermal-Actinide</td>
</tr>
</tbody>
</table>
Lead-cooled Fast Reactor (LFR) is a fast-neutron-spectrum reactor which uses lead or lead-bismuth as the coolant. The outlet temperature of the coolant is 480 - 567°C at an atmospheric pressure. The primary choice of fuel is a nitride fuel. The Brayton cycle has been chosen as a primary choice for the power cycle while the supercritical Rankine cycle is considered as the secondary choice. Table 1.3 lists several of key design parameters of LFR (OECD Nuclear Energy Agency, 2010; US DOE, 2002).

Table 1.3: Key design parameters of Lead-Cooled Fast Reactor.

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>SSTAR</th>
<th>ELSY</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Power</td>
<td>MWth</td>
<td>19.8</td>
<td>600</td>
</tr>
<tr>
<td>Thermal Efficiency</td>
<td>%</td>
<td>44</td>
<td>42</td>
</tr>
<tr>
<td>Primary Coolant</td>
<td></td>
<td>Lead</td>
<td>Lead</td>
</tr>
<tr>
<td>Coolant Inlet/Outlet Temperature</td>
<td>°C</td>
<td>420/567</td>
<td>400/480</td>
</tr>
<tr>
<td>Peak Cladding Temperature</td>
<td>°C</td>
<td>650</td>
<td>550</td>
</tr>
<tr>
<td>Fuel</td>
<td>-</td>
<td>Nitrides</td>
<td>MOX, (Nitrides)</td>
</tr>
<tr>
<td>Fuel Pin Diameter</td>
<td>mm</td>
<td>25</td>
<td>10.5</td>
</tr>
</tbody>
</table>
Similar to GFR and LFR, Sodium-cooled Reactor (SFR) is a fast-neutron-spectrum reactor. The main objectives of SFR are the management of high-level radioactive waste and production of electricity. SFR uses liquid sodium as its coolant with an outlet temperature between 530 and 550°C at an atmospheric pressure. The primary choices of fuel for SFR are oxide and metallic fuels. Table 1.4 lists a summary of design parameters of SFR (US DOE, 2002).

Table 1.4: Key design parameters of Sodium Cooled Reactor.

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>Reference Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Power</td>
<td>MW&lt;sub&gt;th&lt;/sub&gt;</td>
<td>1000 - 5000</td>
</tr>
<tr>
<td>Coolant Outlet Temperature</td>
<td>°C</td>
<td>530 - 550</td>
</tr>
<tr>
<td>Pressure</td>
<td>MPa</td>
<td>~0.1</td>
</tr>
<tr>
<td>Average Power Density</td>
<td>MW&lt;sub&gt;th&lt;/sub&gt;/m&lt;sup&gt;3&lt;/sup&gt;</td>
<td>350</td>
</tr>
<tr>
<td>Reference Fuel Compound</td>
<td></td>
<td>Oxide or metal alloy</td>
</tr>
<tr>
<td>Cladding</td>
<td></td>
<td>Ferritic or ODS ferritic</td>
</tr>
<tr>
<td>Average Burnup</td>
<td>GWD/MTTHM</td>
<td>~150 - 200</td>
</tr>
</tbody>
</table>
Figure 1.5: Schematic diagram of Very High Temperature Reactor (US DOE, 2002).

Very High Temperature Reactor (VHTR) is a thermal-neutron-spectrum reactor. The ultimate purpose of this nuclear-reactor design is the co-generation of hydrogen through thermochemical processes. In a VHTR, graphite and helium have been chosen as the moderator and the coolant, respectively. The inlet and outlet temperatures of the coolant are 640 and 1000°C, respectively, at a pressure of 7 MPa (US DOE, 2002). Due to such high outlet temperatures, the thermal efficiency of VHTR will be above 50%. A summary of design parameters of VHTR are listed in Table 1.5 (US DOE, 2002).

Table 1.5: Key design parameters of Very High Temperature Reactor.

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>Reference Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Power</td>
<td>MW&lt;sub&gt;th&lt;/sub&gt;</td>
<td>600</td>
</tr>
<tr>
<td>Average Power Density</td>
<td>MW&lt;sub&gt;th&lt;/sub&gt;/m³</td>
<td>6 - 10</td>
</tr>
<tr>
<td>Coolant Inlet/Outlet Temperature</td>
<td>°C</td>
<td>640/1000</td>
</tr>
<tr>
<td>Coolant: Mass Flow Rate</td>
<td>kg/s</td>
<td>Helium: 320</td>
</tr>
<tr>
<td>Reference Fuel Compound</td>
<td>-</td>
<td>ZrC-coated particles in pins or pebbles</td>
</tr>
<tr>
<td>Net Plant Efficiency</td>
<td>%</td>
<td>&gt;50</td>
</tr>
</tbody>
</table>
A SuperCritical Water-cooled Reactor (SCWR) can be designed as a thermal-neutron-spectrum or fast-neutron-spectrum system. SCWR operates above the critical point of water which is at a temperature of 374°C and a pressure of 22.1 MPa. The operating pressure of SCWR is 25 MPa and the outlet temperature of the coolant is 550°C and/or 625°C depending on the design chosen by the respective country that is developing it. The primary choice of fuel for SCWR is an oxide fuel while a metallic fuel has been considered as the secondary choice for the fast-neutron-spectrum SCWRs. A supercritical-water Rankine cycle has been chosen as the power cycle (US DOE, 2002). The thermal efficiency of SCWR is in the range of 45 – 50 %. Table 1.6 lists the key parameters of the US SCWR design while those of the Canadian design are listed in Table 2.4.

Since SCW CANDU (CANada Deuterium Uranium) is the primary focus of this study, more information has been provided about this design in the following paragraphs.
Currently, several countries worldwide are developing SCWR concepts. Some of the advantages of SCW Nuclear Power Plants (NPPs) over the conventional NPPs include higher thermal efficiency within a range of 45–50% (Pioro and Duffey, 2007) compared to 30–35% for the current NPPs, lower capital costs per kWh of electricity, and the possibility for co-generation of hydrogen. For instance, the copper-chlorine cycle requires steam at temperatures between 500 and 530°C (Naterer et al., 2009, 2010), which is within the operating range of SCWRs. These systems work when supercritical water from a reactor flows through a heat exchanger and transfers heat to a low-pressure steam, which becomes a superheated steam. This superheated steam is transferred at the outlet of the heat exchanger to an adjacent hydrogen plant at a lower pressure (Naterer et al., 2009, 2010).

In general, SCWRs can be classified based on the neutron spectrum, moderator, or pressure boundary. In terms of the pressure boundary, SCWRs are classified into two categories, a) Pressure Vessel (PV) SCWRs, and b) Pressure Tube (PT) or Pressure Channel (PCh) SCWRs (Oka et al., 2010; Pioro and Duffey, 2007). The PV SCWR requires a thick pressure vessel with a thickness of about 50 cm (Pioro and Duffey, 2007) in order to withstand high pressures. The vast majority of conventional Pressurized Water Reactors (PWRs) and Boiling Water Reactors (BWRs) are examples of PV reactors. Figure 1.6 shows a schematic diagram of a PV SCWR. On the other hand, the core of a PT SCWR consists of distributed pressure channels, with a thickness of 10 - 15 mm, which might be oriented vertically or horizontally, analogous to RBMK and

### Table 1.6: Key design parameters of SuperCritical Water-cooled Reactor.

<table>
<thead>
<tr>
<th>Reactor Parameter</th>
<th>Unit</th>
<th>Reference Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Net Efficiency</td>
<td>%</td>
<td>44</td>
</tr>
<tr>
<td>Coolant Inlet/Outlet Temperatures</td>
<td>°C</td>
<td>280/510</td>
</tr>
<tr>
<td>Coolant Pressure</td>
<td>MPa</td>
<td>25</td>
</tr>
<tr>
<td>Spectrum</td>
<td></td>
<td>Thermal</td>
</tr>
<tr>
<td>Average Power Density</td>
<td>MWt/m³</td>
<td>~100</td>
</tr>
<tr>
<td>Reference Fuel</td>
<td></td>
<td>UO₂</td>
</tr>
<tr>
<td>Sheath</td>
<td></td>
<td>Austenitic or ferritic-martensitic stainless steel, or Ni-alloy</td>
</tr>
</tbody>
</table>
CANDU reactors, respectively. Figure 1.7 shows a schematic diagram of a PT SCWR. For instance, SCW CANDU reactor consists of 300 horizontal fuel channels with coolant inlet and outlet temperatures of 350 and 625°C at a pressure of 25 MPa (Pioro and Duffey, 2007). It should be noted that a vertical core option has not been ruled out; both horizontal and vertical cores are being studied by the Atomic Energy of Canada Limited (AECL) (Diamond, 2010). Nevertheless, PT SCWRs provide a better control of flow and density variations. On the other hand, in PV SCWRs, there is a non-uniform temperature variation of coolant at the outlet of the pressure vessel.

![Schematic diagram of SCW CANDU](image)

**Figure 1.7: Schematic diagram of SCW CANDU (Pioro and Duffey, 2007).**

In terms of the neutron spectrum, most SCWR designs are thermal-spectrum; however, fast-spectrum SCWR designs are possible. Recently, Liu et al. (2010) have proposed a mixed spectrum SCWR core, which consists of fast and thermal regions. In general, various solid or liquid moderator options can be utilized in thermal-spectrum SCWRs. These options include light-water, heavy-water, graphite, beryllium oxide, and zirconium hydride (Kirillov et al., 2007). This liquid moderator concept can be used in both PV and
PT SCWRs. The only difference is that in a PV SCWR, the moderator and the coolant are the same fluid. Thus, light-water is a practical choice for the moderator. In contrast, in PT SCWRs the moderator and the coolant are separated. As a result, there are a variety of options in PT SCWRs, mostly due to the separation of the coolant and the moderator.

One of these options is to use a liquid moderator such as light-water or heavy-water. One of the advantages of using a liquid moderator in PT SCWRs is that the moderator acts as a passive heat sink in the event of a Loss Of Coolant Accident (LOCA). A liquid moderator provides an additional safety feature\(^2\), which enhances the safety of operation. On the other hand, one disadvantage of liquid moderators is an increased heat loss from the fuel channels to the liquid moderator, especially at SCWR conditions.

The second option is to use a solid moderator. Currently, in RBMK reactors and some other types of reactors such as Magnox, AGR, and HTR, graphite is used as the moderator. However, graphite may catch fire at high temperatures under some conditions when exposed to water or oxygen. Other materials such as beryllium oxide and zirconium hydride may be used as solid moderators (Kirillov et al., 2007). In this case, heat losses can be reduced significantly. On the contrary, the solid moderators do not act as a passive-safety feature.

High operating temperatures of SCWRs leads to high fuel centerline temperatures. Currently, UO\(_2\) has been used in Light Water Reactors (LWRs) and pressurized heavy-water reactors; however, it has a poor thermal conductivity which may result in high fuel centerline temperatures. Previous studies (Grande et al., 2010; Pioro et al., 2010; Villamere et al., 2009) have shown that the fuel centerline temperatures could exceed the industry limit of 1850°C (Reisch, 2009) when UO\(_2\) is used at SCWR conditions. These studies have been conducted based on an average thermal power per channel and have not taken into account the effects of fuel-sheath gap on the sheath and fuel centreline temperatures. Additionally, the possibility of using enhanced thermal-conductivity fuels in SCWRs has not been examined by previous studies. Moreover, previous studies have focused on the fuel without any emphasis on the fuel channel. Therefore, there is a need to investigate the potential use of conventional and alternative fuels for future use in

\(^2\)Currently, such option is used in CANDU-6 reactors.
SCWRs and analyze the primary fuel-channel design (i.e., High Efficiency Channel, see Section 2.5.1) proposed for SCW CANDU.

The objective of this research is to investigate the possibility of using conventional and alternative fuels especially, high thermal-conductivity and enhanced thermal-conductivity fuels in SCWRs and to study the thermal aspects of the High Efficiency Fuel Channel design, with an objective to determine heat losses from the fuel channel. The nuclear fuels examined in this study are Uranium Dioxide (UO$_2$), Mixed Oxide (MOX), Thorium Dioxide or Thoria (ThO$_2$), Uranium Dioxide plus Silicon Carbide (UO$_2$–SiC), Uranium Dioxide composed of Graphite fibbers (UO$_2$–C), Uranium Dicarbide UC$_2$, Uranium Monocarbide (UC), Uranium Mononitride (UN), and Uranium Dioxide plus Beryllium Oxide (UO$_2$–BeO).

The fuel centerline temperature has been calculated for a PT SCWR (i.e., SCW CANDU). In the present study, the intention has been to use a conservative analysis approach, which is based on the fuel channels with the maximum thermal power, i.e., +15% above the average channel power, instead of using an average thermal power per channel. Additionally, other factors such as volumetric swelling, chemical stability, thermal conductivity, and melting point of the fuel have been considered in order to determine the best fuel options for SCWRs.
2 LITERATURE SURVEY

2.1 Heat Transfer at Supercritical Conditions
The heat transfer at supercritical conditions is characterized by changes in the thermophysical properties of the fluid specifically at pseudocritical points. A pseudocritical point exists at a pressure above the critical pressure of a fluid and at a temperature corresponding to the maximum value of the specific heat for this particular pressure (Pioro and Duffey, 2007). Figure 2.1 shows the increase in the specific heat of water for supercritical pressures up to 26 MPa. As shown in Fig. 2.1, the increase in the specific heat reaches its maximum at the critical point and then decreases as the pressure increases. Furthermore, the pseudocritical temperature increases as the pressure increases. For instance, the corresponding pseudocritical temperatures of light-water at 23 and 25 MPa are approximately 377.5°C and 384.9°C, respectively. Nevertheless, as the temperature passes through the pseudocritical temperature, the specific heat increases. This increase in the specific heat of the fluid allows for the deposition of a significant amount of heat into the fluid. Eventually, this deposited heat can be converted into mechanical energy in steam turbines.

![Figure 2.1: Specific heat of water as a function of pressure and temperature.](image-url)
In addition to the specific heat, other thermophysical properties of a fluid undergo significant changes at the pseudocritical point. These changes affect the heat transfer capabilities of the fluid. Therefore, it is important to ensure that the thermophysical properties of a supercritical fluid are determined with accuracy. Figures 2.2 to 2.6 show water properties (i.e., density, specific heat, enthalpy, thermal conductivity, and dynamic viscosity) at 22.064 and 25 MPa. These thermophysical properties of water have been determined using the NIST REFPROP software.

In general, all thermophysical properties experience considerable changes near the critical and pseudocritical points. These changes are the greatest near the critical point; whereas, they become more gradual in the vicinity of the pseudocritical point.

Figure 2.2: Density of water at 22.064 and 25 MPa.
Figure 2.3: Specific heat of water at 22.064 and 25 MPa.

Figure 2.4: Enthalpy of water at 22.064 and 25 MPa.
Figure 2.5: Thermal conductivity of water at 22.064 and 25 MPa.

Figure 2.6: Dynamic viscosity of water at 22.064 and 25 MPa.
This gradual change in the thermophysical properties of fluids results in a single-phase flow at supercritical conditions. In contrast, at subcritical conditions, a two-phase flow exists as the temperature of the fluid reaches the saturation temperature corresponding to the operating pressure. At the saturation temperature, the fluid undergoes a phase change from liquid to vapor when heat is added to the fluid. As a result of this phase change, there is a discontinuity in the variation of the thermophysical properties of the fluid. Figure 2.7 shows the density of water at 7, 11, and 15 MPa pressures, which correspond to the operating pressures of BWRs, CANDU reactors, and PWRs. As shown in Figure 2.7, there is a sharp drop in the density of water as the saturation temperatures of the corresponding pressures are reached.

![Density of Water at 7, 11, and 15 MPa](image)

**Figure 2.7: Density of Water at 7, 11, and 15 MPa.**

The thermal efficiency of a Nuclear Power Plant (NPP) depends to a large extent on the pressure and temperature of the steam at the inlet to the turbine when the Rankine cycle is considered. In the case of either a direct cycle or an indirect cycle (see Section 2.4), the
physical properties of the steam at the inlet of the turbine depend on the operating temperature and pressure of the reactor coolant. Figure 2.8 shows the operating pressures and temperatures of BWRs, PWRs, and Pressurized Heavy Water Reactors (PHWRs) (e.g., CANDU reactors), which comprise the vast majority of the currently available commercial NPPs.

In terms of the operating conditions of the coolant, these reactors are all categorized as subcritical. PWRs have the highest operating pressure approximately at 15 MPa followed by CANDU reactors and BWRs, which operate at a pressure of 11 and 7 MPa, respectively. The outlet temperature of the coolant depends on the operating pressure of the reactor. In PWRs and CANDU reactors, the outlet temperature of the coolant is slightly below the saturation temperature of their corresponding operating pressures in order to avoid boiling inside the reactor and achieve a high enthalpy rise across the reactor core. Additionally, it is necessary to maintain the pressure within an operational margin due to pressure fluctuation during operation. If the pressure and temperature reach their values at the critical point (i.e., 22.1 MPa and 374°C), the critical heat flux approaches its minimum value which in turn results in the melting of the sheath and the fuel. Therefore, there must be an adequate operational margin between the operating pressure and the critical pressure. As a result, the thermal efficiency of NPPs is limited by operating at subcritical pressures. Consequently, the operating pressure must be increased to pressures above the critical pressure in order to achieve higher efficiencies compared to those of the current NPPs.
Figure 2.8: Operating parameters of several reactors (Pioro and Duffey, 2007).  

As shown in Fig. 2.8, SCWRs operate at pressures and temperatures above the critical pressure and temperature of water. These high temperatures and pressures make it possible to use supercritical “steam” turbines, which have led to high thermal efficiencies when used in coal-fired power plants. As a result, SCWRs will use a proven technology, which has been examined over 50 years of operation in coal-fired power plants. The use of such technology minimizes the technological barriers for the development of suitable turbines for use in the SCW NPPs.

2.2 Heat-Transfer Correlations

The development of SCWRs requires an intensive study of convective heat transfer at supercritical pressures. Heat transfer at a supercritical pressure is different from that of a

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3Pressures are based on the outlet pressures. In other words, the pressure drop across the core or fuel channels has not been shown in Fig. 2.8.
subcritical pressure because the thermophysical properties of light-water coolant undergo significant variations as the temperature of the coolant passes through the pseudocritical point. Therefore, the traditional Nusselt number and other related non-dimensional parameters developed at a subcritical pressure based on the bulk-fluid temperature cannot be used (Bae and Kim, 2009).

At a supercritical pressure, the thermophysical properties of a coolant at the sheath-wall temperature differ significantly from those at the bulk-fluid temperature. A fluid does not undergo a phase change at a supercritical pressure. However, a low-density fluid separates the sheath-wall from a high-density fluid at high heat fluxes and results in a reduction in the convective Heat Transfer Coefficient (HTC). As a result, the sheath-wall temperature increases. This phenomenon is known as the Deteriorated Heat Transfer (DHT) regime. Therefore, the sheath-wall temperature must be reflected in a correlation, which is used to study the heat transfer at supercritical conditions.

Many correlations have been developed for the calculation of HTC at supercritical conditions. The most widely used correlations include those developed by Bishop et al. (1964); Swenson et al. (1965); Krasnoscheckov et al. (1967); Jackson (2002); and Mokry et al. (2009). Zahlan et al. (2011) compared the prediction capabilities of sixteen correlations including the aforementioned correlations. The conclusion of the Zahlan et al. (2011) comparison study showed that the Mokry et al. (2009) correlation resulted in the lowest Root-Mean-Square (RMS) error within the supercritical region compared to all other examined correlations. Table 2.1 shows a comparison between the examined correlations by Zahlan et al. (2011). As shown in Table 2.1, the Mokry et al. (2009) correlation results in the smallest RMS in all three sub-regions indicating the smallest difference between the calculated and experimental values. Table 2.2 lists the Nusselt number correlations examined by Zahlan et al. (2011). Moreover, Table 2.3 provides the validity ranges of these correlations.
Table 2.1: Average and RMS errors in predicting HTC in supercritical sub-regions (Zahlan et al., 2011).

<table>
<thead>
<tr>
<th>Correlation</th>
<th>Liquid-like region</th>
<th>Gas-like region</th>
<th>Close to CP or pc point</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$e_{\text{avg}}$, %</td>
<td>$e_{\text{rms}}$, %</td>
<td>$e_{\text{avg}}$, %</td>
</tr>
<tr>
<td>Dittus-Boelter (1930)</td>
<td>24</td>
<td>44</td>
<td>90</td>
</tr>
<tr>
<td>Sieder and Tate (1936)</td>
<td>46</td>
<td>65</td>
<td>97</td>
</tr>
<tr>
<td>Bishop et al. (1965)</td>
<td>5</td>
<td>28</td>
<td>5</td>
</tr>
<tr>
<td>Swenson et al. (1965)</td>
<td>1</td>
<td>31</td>
<td>-16</td>
</tr>
<tr>
<td>Krasnochekov et al. (1967)</td>
<td>18</td>
<td>40</td>
<td>-30</td>
</tr>
<tr>
<td>Hadaller and Banerjee (1969)</td>
<td>34</td>
<td>53</td>
<td>14</td>
</tr>
<tr>
<td>Gnielinski (1976)</td>
<td>10</td>
<td>36</td>
<td>99</td>
</tr>
<tr>
<td>Watts and Chou (1982), Normal</td>
<td>6</td>
<td>30</td>
<td>-6</td>
</tr>
<tr>
<td>Watts and Chou (1982), Deteriorated</td>
<td>2</td>
<td><strong>26</strong></td>
<td>9</td>
</tr>
<tr>
<td>Griem (1996)</td>
<td>2</td>
<td>28</td>
<td>11</td>
</tr>
<tr>
<td>Koshizuka and Oka (2000)</td>
<td>26</td>
<td>47</td>
<td>27</td>
</tr>
<tr>
<td>Jackson (2002)</td>
<td>15</td>
<td>36</td>
<td>15</td>
</tr>
<tr>
<td>Correlation</td>
<td>Liquid-like region</td>
<td>Gas-like region</td>
<td>Close to CP or pc point</td>
</tr>
<tr>
<td>------------------------</td>
<td>--------------------</td>
<td>-----------------</td>
<td>------------------------</td>
</tr>
<tr>
<td></td>
<td>$e_{\text{avg}, %}$</td>
<td>$e_{\text{rms}, %}$</td>
<td>$e_{\text{avg}, %}$</td>
</tr>
<tr>
<td>Kuang et al. (2008)</td>
<td>-6</td>
<td>27</td>
<td>10</td>
</tr>
<tr>
<td>Mokry et al. (2009)</td>
<td>-5</td>
<td><strong>26</strong></td>
<td>-9</td>
</tr>
<tr>
<td>Cheng et al. (2009)</td>
<td>4</td>
<td>30</td>
<td>2</td>
</tr>
<tr>
<td>Gupta et al. (2010)</td>
<td>-26</td>
<td>33</td>
<td>-12</td>
</tr>
</tbody>
</table>

In **bold** – the minimum values
Table 2.2: List of Nusselt number correlations in Table 2.1 (Zahlan et al., 2011; Bae et al., 2010).

<table>
<thead>
<tr>
<th>Correlation</th>
<th>Equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dittus-Boelter (1930)</td>
<td>( \text{Nu}_b = 0.0243 \text{Re}_b^{0.8} \text{Pr}_b^{0.4} )</td>
</tr>
<tr>
<td>Sieder and Tate (1936)</td>
<td>( \text{Nu}_b = 0.027 \text{Re}_b^{0.8} \text{Pr}_b^{1/3} \left( \frac{\mu}{\mu_w} \right)^{0.14} )</td>
</tr>
<tr>
<td>Bishop et al. (1965)</td>
<td>( \text{Nu}_b = 0.0069 \text{Re}_b^{0.9} \text{Pr}_b^{0.66} \left( \frac{\rho_w}{\rho_b} \right)^{0.43} \left( 1 + 2.4 \frac{D}{x} \right) )</td>
</tr>
<tr>
<td>Swenson et al. (1965)</td>
<td>( \text{Nu}_w = 0.00459 \text{Re}_w^{0.923} \text{Pr}_w^{0.613} \left( \frac{\rho_w}{\rho_b} \right)^{0.231} )</td>
</tr>
</tbody>
</table>
| Krasnoshchekov et al. (1967)      | \( \text{Nu}_b = \text{Nu}_0 \left( \frac{\rho_w}{\rho_b} \right)^{0.3} \left( \frac{c_p}{c_{pb}} \right)^n, \quad \text{Nu}_0 = \frac{\frac{f}{8} \text{Re}_b \text{Pr}}{12.7 \left( \frac{f}{8} \right)^{0.5} \left( \text{Pr}_w^2 - 1 \right) + 1.07} \)\)
<p>|                                  | ( f = \left( \frac{\rho_w}{\rho_b} \frac{T_w}{T_{pc}} \right)^{-2} ) |
|                                  | ( n = 0.4 ) at ( \frac{T_w}{T_{pc}} \leq 1 ) or ( \frac{T_b}{T_{pc}} \geq 1.2 ); ( n = n_1 = 0.22 + 0.18 ) at ( \frac{T_w}{T_{pc}} \leq 2.5 ); ( n = n_1 + (5n_1 - 2) \times \left( 1 - \frac{T_b}{T_{pc}} \right) ) at ( \frac{T_b}{T_{pc}} \leq 1.2 ) |
| Hadaller and Banerjee (1969)      | ( \text{Nu}_f = 0.0101 \text{Re}_f^{0.8774} \text{Pr}_f^{0.6112} (L/D)^{0.0328} ) |
| Gnielinski (1976)                 | ( \text{Nu} = \frac{\left( \frac{f}{8} \right) \left( \text{Re}_b - 1000 \right) \text{Pr}_b}{1 + 12.7 \left( \frac{f}{8} \right)^{1/2} \left( \text{Pr}_b^{2/3} - 1 \right) \left( \text{Pr}_b \right)^{0.11} \left( 1 + (D/L)^2/3 \right)}, \quad f = [0.79 \ln(\text{Re}) - 1.64]^{-2} ) |</p>
<table>
<thead>
<tr>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Watts and Chou (1982), normal heat transfer</td>
</tr>
<tr>
<td>[ \text{Nu}_b = 0.021 \text{Re}_b^{0.8} \text{Pr}_b^{0.55} \left( \frac{\rho_w}{\rho_b} \right)^{0.35} f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}<em>b^{0.5}} \right), \quad \text{Gr}<em>b = \left( \rho_b - \rho</em>{avg} \right)D</em>{hy}^3 g / \rho_b v_b^2 ]</td>
</tr>
<tr>
<td>If ( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \leq 10^{-4} ), ( f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right) = \left( 1 - 3000 \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right)^{0.295} )</td>
</tr>
<tr>
<td>If ( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} &gt; 10^{-4} ), ( f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right) = \left( 7000 \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right)^{0.295} )</td>
</tr>
</tbody>
</table>

| Watts and Chou (1982), deteriorated heat transfer |
| \[ \text{Nu}_b = 0.021 \text{Re}_b^{0.8} \text{Pr}_b^{0.55} \left( \frac{\rho_w}{\rho_b} \right)^{0.35} f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right) \]  |
| If \( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} < 4.5 \times 10^{-4} \), \( f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right) = \left( 1.27 - 19500 \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right)^{0.295} \)  |
| If \( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} > 4.5 \times 10^{-4} \), \( f \left( \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right) = \left( 2600 \frac{\text{Gr}_b}{\text{Re}_b^{2.7} \text{Pr}_b^{0.5}} \right)^{0.295} \)  |

| Griem (1996) |
| \[ \text{Nu}_b = 0.0169 \text{Re}_b^{0.8356} \text{Pr}_b^{0.432} \]  |

<p>| Koshizuka and Oka (2000) |
| [ \text{Nu}_b = 0.015 \text{Re}_b^{0.85} \text{Pr}_b^{0.69} \left( \frac{81000}{CHF} + 1000f_cq \right), q(\text{W/m}^2), f_c = 2.9 \times 10^{-8} + \frac{0.11}{CHF}, \text{for } H &lt; 1.5 \text{ MJ/kg} ]  |
| ( f_c = -8.7 \times 10^{-8} - \frac{0.65}{CHF}, \text{for } 1.5 \leq H \leq 3.3 \text{ MJ/kg}; \text{ and} )  |
| ( f_c = -9.7 \times 10^{-7} + \frac{1.3}{CHF}, \text{for } 3.3 \leq H \leq 4 \text{ MJ/kg}, CHF = 200 G^{1.2} )  |</p>
<table>
<thead>
<tr>
<th>Correlation</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Jackson (2002)</strong></td>
<td>( \text{Nu}_b = 0.0183 \text{Re}_b^{0.82} \text{Pr}<em>b^{0.5} \left( \frac{\rho_w}{\rho_b} \right)^{0.3} \left( \frac{c_p}{c</em>{p,b}} \right)^8 )</td>
</tr>
<tr>
<td><strong>Kuang et al. (2008)</strong></td>
<td>( \text{Nu} = 0.0239 \text{Re}_b^{0.759} \text{Pr}_b^{0.833} \left( \frac{k_w}{k_b} \right)^{0.0863} \left( \frac{\mu_w}{\mu_b} \right)^{0.832} \left( \frac{\rho_w}{\rho_b} \right)^{0.31} \left( \text{Gr}^+ \right)^{0.014} \left( q^+ \right)^{-0.21} )</td>
</tr>
<tr>
<td><strong>Mokry et al. (2009)</strong></td>
<td>( \text{Nu}_b = 0.0061 \text{Re}_b^{0.904} \text{Pr}_b^{0.684} \left( \frac{\rho_w}{\rho_b} \right)^{0.564} )</td>
</tr>
</tbody>
</table>
| **Cheng et al. (2009)** | \( F = \frac{\text{Nu}}{\text{Nu}_0} = \frac{\text{Nu}}{0.023 \text{Re}_b^{0.8} \text{Pr}_b^{3/4}} = \min(F_1,F_2) \)  
\( F_1 = 0.85 + 0.776 (\pi_A \times 10^3)^{2.4}; \quad F_2 = \frac{0.48}{(\pi_A \times 10^3)^{1.55}} + 1.21 \left( 1 - \frac{\pi_A}{\pi_A,pc} \right) \) |
| **Gupta et al. (2010)** | \( \text{Nu}_w = 0.0033 \text{Re}_w^{0.941} \text{Pr}_w^{0.764} \left( \frac{\rho_w}{\rho_b} \right)^{0.156} \left( \frac{\mu_w}{\mu_b} \right)^{0.398} \); \( \text{Nu}_{w,\text{entrance}} = \text{Nu}_w \left( 1 + \exp \left( - \frac{L}{24D} \right) \right)^{0.3} \) |
Table 2.3: Validity ranges of Nusselt number correlations (Zahlan et al., 2011).

<table>
<thead>
<tr>
<th>Correlation</th>
<th>Medium, or Pr and Re Range</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Dittus-Boelter (1930)</strong></td>
<td>(0.7 \leq \text{Pr} \leq 160;) (\text{Re}<em>D \geq 10,000;) and (\text{L}/\text{D}</em>{hy} \geq 10)</td>
</tr>
</tbody>
</table>
| **Sieder and Tate (1936)**

\(4\) | \(0.7 \leq \text{Pr} \leq 16700\)
\(4\) | \(\text{Re}_b \geq 10000;\) and \(\text{L}/\text{D}_{hy} \geq 10\) |
| **Bishop et al. (1965)** | Water |
| **Swenson et al. (1965)** | Water |
| **Krasnoshchekov et al. (1967)** | \(0.9 \leq \text{Re}_b < 5.10\)
\(8.104 \leq \text{Re}_b < 5.10\)
\(0.85 \leq \text{Pr}_{b, \text{avg}} < 65\)
\(0.9 \leq \rho_w/\rho_b < 1.0\)
\(0.02 \leq c_{p, \text{avg}}/c_{pb} < 4.0\) |
| **Gnielinski (1976)**

\(4\) | \(0.5 \leq \text{Pr} \leq 2000\)
\(3000 \leq \text{Re}_b \leq 5 \times 10^6;\) and \(\text{L}/\text{D}_{hy} \geq 10\) |
| **Watts and Chou (1982)** | Water |

<table>
<thead>
<tr>
<th></th>
<th>(P, \text{MPa})</th>
<th>(T, \text{K})</th>
<th>(q, \text{kw/m}^2)</th>
<th>(G, \text{kg/m}^2\text{s})</th>
<th>Flow Geometry/direction</th>
<th>Medium, or Pr and Re Range</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Dittus-Boelter (1930)</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
| **Sieder and Tate (1936)**

\(4\) | | | | | | |
| **Bishop et al. (1965)** | | | | | | |
| **Swenson et al. (1965)** | | | | | | |
| **Krasnoshchekov et al. (1967)** | | | | | | |
| **Gnielinski (1976)**

\(4\) | | | | | | |
| **Watts and Chou (1982)** | | | | | | |

\(4\) Incropera et al. (2006)
<table>
<thead>
<tr>
<th>Source</th>
<th>$P$, MPa</th>
<th>$T$, °C</th>
<th>$q$, kW/m²</th>
<th>$G$, kg/m²s</th>
<th>Flow Geometry/direction</th>
<th>Medium, or Pr and Re Range</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Griem (1996)</strong></td>
<td>22.08–25</td>
<td>332–424</td>
<td>300–600</td>
<td>500–2500</td>
<td>downward flow</td>
<td>Water</td>
</tr>
<tr>
<td><strong>Koshizuka and Oka (2000)</strong></td>
<td>31</td>
<td>$T_b = 351–356$</td>
<td>473</td>
<td>540</td>
<td>Tube (ID = 9.4 mm)</td>
<td>Water</td>
</tr>
<tr>
<td><strong>Kuang et al. (2008)</strong></td>
<td>22.75–31.0</td>
<td>–</td>
<td>233–3474</td>
<td>380–3600</td>
<td>Tube (ID = 7.5–26 mm)</td>
<td>Water</td>
</tr>
<tr>
<td><strong>Mokry et al. (2009)</strong></td>
<td>24</td>
<td>$320 &lt; T_{b, in} &lt; 350$</td>
<td>Up to 1250</td>
<td>200–1500</td>
<td>Tube (4-m-long vertical)/upward</td>
<td>Water</td>
</tr>
<tr>
<td><strong>Gupta et al. (2010)</strong></td>
<td>24</td>
<td>$320 &lt; T_{b, in} &lt; 350$</td>
<td>$380 &lt; T_{b, in} &lt; 406$</td>
<td>$70–1250$</td>
<td>$200, 500, 1000, 1500$</td>
<td>Tube (4-m-long vertical)/upward</td>
</tr>
</tbody>
</table>
2.3 Specifications of Generic 1200-MW_{el} PT SCWR

The core of a generic 1200-MW_{el} PT SCWR consists of 300 fuel channels that are located inside a cylindrical tank called the calandria vessel. Figure 2.9 shows a cross-sectional view of the core, which consists of 220 SuperCritical-Water (SCW) fuel channels and 80 Steam Re-Heat (SRH) fuel channels. SRH and SCW fuel channels are located on the periphery and at the center of the core, respectively. In terms of neutron spectrum, the studied PT SCWR is a thermal-spectrum reactor. In this thermal-spectrum PT SCWR, light-water and heavy-water have been chosen as the coolant and the moderator, respectively. The coolant enters the supercritical fuel channels at an inlet temperature of 350°C and reaches an outlet temperature of 625°C at a pressure of 25 MPa. The inlet temperature of the SuperHeated Steam (SHS), which is used as the coolant, in the SRH fuel channels, is 400°C and the corresponding outlet temperature is 625°C at an operating pressure of 5.7 MPa. Table 2.4 lists the operating parameters of the generic 1200-MW_{el} PT SCWR (Naidin et al., 2009).

Table 2.4: Operating parameters of generic PT SCWR (Naidin et al., 2009).

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Unit</th>
<th>Generic PT SCWR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Electric Power</td>
<td>MW</td>
<td>1143-1220</td>
</tr>
<tr>
<td>Thermal Power</td>
<td>MW</td>
<td>2540</td>
</tr>
<tr>
<td>Thermal Efficiency</td>
<td>%</td>
<td>45-48</td>
</tr>
<tr>
<td>Coolant</td>
<td>-</td>
<td>H₂O</td>
</tr>
<tr>
<td>Moderator</td>
<td>-</td>
<td>D₂O</td>
</tr>
<tr>
<td>Pressure of SCW at Inlet/Outlet</td>
<td>MPa</td>
<td>25.8</td>
</tr>
<tr>
<td>Pressure of SHS at Inlet/Outlet</td>
<td>MPa</td>
<td>6.1</td>
</tr>
<tr>
<td>$T_{in}/T_{out}$ Coolant (SCW)</td>
<td>°C</td>
<td>350</td>
</tr>
<tr>
<td>$T_{in}/T_{out}$ Coolant (SHS)</td>
<td>°C</td>
<td>400</td>
</tr>
<tr>
<td>Mass Flow Rate per SCW/SRH Channel</td>
<td>kg/s</td>
<td>4.4</td>
</tr>
<tr>
<td>Thermal Power per SCW/SRH Channel</td>
<td>MW</td>
<td>8.5</td>
</tr>
<tr>
<td># of SCW/SRH Channels</td>
<td>-</td>
<td>220</td>
</tr>
<tr>
<td>Heat Flux in SCW/SRH Channel</td>
<td>kW/m²</td>
<td>970</td>
</tr>
<tr>
<td>Fuel Bundle (Leung, 2008)</td>
<td>-</td>
<td>Variant-20</td>
</tr>
</tbody>
</table>
2.4 Thermal Cycles
The use of supercritical “steam” turbines in NPPs leads to higher thermal efficiencies compared to those of the current NPPs. There are several design options of Rankin cycles in order to convert the thermal energy of the supercritical “steam” into work in a supercritical turbine. These design options include direct, indirect, and dual cycles. In a direct cycle, supercritical “steam” from the reactor passes directly through a supercritical turbine eliminating the need for the steam generators. This elimination reduces the costs and leads to higher thermal efficiencies compared to those produced in indirect cycles. In an indirect cycle, the supercritical coolant passes through the heat exchangers or steam generators to transfer heat to a secondary fluid, which passes through the turbine(s). The advantage of an indirect cycle is that potential radioactive particles would be contained inside the steam generators. On the other hand, the temperature of the secondary loop
fluid is lower than that of the primary loop (e.g., reactor heat transport system loop). As a result, the thermal efficiency of an indirect cycle is lower than that of a direct cycle (Pioro et al., 2010).

With direct cycles, the thermal efficiency can be increased further through a combination of reheat and regeneration options. In a single-reheat cycle, supercritical “steam” from the reactor passes through a high pressure turbine where its temperature and pressure drop. Then, the steam from the outlet of the high pressure turbine is sent through the SRH fuel channels inside the reactor core, but at a lower pressure. As the steam passes through the SRH fuel channels its temperature increase to an outlet temperature of 625°C at a pressure between 3 and 7 MPa (Pioro et al., 2010). At the outlet of the SRH channels, SHS passes through the intermediate pressure turbines. When a regenerative option is considered, steam from high and intermediate turbines are extracted and sent to a series of open and closed feed-water heat exchangers. The steam is used to increase the temperature of the feed-water to the inlet temperature of the reactor. Figure 2.10 shows a single-reheat cycle with the regenerative option.

![Diagram of a single-reheat cycle for SCW NPP](image)

**Figure 2.10:** Single-reheat cycle for SCW NPP (Naidin et al., 2009).
2.5 Fuel Channel Designs
The design of a fuel channel for SCWRs is an arduous undertaking due to high operating temperatures, which require materials that withstand temperatures as high as 625°C under normal operating conditions. In contrast, current materials, which withstand such design temperatures, have high absorption cross-sections for thermal neutrons. Consequently, a fuel-channel design must address the limitations due to material options to allow for maximum performance using available materials. AECL has proposed several fuel-channel designs for SCWRs. These fuel-channel designs can be classified into two categories: direct-flow and re-entrant channel concepts, which will be described in Sections 2.5.1 and 2.5.2. It should be noted that a re-entrant fuel-channel concept was developed by Russian scientists and was utilized at Unit 1 of the Beloyarskaya NPP in the 1960s (Saltanov et al., 2009).

2.5.1 High-Efficiency Fuel Channel
The High Efficiency fuel Channel (HEC) consists of a pressure tube, a ceramic insulator, a liner tube, and 12 fuel bundles. Figure 2.11 shows a 3-D view of HEC. The outer surface of the pressure tube is exposed to a moderator. The moderator could be a liquid moderator such as heavy-water or a solid moderator. There are several solid moderator options including beryllium, beryllium oxide, zirconium hydride and graphite (Kirillov et al., 2007). However, using graphite as a solid moderator is unlikely due to the Chernobyl’s nuclear disaster.

The purpose of using an insulator is to reduce the operating temperature of the pressure tube and heat losses from the coolant to the moderator. Low operating temperatures of the pressure tube would allow for the use of available materials such as Zr-2.5%Nb, which has low absorption cross-sections for thermal neutrons (Chow and Khartabil, 2008). The liner, which is a perforated tube and made of stainless steel, intends to protect the ceramic insulator from being damaged during operation or possible refuelling due to stresses introduced by fuel bundles and from erosion by the coolant flow. Since this fuel channel is the primary option for the SCW CANDU, further analysis of this fuel channel has been provided in this study (see Section 4.2).
Figure 2.11: 3-Dimensional view of High Efficiency fuel channel (based on Chow and Khartabil, 2008).

2.5.2 Re-Entrant Fuel Channels
There are several Re-Entrant fuel Channel (REC) designs. The first design consists of a pressure tube and a flow tube which are separated by a gap. The coolant flows along the gap between the flow tube and pressure tube. When the coolant reaches the end of the fuel channel, it flows inside the flow tube where a bundle string is placed. The outer surface of the pressure tube is in contact with the moderator. The use of such fuel-channel design is possible only if the liquid moderator is pressurized to reduce heat losses. Figure 2.12 shows a 3-D view of the fuel channel and flow direction of the coolant.

Since the heat loss from the aforementioned fuel channel is significantly high, this design has been modified in the form of the fuel channels shown in Figs. 2.13 and 2.14. The second design (see Fig. 2.13) consists of a calandria tube, a pressure tube, and a flow tube. The gap between the pressure tube and the calandria tube is filled with an inert gas, which provides thermal insulation, reducing the heat losses from the ‘hot’ pressure tube to the moderator. As shown in Fig. 2.13, the outer surface of the calandria tube is exposed to a liquid moderator.
Figure 2.12: 3-Dimensional view of Re-Entrant fuel channel (based on Chow and Khartabil, 2008).

Unlike the HEC design, forces due to fuelling/refuelling are not exerted directly on the ceramic in the third design shown in Fig. 2.14, ensuring that the mechanical integrity of the ceramic insulator is maintained. Additionally, the ceramic insulator reduces the heat
losses from the coolant to the moderator. Moreover, the ceramic insulator acts as a thermal barrier, which in turn results in relatively lower operating temperatures of the pressure tube. Such low operating temperatures allow for the use of Zr-2.5%Nb, which has low absorption cross-sections for thermal neutrons, as the material of the pressure tube. Therefore, lower heat losses, a better protection of the ceramic insulator, and the possibility of using Zr-2.5%Nb as the material of the pressure tube are several advantages of this fuel channel.

Figure 2.14: 3-Dimensional view of Re-Entrant fuel channel with ceramic insulator.

2.6 Fuel-Bundle Designs
AECL has developed several fuel-bundle designs, which are suitable for use in PT reactors. These fuel-bundles designs include the 37-element, CANFLEX\textsuperscript{5}, Variant-18, and Variant-20 bundles. The 37-element fuel bundle consists of 37 fuel elements each having an outer diameter of 13.06 mm. The differences between the 37-element and CANFLEX fuel-bundle designs include a higher number of elements and variation in the size of fuel elements. CANFLEX is a 43-element fuel bundle, which consists of 35 fuel elements with an outer diameter of 11.5 mm and 8 fuel elements with an outer diameter of 13.5 mm. Similar to the CANFLEX fuel bundle, the Variant-18 and Variant-20 fuel bundles have 43 elements. However, the difference is that only 42 elements are fuel

\textsuperscript{5} CANDU Flexible.
elements. The central elements of the Variant-18 and Variant-20 fuel bundles have an outer diameter of 18 mm and 20 mm, respectively. Additionally, the central elements contain burnable neutron absorber. Figure 2.15 shows a 2-D view of these fuel bundles.

![Figure 2.15: Comparison of fuel-bundle geometries: (a) 37-element, (b) 43-element, (c) Variant-18, and (d) Variant-20 (Grande et al., 2010).](image)

A summary of the dimensions of elements in these fuel-bundle designs is listed in Table 2.5. As indicated in Table 2.5, the general trend in the development of new fuel bundles is to reduce the diameter of fuel elements and to increase the number of fuel elements. Smaller fuel element diameters result in lower fuel centerline temperatures for the same fuel-channel specifications (e.g., coolant flow rate, thermal power, or length).

<table>
<thead>
<tr>
<th>Bundle</th>
<th>Number of Element</th>
<th>Element Diameter (mm)</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>37-Element</td>
<td>37</td>
<td>Center: 13.06</td>
<td>Inner Ring: 13.06</td>
<td>Middle Ring: 13.06</td>
<td>Outer Ring: 13.06</td>
</tr>
<tr>
<td>CANFLEX</td>
<td>43</td>
<td>Center: 13.5</td>
<td>Inner Ring: 13.5</td>
<td>Middle Ring: 11.5</td>
<td>Outer Ring: 11.5</td>
</tr>
<tr>
<td>Variant-18</td>
<td>43</td>
<td>Center: 18</td>
<td>Inner Ring: 11.5</td>
<td>Middle Ring: 11.5</td>
<td>Outer Ring: 11.5</td>
</tr>
<tr>
<td>Variant-20</td>
<td>43</td>
<td>Center: 20</td>
<td>Inner Ring: 11.5</td>
<td>Middle Ring: 11.5</td>
<td>Outer Ring: 11.5</td>
</tr>
</tbody>
</table>

Table 2.5: Summary of element diameters in bundle designs (Leung, 2008).
Leung (2008) has conducted full-scale bundle experiments in order to examine the condition and power level leading to dryout power in the aforementioned fuel bundles. A vertical test section was used with an overall length of 7.94 m. A flow tube, which holds the fuel-bundle simulators, was inserted inside the test section. The simulator was electrically heated with a heated length of 4.33 m. The experiments were conducted using Refrigerant R-134a under conditions listed in Table 2.6, which includes the equivalent operating conditions based on water. Appendix A provides scaling parameters for fluid-to-fluid modeling at supercritical conditions.

Table 2.6: Experimental conditions on a full-scale R-134a.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Unit</th>
<th>Range</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Refrigerant-134a</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Water Equivalent</td>
</tr>
<tr>
<td>Outlet Pressure</td>
<td>MPa</td>
<td>1.8-2.11</td>
</tr>
<tr>
<td></td>
<td></td>
<td>11-12.5</td>
</tr>
<tr>
<td>Mass Flow Rate</td>
<td>kg/s</td>
<td>12, 15, 19</td>
</tr>
<tr>
<td></td>
<td></td>
<td>17, 21, 26</td>
</tr>
<tr>
<td>Inlet Fluid Temperature</td>
<td>°C</td>
<td>42-59</td>
</tr>
<tr>
<td></td>
<td></td>
<td>260-300</td>
</tr>
</tbody>
</table>

The result of Leung’s (2008) experiments indicated that the Variant-20 fuel bundle resulted in a reduction in coolant void reactivity\(^6\) and an increase in dryout power. In other words, the Variant-20 fuel bundles can be operated at a higher power rating before the dryout occurs. As a result, the Variant-20 fuel bundle has been chosen for the purpose of the calculation of the sheath and fuel centerline temperatures at SCWR conditions. Figure 2.16 shows a 3-D view of the Variant-20 fuel bundle.

It should be mentioned that the thermal-hydraulic analysis conducted in this thesis is a one-dimensional heat transfer. Additionally, the fuel centerline and sheath temperatures have been calculated only for one fuel element with an outer diameter of 11.5 mm. Therefore, the results are applicable to any fuel-bundle design with such fuel element diameters.

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\(^6\) In a CANDU core, when the coolant is lost or voided due to boiling, a positive reactive is added to the core which in turn increases the power.
2.7 Nuclear Fuels

Nuclear fuels can be classified into two categories; metallic fuels and ceramic fuels. The most common metallic fuels include uranium, plutonium, and thorium (Kirillov et al., 2007). The advantage of metallic fuels is their high thermal conductivity; however, they suffer from low melting points and also that the fuel undergoes phase change. The three phases in a metallic uranium fuel includes $\alpha$-, $\beta$-, and $\gamma$-phase. A phase changes to another phase as a function of temperature, resulting in a volume change in the fuel. For instance, $\alpha$-phase of uranium metal is stable up to $670^\circ$C, the $\beta$-phase exists between 670 and $776^\circ$C, and the $\gamma$-phase exists from $776^\circ$C up to the melting point of ~$1135^\circ$C (Kirillov et al., 2007). Additionally, the exposure of the metallic fuel to a neutron flux results in fuel swelling and has negative effects on its thermal conductivity (Kirillov et al., 2007). Moreover, metallic fuels undergo oxidation when exposed to air or water.

For use in high-temperature applications, a potential fuel must have a high melting point, high thermal conductivity, and good irradiation and mechanical stability (Ma, 1983). These requirements eliminate various nuclear fuels categorized under the metallic fuels mainly due to their low melting points and high irradiation creep and swelling rates (Ma, 1983). On the other hand, ceramic fuels have promising properties, which make these fuels suitable candidates for SCWR applications.
In general, ceramic fuels have good dimensional and radiation stability and are chemically compatible with most coolants and sheath materials. As a result, this study focuses only on the ceramic fuels. The ceramic fuels examined in this study are UO₂, MOX, ThO₂, UC, UC₂, UN, UO₂–SiC, UO₂–C, and UO₂–BeO. Further, these ceramic fuels can be classified into three categories: a) low thermal-conductivity fuels, b) enhanced thermal-conductivity fuels, and c) high thermal-conductivity fuels. Low thermal-conductivity fuels are UO₂, MOX, and ThO₂. Enhanced thermal-conductivity fuels are UO₂–SiC, UO₂–C, and UO₂–BeO; and high thermal-conductivity fuels are UC₂, UC, and UN. Table 2.7 provides basic properties of selected fuels at 0.1 MPa and 25°C.

In addition to the melting point of a fuel, the thermal conductivity of the fuel is a critical property that affects the operating temperature of the fuel under specific conditions. UO₂ has been used as the fuel of choice in BWRs, PWRs, and CANDU reactors. The thermal conductivity of UO₂ is between 2 and 3 W/m K within the operating temperature range of SCWRs. On the other hand, fuels such as UC₂, UC, and UN have significantly higher thermal conductivities compared to that of UO₂ as shown in Fig. 2.17. Thus, under the same operating conditions, the fuel centerline temperature for these fuels should be lower than that of UO₂ fuel. In order to provide a comparison, the fuel centerline temperature profiles of all the aforementioned fuels have been calculated and presented in Section 4.
Table 2.7: Basic properties of selected fuels at 0.1 MPa and 25°C (Chirkin, 1968; IAEA, 2008; Frost, 1963; Cox and Cronenberg, 1977; Leitnaker and Godfrey, 1967; Lundberg and Hobbins, 1992).

<table>
<thead>
<tr>
<th>Property</th>
<th>Unit</th>
<th>UO₂</th>
<th>MOX⁷</th>
<th>ThO₂</th>
<th>UC</th>
<th>UC₂</th>
<th>UN</th>
</tr>
</thead>
<tbody>
<tr>
<td>Molecular Mass</td>
<td>amu</td>
<td>270.3</td>
<td>271.2</td>
<td>264</td>
<td>250.04</td>
<td>262.05</td>
<td>252.03</td>
</tr>
<tr>
<td>Theoretical density</td>
<td>kg/m³</td>
<td>10960</td>
<td>11.074</td>
<td>10.000</td>
<td>1363⁸</td>
<td>1168⁰</td>
<td>14420</td>
</tr>
<tr>
<td>Melting Point</td>
<td>°C</td>
<td>2847±30</td>
<td>2750</td>
<td>3227±150</td>
<td>2507⁹</td>
<td>2520</td>
<td>253²</td>
</tr>
<tr>
<td>Heat Capacity</td>
<td>J/kgK</td>
<td>235</td>
<td>240</td>
<td>235</td>
<td>203¹²</td>
<td>233⁴</td>
<td>190</td>
</tr>
<tr>
<td>Heat of Vaporization</td>
<td>kJ/kg</td>
<td>1530</td>
<td>1498</td>
<td>-</td>
<td>2120</td>
<td>1975±2³⁰</td>
<td>1144¹³</td>
</tr>
<tr>
<td>Thermal Conductivity</td>
<td>W/mK</td>
<td>8.7</td>
<td>7.8¹⁵</td>
<td>9.7</td>
<td>21.2</td>
<td>11.57</td>
<td>14.6</td>
</tr>
<tr>
<td>Linear Expansion Coefficient,</td>
<td>1/K</td>
<td>9.75</td>
<td>9.43</td>
<td>8.9¹⁶</td>
<td>10.1</td>
<td>18.1¹⁷</td>
<td>7.52</td>
</tr>
<tr>
<td>×10⁻⁶</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Electric Resistivity</td>
<td>Ω·m</td>
<td>7.32</td>
<td>-</td>
<td>-</td>
<td>250</td>
<td>120</td>
<td>146</td>
</tr>
<tr>
<td>×10⁻⁸</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Crystal Structure</td>
<td></td>
<td>FCC</td>
<td>FCC</td>
<td>FCC</td>
<td>FCC</td>
<td>FCC</td>
<td>FCC</td>
</tr>
</tbody>
</table>

FCC – Faced-Centered Cubic
BCT – Body-Centered Tetragonal

⁷ MOX – Mixed Oxides (U₀.₈Pu₀.₂)O₂, where 0.8 and 0.2 are the molar parts of UO₂ and PuO₂.
⁸ Frost(1963)
⁹ Cox and Cronenberg (1977)
¹⁰ Lundberg and Hobbins (1992)
¹¹ at nitrogen pressure ≥ 0.25 MPa
¹² Leitnaker & Godfrey (1967)
¹³ UN(s)=U(l)+0.5N₂(g), Gingerich (1969)
¹⁴ UN(s)=U(g)+0.5N₂(g), Gingerich (1969)
¹⁵ at 95% density
¹⁶ at 1000°C (Bowman, Arnold, Witteman, & Wallace, 1966)
¹⁷ At 1000°C, Bowman et al. (1965)
Figure 2.17: Thermal conductivity of several fuels (Cox and Cronenberg, 1977; Frost et al., 1963; IAEA, 2008; Ishimoto et al., 1995; Leitnaker and Godfrey, 1967; Khan et al., 2010, Kirillov et al., 2007; Lundberg and Hobbins, 1992; Solomon et al., 2005).

2.7.1 Low Thermal-Conductivity Fuels: UO$_2$, MOX, and ThO$_2$

2.7.1.1 UO$_2$ and MOX
As a ceramic fuel, Uranium Dioxide (UO$_2$) is a hard and brittle material due to its ionic or covalent interatomic bonding. In spite of that, the uranium dioxide fuel is currently used in PWRs, BWRs, and CANDU reactors because of its properties. Firstly, oxygen has a very low thermal-neutron absorption cross-section, which does not result in a serious loss of neutrons. Secondly, UO$_2$ is chemically stable and does not react with water within the operating temperatures of these reactors. Thirdly, UO$_2$ is structurally very stable.
Additionally, the crystal structure of the UO₂ fuel retains most of fission products even at high burn-up (Cochran and Tsoulfanidis, 1999). Moreover, UO₂ has a high melting point; however, its thermal conductivity is very low, minimizing the possibility of using UO₂ as a fuel of choice for SCWRs. The thermal conductivity of 95% Theoretical Density (TD) UO₂ can be calculated using the Frank correlation, shown as Eq. (2.1) (Carbajo et al., 2001). This correlation is valid for temperatures in the range of 25 to 2847°C.

\[
k_{\text{UO}_2}(T) = \frac{100}{7.5408 + 17.692 \times (10^{-3}T) + 3.6142 \times (10^{-3}T)^2} + \frac{6400}{(10^{-3}T)^{5/2}} \exp^{-16.35/(10^{-3}T)} \quad (2.1)
\]

Mixed Oxide (MOX) fuel refers to nuclear fuels consisting of UO₂ and plutonium dioxide (PuO₂). MOX fuel was initially designed for use in Liquid-Metal Fast Breeder Reactors (LMFBRs) and in Light Water Reactor (LWRs) when reprocessing and recycling of the used fuel is adopted (Cochran and Tsoulfanidis, 1999). The uranium oxide content of MOX may be natural, enriched, or depleted uranium, depending on the application of MOX fuel. In general, MOX fuel contains between 3 and 5% PuO₂ blended with 95 – 97% natural or depleted uranium dioxide (Carbajo et al., 2001). The small fraction of PuO₂ slightly changes the thermophysical properties of MOX fuel compared with those of UO₂ fuel. Nonetheless, the thermophysical properties of MOX fuel should be selected when a study of the fuel is undertaken.

Most thermophysical properties of UO₂ and MOX (3 – 5% PuO₂) have similar trends. For instance, thermal conductivities of UO₂ and MOX fuels decrease as the temperature increases up to 1700°C (see Fig. 2.17). The most significant differences between these two fuels have been summarized in Table 2.7. Firstly, MOX fuel has a lower melting temperature, lower heat of fusion, and lower thermal conductivity than UO₂ fuel. For the same power, MOX fuel has a higher stored energy which results in a higher fuel centerline temperature compared with UO₂ fuel. Secondly, the density of MOX fuel is slightly higher than that of UO₂ fuel.
The thermal conductivity of the fuel is of importance in the calculation of the fuel centerline temperature. The thermal conductivities of MOX and UO$_2$ decrease as functions of temperature up to temperatures around 1527 – 1727°C, and then it increases as the temperature increases (see Fig. 2.17). In general, the thermal conductivity of MOX fuel is slightly lower than that of UO$_2$. In other words, addition of small amounts of PuO$_2$ decreases the thermal conductivity of the mixed fuel. However, the thermal conductivity of MOX does not decrease significantly when the PuO$_2$ content of the fuel is between 3 and 15%. Moreover, the thermal conductivity of MOX fuel decreases as the concentration of PuO$_2$ increases beyond 15%. As a result, the concentration of PuO$_2$ in commercial MOX fuels is kept below 5% (Carbajo et al., 2001). Carbajo et al. (2001) recommend the following correlation shown as Eq. (2.2) for the calculation of the thermal conductivity of 95% TD MOX fuel. This correlation is valid for temperatures between 427 and 2827°C, $x$ less than 0.05, and PuO$_2$ concentrations between 3 and 15%. In Eq. (2.2), $T$ indicates temperature in Kelvin.

$$k(T,x) = \frac{1}{A+C(10^{-3}T)} + \frac{6400}{(10^{-3}T)^{5/2}} \exp^{-16.35/(10^{-3}T)}$$  \quad (2.2)$$

Where $x$ is a function of oxygen to heavy metal ration ($x = 2- O/M$) and

$$A(x) = 2.58x + 0.035 \text{ (mK/W), } C(x) = (-0.715x + 0.286) \text{ (m/W)}$$

### 2.7.1.2 ThO$_2$

Currently, there is an interest in using thorium based fuels in nuclear reactors. Thorium is widely distributed in nature and is approximately three times as abundant as uranium. However, ThO$_2$ does not have any fissile elements to fission with thermal neutrons. Consequently, ThO$_2$ must be used in combination with a “driver” fuel (e.g., UO$_2$, UC, or PuO$_2$), which has $^{235}$U as its initial fissile elements. The presence of a “driver” fuel such as UO$_2$ in a nuclear-reactor core results in the production of enough neutrons, which in turn start the thorium cycle. In this cycle, $^{232}$Th is converted into $^{233}$Th, which decays to
The latter element eventually results in the formation of $^{233}$U, which is a fissile element (Cochran and Tsoulfanidis, 1999).

In regards to PT reactors, there are two possibilities when ThO$_2$ is used. One option is to place ThO$_2$ and a “driver” fuel in different fuel channels. The separation between ThO$_2$ fuel and the “driver” fuel allows ThO$_2$ fuel to stay longer inside the core. The second option is to enclose ThO$_2$ and the “driver” in same fuel bundles, which are placed inside the fuel channels throughout the reactor core. This option requires the enrichment of the “driver” fuel since it has to be irradiated as long as ThO$_2$fuel stays inside the core (IAEA, 2005). Nevertheless, the current study considers the thermal aspects of one single fuel channel, which consists of ThO$_2$ fuel bundles (i.e., first Option). However, this assumption does not suggest that the whole core is composed of fuel channels containing ThO$_2$.

The use of thorium based fuels in nuclear reactors requires information on the thermophysical properties of these fuels. Jain et al. (2006) have conducted experiments on thorium and the solid solutions of thorium dioxide (ThO$_2$) and lanthanum oxide (LaO$_{1.5}$). As a result of their experiments, Jain et al. (2006) have determined the density, thermal diffusivity, and specific heat for several compositions of ThO$_2$ and LaO$_{1.5}$ ranging from pure thorium to 10 mole percent LaO$_{1.5}$. These properties were measured for temperatures between 100 and 1500°C (Jain et al., 2006). Information about the effects of Lanthanum on the thermal conductivity of ThO$_2$ is provided in Appendix B.

In their analysis, the thermal conductivity values have been calculated based on Eq. (2.3), which requires the measured values of the thermal diffusivity, specific heat, and density of these solid solutions. In the current study, the correlation developed by Jain et al. (2006), which is shown as Eq. (2.4), has been used in order to calculate the thermal conductivity of ThO$_2$ fuel for the purpose of the calculation of the fuel centerline temperature. The thermal conductivity of ThO$_2$ as a function of temperature is shown in Fig. 2.18.

$$k = \alpha \rho c_p$$  (2.3)
\[ k_{\text{ThO}_2} = \frac{1}{0.0327 + 1.603 \times 10^{-4} T} \] (2.4)

**Figure 2.18:** Thermal conductivity of ThO$_2$ (Jain et al., 2006).

### 2.7.2 High Thermal-Conductivity Fuels: UC, UC$_2$, and UN

#### 2.7.2.1 UC

There is an interest on carbides of uranium as nuclear fuels due to their high thermal conductivities and high melting points. Carbides of uranium usable for nuclear fuels are Uranium Carbide (UC) and Uranium Dicarbide (UC$_2$). For instance, UC has been proposed as the fuel of choice for a SCWR concept in Russia (Pioro and Duffey, 2007). Uranium sesquicarbide (U$_2$C$_3$) is another carbide of uranium; however, it cannot be manufactured through casting or compaction of a powder. However, UC$_2$ may transform to U$_2$C$_3$ at high temperatures and under stress (Frost, 1963).

UC, which has a Faced-Centered Cubic (FCC) crystal structure similar to those of UN and NaCl, has a high melting point approximately 2507°C and a high thermal conductivity, above 19 W/m K at all temperatures up to the melting point. UC has a
density of 13630 kg/m³, which is lower than that of UN but higher than those of UO₂ and UC₂. It should be noted that the density of hypo-stoichiometric UC is slightly higher than that of stoichiometric UC, which is listed in Table 2.7. Coninck et al. (1975) report densities between 13730 and 13820 kg/m³ at 25°C for hypo-stoichiometric UC. Moreover, UC has a higher uranium atom density compared to UO₂ but lower than that of UN. The uranium atom densities of UC and UN are 1.34 and 1.4 times that of UO₂.

For the purpose of the calculation of the fuel centerline temperature, thermodynamic properties of UC are required as functions of temperature. Coninck et al. (1975) conducted experiments on hypo-stoichiometric and stoichiometric UC, and determined the thermal diffusivity, thermal conductivity, and spectral emissivity of UC. For hypo-stoichiometric UC, the thermal diffusivity α, in m²/s, and thermal conductivity k, in W/m K, correlations are valid for a temperature range of 570 and 2000°C. In Eqs. (2.5) and (2.6), T is in degrees Kelvin (Coninck et al., 1975).

\[
\alpha = 10^{-4} \cdot [5.75 \cdot 10^{-2} + 1.25 \cdot 10^{-6}(T - 273.15)] \quad (2.5)
\]

\[
k = 100 \cdot [2.04 \cdot 10^{-1} + 2.836 \cdot 10^{-8}(T - 843.15)^2] \quad (2.6)
\]

Coninck et al. (1975) provide two correlations for the calculation of the spectral emissivity of hypo-stoichiometric UC. Equation (2.7) has been suggested for pure UC when temperature varies between 1100°C and 2000°C. Moreover, Eq. (2.8) can be used in order to determine the spectral emissivity of oxidized samples for temperatures between 1100°C and 1600°C. In Eqs. (2.7) and (2.8), T is in degrees Kelvin.

\[
\epsilon = 5.5 \cdot 10^{-1} - 8.5 \cdot 10^{-5}(T - 273.15) \quad (2.7)
\]

\[
\epsilon = -4.666 \cdot 10^{-1} + 1.050 \cdot 10^{-1}(T - 273.15) - 7.627 \cdot 10^{-5}(T - 273.15)^2 + 1.813 \cdot 10^{-8}(T - 273.15)^3 \quad (2.8)
\]
Coninck et al. (1975) provided two correlations, shown as Eqs. (2.9) and (2.10), which can be used to determine the mean values of the thermal diffusivity and thermal conductivity of stoichiometric UC for a temperature range between 850 and 2250°C, in m²/s and W/m K, respectively. Additionally, Eq. (2.11) can be used to calculate the spectral emissivity of stoichiometric UC for temperatures between 1100 and 2250°C (Coninck et al., 1975). In Eq. (2.9) through (2.11), \( T \) is in degrees Kelvin.

\[
\alpha = 10^{-4} \cdot [5.7 \cdot 10^{-2} + 1.82 \cdot 10^{-12}(T - 1123.15)^3] \tag{2.9}
\]

\[
k = 100 \cdot [1.95 \cdot 10^{-1} + 3.57 \cdot 10^{-8}(T - 1123.15)^2] \tag{2.10}
\]

\[
\varepsilon = 5.65 \cdot 10^{-1} - 5 \cdot 10^{-5}(T - 273.15) \tag{2.11}
\]

In addition to Eqs. (2.6) and (2.10), Kirillov et al. (2007) have recommended another correlation, shown as Eqs. (2.12) and (2.13), for the calculation of the thermal conductivity of UC in W/m K. Figure 2.19 shows the thermal conductivity calculated using Eqs. (2.6), (2.10), (2.12), and (2.13) as a function of temperature. In the current study, Eq. (2.10) have been used to determine the thermal conductivity of UC for the calculation of the UC fuel centerline temperature at SCWR conditions, because this equation provides the lowest thermal conductivity values for a wide temperature range, leading to a conservative calculation of the fuel centerline temperature. In Eqs. (2.12) and (2.13), \( T \) is in degrees Kelvin.

\[
k = 21.7 - 3.04 \cdot 10^{-3}(T - 273.15) + 3.61 \cdot 10^{-6}(T - 273.15)^2, 323 < T < 973 \text{ K} \tag{2.12}
\]

\[
k = 20.2 + 1.48 \cdot 10^{-3}(T - 273.15), 973 < T < 2573 \text{ K} \tag{2.13}
\]
Leitnaker and Godfrey (1967) have conducted experiments on UC in a temperature range between 298.15 K and 2800 K. As a result, they have provided Eqs. (2.14) and (2.15), which can be used in order to calculate the specific heat and the enthalpy of UC based on the results of Leitnaker and Godfrey (1967), where $T$ is in degrees Kelvin and the specific heat and enthalpy are in J/kg K and J/kg, respectively. The average percent error associate with Eq. (2.15) is ± 0.84 %.

$$c_p = 6 \cdot 10^{-15}T^5 - 6 \cdot 10^{-11}T^4 + 2 \cdot 10^{-7}T^3 - 3 \cdot 10^{-4}T^2 + 0.2655 T + 147.34 \quad (2.14)$$

$$H(T) - H(298 K) = \frac{4184}{250.04} \left[ 14.430 T - 1.074 \cdot 10^{-3}T^2 + 1.890 \cdot 10^5 T^{-1} + 3.473 \cdot 10^{-5}T^{5/2} - 4.894 \cdot 10^3 \right] \quad (2.15)$$
The linear thermal expansion of UC, in 1/K, for a temperature range of 0 to 2000°C can be calculated using a correlation shown as Eq. (2.16) (IAEA, 2008) with an uncertainty of ±15%. In Eq. (2.16), \( T \) is in degrees Kelvin. Figure 2.20 shows the variations in the specific heat, enthalpy, and linear thermal expansion of UC as functions of temperature.

\[
\alpha = 1.007 \cdot 10^{-5} + 1.17 \cdot 10^{-9}(T - 273.15) \tag{2.16}
\]

Figure 2.20: Thermodynamic properties of UC as function of temperature (IAEA, 2008; Leitnaker and Godfrey, 1967).

Frost (1963) has developed a correlation shown as Eq. (2.17), which can be used to determine the diametric increase of UC fuel as a function of time-averaged fuel centerline temperature. According to Eq. (2.17), UC fuel undergoes significant swelling for temperatures above 1000°C. In Eq. (2.17), \( R_D \) and \( T \) are percent diametric increase per atom % burn-up and time-averaged fuel centerline temperature in K, respectively. Additionally, Harrison (1969) provides the volumetric swelling of UC as a function of
burn-up for various temperatures. Figure 2.21 shows the result of the analysis conducted by Harrison (1969) on the volumetric swelling of UC.

\[ R_D = 0.6 + 0.77 \left( \frac{9 T}{5000} - 1 \right) \]  \hspace{1cm} (2.17)

Figure 2.21: Volumetric swelling of UC as function of temperature and burn-up (based on Harrison, 1969).

Stellrecht et al. (1968) have developed a correlation, shown as Eq. (2.18), which can be used to determine the compressive creep rate of UC in 1/hr. This correlation was developed specifically based on data obtained on hyper-stoichiometric UC (e.g., UC\textsubscript{1.08}). Seltzer et al. (1975) have studied the effects of deviation from stoichiometry on the creep rate of UC and found that the creep rate decreases by increasing the C/U atomic ratio due to precipitation strengthening. Tokar et al. (1970) also demonstrate that the creep rate is higher for hypo-stoichiometric UC than hyper-stoichiometric UC due to the existence of free uranium in the microstructure of hypo-stoichiometric UC. However, this reduction
in the creep rate depends on temperature and only exhibits at temperatures up to 1700°C. Figure 16 shows the creep rate of UC as a function of temperature for several selected stress values. In Eq. (2.18), $\sigma$, $R$, and $T$ are in MPa, cal/K mol, and Kelvin, respectively. As shown in Figure 2.22, the creep rate increases as the temperature increases; this indicates that the creep rate proportionally depends on temperature. Additionally, the increase in temperature changes the creep mechanism from vacancy migration to dislocation motion (Tokar et al., 1970).

$$
\dot{\varepsilon} = 1.8 \cdot 10^{-3}(\sigma/6894.76)^3 e^{-\left(\frac{90000}{RT}\right)}
$$  \hspace{1cm} (2.18)

Figure 2.22: Creep rate of UC as function of temperature (Stellrecht et al., 1968).

2.7.2.2 UC$_2$

Uranium Dicarbide (UC$_2$) is a carbide of uranium, which has a high melting point and a high thermal conductivity. UC$_2$ has a Body-Centered Tetragonal (BCT) crystal structure up to the transformation temperature of 1820±20°C, where it transforms to a Face-Centered Cubic (FCC) structure, similar to that of UO$_2$ (Frost, 1963). Frost (1963) has indicated that UC$_2$ has always been found in hypo-stoichiometric forms such as UC$_{1.75}$. 
The most common and probable composition of Uranium Dicarbide is UC₁.₈, which is often written as UC₂ (Frost, 1963).

The thermodynamic properties of UC₂ have been studied by several authors. Coninck et al. (1976) conducted experiments on UC₂, and provided correlations for the calculation of the thermal diffusivity, thermal conductivity, and emissivity of UC₂ as functions of temperature. Coninck et al. (1976) used the modulated electron beam technique in order to determine the thermal diffusivity of UC₂ samples. In this technique, an electron gun is used to bombard a material in the form of a thin solid plate from one face. The electron gun is modulated to vary sinusoidally as a function of time. The phase difference between the temperature fluctuations of the two faces of the plate is measured, which is used to determine the thermal diffusivity of the material (Wheeler, 1965). Then, thermal conductivity is calculated as the multiplication of thermal diffusivity, density and specific heat as shown in Eq. (2.3).

Coninck et al. (1976) have developed two correlations shown as Eqs. (2.19) through (2.22) for the calculation of the thermal diffusivity, in m²/s, and thermal conductivity, in W/ m K, of the nearly stoichiometric UC₂. The correlations for slightly hypo-stoichiometric UC₂ and hypo-stoichiometric UC₂ have been shown as Eqs. (2.23) through (2.28). In Eqs. (2.19) through (2.28), T is in degrees Kelvin.

\[
\alpha = 10^{-4} \cdot [0.0398 - 1.775 \cdot 10^{-6}(T - 273.15) - 8.65 \cdot 10^{-10}(T - 273.15)^2], \ 873 < T < 2013 \text{ K} \tag{2.19}
\]

\[
\alpha = 0.0375 \cdot 10^{-4}, \ 2103 < T < 2333 \text{ K} \tag{2.20}
\]

\[
k = 100 \cdot [0.115 + 2.7 \cdot 10^{-5}(T - 273.15) + 2.8 \cdot 10^{-10}(T - 273.15)^2 + 3.035 \cdot 10^{-12}(T - 273.15)^3], \ 873 < T < 2013 \text{ K} \tag{2.21}
\]

\[
k = 100 \cdot [0.082 + 5.64 \cdot 10^{-5}(T - 273.15)], \ 2103 < T < 2333 \text{ K} \tag{2.22}
\]
\textit{Slightly hypo-stoichiometric:}
\[
\alpha = 10^{-4} \cdot [0.0454 - 4.73 \cdot 10^{-6}(T - 273.15) - 5.8 \cdot 10^{-10}T^2], \ 873 < T < 1993 \text{ K} \\
(2.23)
\]
\[
\alpha = 0.045 \cdot 10^{-4}, \ 2093 < T < 2343 \text{ K} \\
(2.24)
\]
\[
k = 100 \cdot [0.1182 + 2.895 \cdot 10^{-5}(T - 273.15) + 3.8 \cdot 10^{-9}(T - 273.15)^2 + 1.9 \cdot 10^{-12}(T - 273.15)^3], \ 873 < T < 1993 \text{ K} \\
(2.25)
\]
\[
k = 100 \cdot [0.102 + 4.88 \cdot 10^{-5}(T - 273.15)], \ 2093 < T < 2343 \text{ K} \\
(2.26)
\]

\textit{Hypo-stoichiometric UC}_2:
\[
\alpha = 10^{-4} \cdot [0.043 - 1.9 \cdot 10^{-6}(T - 273.15) - 1.2 \cdot 10^{-9}(T - 273.15)^2 - 4.11 \cdot 10^{-13}(T - 273.15)^3], \ 873 < T < 1973 \text{ K} \\
(2.27)
\]
\[
k = 100 \cdot [0.132 + 1.9 \cdot 10^{-5}(T - 273.15) + 4.3 \cdot 10^{-9}(T - 273.15)^2], \ 873 < T < 19793 \text{ K} \\
(2.28)
\]

Figures 2.23 and 2.24 show the thermal conductivity and thermal diffusivity for stoichiometric, slightly hypo-stoichiometric, and hypo-stoichiometric UC\textsubscript{2} as functions of temperature. As shown in Figure 2.23, the deviation from stoichiometry does not significantly change the thermal conductivity of UC\textsubscript{2}. For all cases, the thermal conductivity increases, and the thermal diffusivity (see Fig. 2.24) decreases as temperature rises.
Figure 2.23: Thermal conductivity for stoichiometric, slightly hypo-stoichiometric, and hypo-stoichiometric UC$_2$ as function of temperature (Coninck et al., 1976).

Figure 2.24: Thermal diffusivity for stoichiometric, slightly hypo-stoichiometric, and hypo-stoichiometric UC$_2$ as function of temperature (Coninck et al., 1976).
Leitnaker and Godfrey (1967) have conducted experiments on a mixture consisting of 5.5% UC, 94.5% UC$_{1.91}$, and 7% carbon in a temperature range between 25 and 2727°C. They have provided the values of the specific heat of the mixture as shown in Fig. 2.25. Equation (2.29) can be used in order to calculate the specific heat of the mixture in a temperature range between 25 - 1787°C, where the temperature is in Kelvin and the specific heat is in J/kg K. Moreover, Leitnaker and Godfrey (1967) provided Eqs. (2.30) and (2.31), which can be used for the calculation of enthalpy in J/kg. Equations (2.30) and (2.31) are valid for temperature ranges between 25 - 1787°C and 1787 - 2308°C, respectively. The average percent errors associated with Eqs. (2.30) and (2.31) are ±0.25 and ± 0.30%, respectively (Leitnaker and Godfrey, 1967).

\[ c_p = -1 \cdot 10^{-10}T^4 + 7 \cdot 10^{-7}T^3 - 11 \cdot 10^{-4}T^2 + 0.8401T + 65.088 \]  \hspace{1cm} (2.29)

\[ H(T) - H(298 \text{ K}) = \frac{4184}{262.05} \cdot [4.076T + 2.631 \cdot 10^{-2}T^2 - 2.332 \cdot 10^{-5}T^3 + 1.025 \cdot 10^{-8}T^4 - 1.573 \cdot 10^{-12}T^5 - 3.013 \cdot 10^3], 298.15 < T < 2060 \text{ K} \] \hspace{1cm} (2.30)

\[ H(T) - H(298 \text{ K}) = \frac{4184}{262.05} \cdot [-2.512T + 6.894 \cdot 10^{-3}T^2 + 1.806 \cdot 10^4], 2060 < T < 2581 \text{ K} \] \hspace{1cm} (2.31)
2.7.2.3 UN

Uranium mononitride or uranium nitride (UN), which is a ceramic fuel, can be produced by the carbothermic reduction of uranium dioxide plus carbon in nitrogen. This process produces UN with densities in the range of 65 to 90% of TD (Shoup and Grace, 1977). UN has a high melting point, high thermal conductivity, and high radiation stability. These properties enhance the safety of operation and allow the fuel to achieve high burn-ups (IAEA, 2008). Additionally, UN has the highest fissile atom density, which is approximately 1.4 times that of UO$_2$ and greater than those of other examined fuels. In other words, when UN is used as a fuel, a smaller volume of fuel is required, which leads to a smaller core. In contrast, one disadvantage of the UN fuel is that under some conditions it decomposes to liquid uranium and gaseous nitrogen (IAEA, 2008), which in turn results in the formation of cracks in the fuel. These cracks increase the chance of the release of gaseous fission products. Additionally, the formation of cracks in nuclear fuels has adverse effects on their mechanical and thermophysical properties.
It is significantly important to establish a temperature-pressure relationship for the melting of UN fuel in order to establish temperature limits for UN fuel elements. UN melts congruently at high nitrogen pressures. In contrast, at high nitrogen pressures, UN melts incongruently, which means UN decomposes to liquid uranium and releases nitrogen gas. Therefore, it is expected to measure low UN vapor pressure over UN fuel due to its tendency to decompose. In comparison with UO$_2$ fuel, the vapor pressure of UN over UN fuel is four orders of magnitude less than the vapor pressure of UO$_2$ over UO$_2$ fuel. UN fuel melts congruently at high partial pressures of nitrogen; however, the decomposition of UN occurs at low nitrogen partial pressures. Therefore, the partial pressure of UN fuel is an indication of melting or decomposition of the fuel, which in turn can be used to establish engineering limits for UN fuel (Hayes et al., 1990c).

Hayes et al. (1990c) have developed an empirical correlation shown as Eq. (2.32), which can be used to calculate the melting point of UN, in degrees Kelvin, as a function of partial pressure of nitrogen that depends on temperature. Equation (2.32) is valid when the partial pressure of nitrogen is between $10^{-8}$ and $10^5$ Pa. Partial pressure of nitrogen in Eq. (2.32) can be calculated using Eq. (2.33). Additionally, Eq. (2.34) can be used in order to calculate the vapor pressure of uranium over UN in Pascal (Hayes et al., 1990c). The total vapor pressure over UN is the sum of the partial pressures of N$_2$ and U. Figures 2.26, 2.27 and 2.28 show the partial pressures of nitrogen and uranium over UN as functions of temperature, and the melting point of UN as a function of partial pressure of nitrogen over UN, respectively.

$$T_m = 3035.0 \left( \frac{P_{N_2}}{1.01 \cdot 10^5} \right)^{0.02832}$$  

(2.32)

$$\log_{10}(P_{N_2}) = 1.01 \cdot 10^5(1.8216 + 1.882 \cdot 10^{-3}T - 23543.4/T), \; 1400 < T < 3170 \; \text{K}$$  

(2.33)

$$\log_{10}(P_U) = 1.01 \cdot 10^5(6.9654 - 5.137 \cdot 10^{-4}T - 26616.1/T), \; 1400 < T < 2400 \; \text{K}$$  

(2.34)
Figure 2.26: Vapor pressure of nitrogen as function of temperature.

Figure 2.27: Vapor pressure of uranium as function of temperature.
Ross et al. (1988) have developed a correlation, shown as Eq. (2.35), for the calculation of the thermal conductivity of UN, in W/m K. This correlation, which has an uncertainty within ±10%, calculates the thermal conductivity of UN fuel with 100% of TD. In general, nuclear fuels are manufactured with porosity to accommodate for the gaseous fission products. Therefore, it is necessary to determine the thermal conductivity of a fuel based on its porosity. Kikuchi et al. (1972) have developed a correlation, shown as Eq. (2.36), which can be used to calculate the effective thermal conductivity of porous UN fuel as a function of percent porosity. In Eq. (2.36), the coefficient β is independent of temperature and has a value of 1.79±0.05 for porosities below 10%. Nevertheless, β becomes temperature dependent when porosity increases beyond 12%. The value of β varies from 1.38±0.12 at 300°C to -0.09±0.05 at 1300°C (Kikuchi et al., 1972).

\[ k_{100\%TD} = 1.37 \; T^{0.41} \]  
\[ k_p = k_{100\%TD} \left( \frac{1-P}{1+\beta P} \right) \]
In addition to the Ross et al. correlation, Hayes et al. (1990a) developed another correlation shown as Eq. (2.37), which calculates the thermal conductivity of UN, in W/m K. This correlation, which is a function of both temperature and percent porosity, can be applied when porosity changes between 0 and 20% for temperatures in the range of 25°C and 1650°C (Hayes et al., 1990a). Figure 2.29 shows the thermal conductivity of UN with 5% porosity as a function of temperature, calculated based on the two studied correlations. As shown in Fig. 2.29, the Hayes et al. correlation results in lower thermal conductivity values for temperatures approximately above 700°C. In other words, the Hayes et al. correlation is more conservative than the Ross et al. correlation in the prediction of the thermal conductivity of UN at temperatures above 700°C. Additionally, the standard deviation of the Hayes et al. correlation is ±2.3% compared to ±3.2% for the Ross et al. correlation. Therefore, as a conservative approach, the Hayes et al. correlation has been used in the calculation of the centerline temperature of UN fuel at SCWR conditions.

![Thermal conductivity of 95%TD UN fuel](image)

Figure 2.29: Thermal conductivity of 95%TD UN fuel (based on the Ross et al. (1988) and Hayes et al. (1990a) Correlations).
Hayes et al. (1990c) developed correlations for the calculation of the thermodynamic properties of UN including specific heat, enthalpy, entropy, and Gibbs free energy as functions of temperature; these correlations are shown as Eqs. (2.38) through (2.41), respectively. The specific heat and the entropy are in J/ kg K, and the enthalpy and the Gibbs free energy are in J/ kg.

\[ k = 1.864e^{-2.14P^{0.361}} \] (2.37)

The specific heat correlation is valid for temperatures between 25 and 2355°C, where \( T \) is temperature in degrees Kelvin and \( \Theta \) is the empirically determined Einstein temperature, which is 365.7 K for UN. Figure 2.30 shows the selected thermodynamic properties of UN.

\[
c_p = \frac{1000}{252.04} \cdot \left[ 51.14(\Theta/T) \frac{\exp(\Theta/T)}{[\exp(\Theta/T)-1]^2} + 9.491 \cdot 10^{-3} T + \frac{2.642 \times 10^{11}}{T^2} \exp(-18081/T) \right] \] (2.38)

\[
H(T) - H(298 \text{ K}) = \frac{1000}{252.04} \cdot \left[ \frac{51.14\Theta}{\exp(\Theta/T)-1} + 4.746 \cdot 10^{-3} T^2 - 8148.34 + 1.461 \cdot 10^7 \exp(-18081/T) \right] \] (2.39)

\[
S = \frac{1000}{252.04} \cdot \left[ 51.14(\Theta/T) \frac{\exp(\Theta/T)}{\exp(\Theta/T)-1} - 51.14 \ln\{1 - \exp(-\Theta/T)\} + 9.491 \cdot 10^{-3} T + 16.31 \right] \] (2.40)

\[
G = \frac{1000}{252.04} \cdot \left[ 51.14 T \ln\{1 - \exp(-\Theta/T)\} - 4.746 \cdot 10^{-3} T^2 - 16.31 + 1.461 \cdot 10^7 \exp(-18081/T) \right] \] (2.41)
It is essential for a fuel to maintain its structural integrity under the conditions of a nuclear reactor. In other words, the fuel must have an adequate mechanical stability and withstand stresses under operating conditions. The mechanical stability of a fuel is related to its mechanical properties. Thus, the study of mechanical properties of the fuel is an inseparable part of a safe design.

Mechanical properties of UN such as modulus of elasticity, shear modulus, and Poisson’s ratio can be determined using Eqs. (2.42) through (2.44), where $E$, $G$, $\nu$, and $TD$ are the Young’s modulus, Shear modulus, Poisson’s ratio, and theoretical density (e.g., $TD = 95$ for a fuel with a 95% theoretical density), respectively (Hayes et al., 1990b). Figure 2.31 shows the Young’s modulus and shear modulus of UN, both in MPa, as functions of temperature for 95% TD UN. Equations (2.42) through (2.44) were developed based on percent theoretical densities between 70 and 100%; however, they can be used for fuels with higher porosities. Additionally, Hayes et al. (1990b) provided a correlation, shown as Eq. (2.45), for the calculation of the hardness of UN in MPa. The latter correlation is valid for temperatures in the range of 25 and 1400°C, and porosities between 0.0 and 0.26.
Moreover, the density and linear expansion coefficient of UN, in kg/m$^3$ and 1/K, can be calculated using Eqs. (2.46) and (2.47) respectively, which are valid for temperatures between 25 and 2250°C (IAEA, 2008). Figure 2.32 shows the linear thermal expansion of UN as a function of temperature. In Eqs. (2.38) through (2.47), $T$ is in degrees Kelvin.

\[
E = 0.258 T D^{3.002} [1 - 2.375 \cdot 10^{-5} T], \quad 298 \text{ K} < T < 1473 \text{ K} \tag{2.42}
\]

\[
G = 1.44 \cdot 10^{-2} T D^{3.446} [1 - 2.375 \cdot 10^{-5} T], \quad 298 \text{ K} < T < 1473 \text{ K} \tag{2.43}
\]

\[
\nu = 1.26 \cdot 10^{-3} T D^{1.174}, \quad 298 \text{ K} < T < 1473 \text{ K} \tag{2.44}
\]

\[
HD = 9.807 \cdot [951.8(1 - 2.1P)\exp(-1.882 \cdot 10^{-3} T)] \tag{2.45}
\]

\[
\alpha = 7.096 \cdot 10^{-6} + 1.409 \cdot 10^{-9} T \tag{2.46}
\]

Figure 2.31: Young's and Shear moduli of UN with 95% TD as function of temperature.
Irradiation swelling, growth, and creep are the primary effects of irradiation on the fuel. Irradiation swelling results in volumetric instability of the fuel at high temperatures while irradiation growth causes dimensional instability of the fuel at temperatures lower than 2/3 of the melting point of the fuel (Ma, 1983). In addition to dimensional and volumetric instability, a continuous and plastic deformation of the fuel due to creep may adversely affect its mechanical properties. Thus, it is required to study the behaviour of the fuel under irradiation specifically the irradiation-induced swelling, irradiation-induced growth and irradiation-induced creep of the fuel.

Ross et al. (1990) have developed a correlation for the calculation of the percent volumetric swelling of UN fuel. This correlation is shown as Eq. (2.48), where $T_{\text{avg}}$ is the volume average fuel temperature in K, $B$ is the fuel burn-up in MW day/M g(U), and $\rho_{\%TD}$ is the percent theoretical density of the fuel (e.g., $\rho_{\%TD}$ equals to 0.95 for a fuel with 5% porosity). In addition to this correlation, the volumetric swelling of UN can be calculated based on the fuel centerline temperature using Eq. (2.49) (Ross et al., 1990). The uncertainty of the volumetric swelling correlation, Eq. (2.49), is ±25% for burn-ups above 10,000 MW day/Mg (U). On the other hand, the uncertainty associated with this correlation increases to ±60% at lower burn-ups (Ross et al., 1990). Figure 2.33 shows the volume expansion of UN as a function of temperature for selected burn-up values.

\[
\rho = 14420 - 0.2779T - 4.897 \cdot 10^{-5}T^2
\]  

(2.47)
Figure 2.32: Linear thermal expansion of UN as function of temperature (based on IAEA, 2008).

Figure 2.33: Percent volumetric swelling of UN as function of burn-up and temperature (based on Ross et al., 1990).
\[
\Delta V/V (%) = 4.7 \cdot 10^{-11} \tau_{avg}^{3.12} \left( \frac{B}{9008.1} \right)^{0.83} \rho_{TD}^{0.5}
\] (2.48)

\[
\Delta V/V (%) = 1.16 \cdot 10^{-8} \tau_{CLT}^{2.36} \left( \frac{B}{9008.1} \right)^{0.82} \rho_{TD}^{0.5}
\] (2.49)

Additionally, Hayes et al. (1990b) have developed a correlation shown as Eq. (2.50) which gives the steady-state creep rate of dense UN with 100% TD, in 1/hr. This correlation is valid for temperatures between 1497 and 1810°C and stresses, \( \sigma \), in the range of 20 to 34 MPa. To account for the porosity of the fuel, Eq. (2.50) should be multiplied by the creep porosity correlation factor shown as Eq. (2.51) (Hayes et al., 1990b). In Eq. (2.51), \( P \) is the porosity in volume fraction. Figure 2.34 shows the creep of UN with 100% TD and 95% TD as a function of temperature for a stress value of 25 MPa. Figure 2.34 also indicates that the creep rate increases by increasing the porosity.

![Figure 2.34: Steady-state creep rate of UN at 25 MPa stress as function of temperature (based on Hayes et al., 1990b).](image)
\[ \dot{\varepsilon} = 3600 \cdot 2.054 \cdot 10^{-3} \sigma^{4.5} \exp(-39369.5/T) \]  

(2.50)

\[ f(P) = \frac{0.987}{(1-P)^{27.6}} \exp(-8.65 P) \]  

(2.51)

### 2.7.3 Composite Fuels with Enhanced Thermal-Conductivity

Currently, there is a high interest in developing high thermal-conductivity fuel, and improving the thermal conductivity of low thermal-conductivity fuels such as UO\(_2\). High thermal conductivities result in lower fuel centerline temperatures and limit the release of gaseous fission products (Hollenbach and Ott, 2010). As shown previously, UO\(_2\) has a very low thermal conductivity at high temperatures compared to other fuels such as UC, UC\(_2\), and UN. However, there is a possibility to increase the thermal conductivity of UO\(_2\). This increase in the thermal conductivity of UO\(_2\) can be performed either by adding a continuous solid phase or long, thin fibers of a high thermal-conductivity material (Hollenbach and Ott, 2010; Solomon et al., 2005).

A high thermal-conductivity material must have a low thermal-neutron absorption cross-section, assuming that the fuel will be used in a thermal-spectrum nuclear reactor (Hollenbach and Ott, 2010). Additionally, it must have a high melting point and be chemically compatible with the fuel, the cladding, and the coolant. The need to meet the aforementioned requirements narrows the potential materials to silicon carbide (SiC), beryllium oxide (BeO), and graphite (C). The following sections provide some information about UO\(_2\) fuel composed of the aforementioned high thermal-conductivity materials.

#### 2.7.3.1 UO\(_2\) - SiC

The thermal conductivity of UO\(_2\) fuel can be improved by incorporating silicon carbide (SiC) into the matrix of the fuel. SiC has a high melting point approximately at 2800°C, high thermal conductivity (78 W/m K at 727°C), high corrosion resistance even at high temperatures, low thermal neutron absorption, and dimensional stability (Khan et al., 2010). Therefore, when used with UO\(_2\), SiC can address the problem of poor thermal conductivity of UO\(_2\) fuel.
Calculation of the thermal conductivity of UO$_2$ plus SiC fuel falls under the theories of composites. Generally, theories contemplating the thermal conductivity of composites are classified into two categories. One category assumes that inclusions are randomly distributed in a homogeneous mixture. The effective thermal conductivities (ETC) of the composites, based on the aforementioned principle, are formulated by Maxwell. The other category, which is based on the work performed by Rayleigh, assumes that particles are distributed in a regular manner within the matrix.

Khan et al. (2010) provided the thermal conductivity of UO$_2$–SiC fuel as a function of temperature and weight percent of SiC. Khan et al. (2010) assumed that the thin coat of SiC covered UO$_2$ particles and determined the thermal conductivity of the composite fuel for three cases. These cases, which are described in the following paragraph, were solved based on the Rayleigh equation shown as Eq. (2.52) (Khan et al., 2010).

\[
k_{\text{eff}}_R(\psi) = k_{\text{SiC}} \cdot \left[ 1 + 3 \frac{k_{\text{UO}_2} + 2k_{\text{SiC}}}{3k_{\text{UO}_2} - k_{\text{SiC}}} \psi + 1.569 \frac{k_{\text{UO}_2} - k_{\text{SiC}}}{3k_{\text{UO}_2} - 4k_{\text{SiC}}} \psi^{10/3} \right]
\] (2.52)

In Case I, it was assumed that all UO$_2$ particles are completely covered within a layer of SiC. In Case II, the coating on UO$_2$ particles is not complete. In other words, it was assumed that there were blocks of UO$_2$ covered with SiC along the radial direction of the fuel. Finally, in Case III, it was assumed that there were blocks of UO$_2$ coated with SiC. The SiC coating in the latter case was discontinued such that SiC covered only two opposite sides of each UO$_2$ block.

For all three examined cases, the thermal conductivities were calculated for 97% TD and when the weight percent of SiC was 12 % and 8 %. The results indicate a small difference between the ETC of Case I and Case II. This small difference was due to the continuity of SiC layer in Case I and II. However, in Case III, the discontinuity of SiC resulted in little improvement in the ETC of the fuel. Therefore, the addition of a continuous solid phase of SiC to UO$_2$ fuel increases the ETC of the fuel. In the present
study, UO$_2$–SiC fuel with 12wt% SiC has been examined and its thermal conductivity has been calculated using Eq. (2.53). Equation (2.53) has been developed based on the analysis conducted for Case I.

$$k_{eff} = -9.59 \times 10^{-9}T^3 + 4.29 \times 10^{-5}T^2 - 6.87 \times 10^{-2}T + 4.68 \times 10^1$$  \hspace{1cm} (2.53)

2.7.3.2 UO$_2$-C

Hollenbach and Ott (2010) have studied the effects of the addition of graphite fibers on thermal conductivity of UO$_2$ fuel. Theoretically, the thermal conductivity of graphite varies along different crystallographic planes. For instance, the thermal conductivity of perfect graphite along basal planes is more than 2000 W/m K (Hollenbach and Ott, 2010). On the other hand, it is less than 10 W/m K in the direction perpendicular to the basal planes. Hollenbach and Ott (2010) have performed computer analyses in order to determine the effectiveness of adding long, thin fibers of high thermal-conductivity materials to low thermal-conductivity materials to determine the effective thermal conductivity. In their studies, the high thermal-conductivity material had a thermal conductivity of 2000 W/m K along the axis, and a thermal conductivity of 10 W/m K radially, similar to perfect graphite. The low thermal-conductivity material had properties similar to UO$_2$ (e.g., with 95% TD at ~1100°C) with a thermal conductivity of 3 W/m K.

Hollenbach and Ott (2010) have examined the effective thermal conductivity of the composite for various volume percentages of the high thermal-conductivity material, varying from 0 to 3%. Figure 2.35 shows that the addition of just one volume percent of high thermal-conductivity material increases the effective thermal conductivity of the composite approximately by a factor of 5. Moreover, if the amount of the high thermal-conductivity material increases to 2% by volume, the effective thermal conductivity of the composite reaches the range of high thermal-conductivity fuels, such as UN and UC.
Figure 2.35: Thermal conductivity of UO$_2$ as function of graphite fibber volume percent (Hollenbach and Ott, 2010).

In this study, the fuel centerline temperature has been calculated at SCWR conditions for UO$_2$ fuel composed of 1% by volume graphite fibbers. Since the thermal conductivity as a function of temperature was not available, the fuel centerline temperature calculation has been conducted with a constant thermal conductivity.

2.7.3.3 UO$_2$–BeO
Beryllium Oxide (BeO) is a metallic oxide with a very high thermal conductivity. BeO is chemically compatible with UO$_2$, most sheath materials including zirconium alloys, and water. In addition to its chemical compatibility, BeO is insoluble with UO$_2$ at temperatures up to 2160°C. As a result, BeO remains as a continuous second solid phase in the UO$_2$ fuel matrix while being in good contact with UO$_2$ molecules at the grain boundaries. BeO has desirable thermochemical and neutronic properties, which have resulted in the use of BeO in aerospace, electrical and nuclear applications. For example,
BeO has been used as the moderator and the reflector in some nuclear reactors. However, the major concern with beryllium is its toxicity. But, the requirements for safe handling of BeO are similar to those of UO₂. Therefore, the toxicity of BeO is not a limiting factor in the use of this material with UO₂ (Solomon et al., 2005).

Similar to other enhanced thermal-conductivity fuels, the thermal conductivity of UO₂ can be increased by introducing a continuous phase of BeO at the grain boundaries. The effects of the present of such second solid phase on the thermal conductivity of UO₂ is significant such that only 10% by volume of BeO would improve the thermal conductivity of the composite fuel by 50% compared to that of UO₂ with 95% TD. Figure 2.36 shows the thermal conductivity of UO₂–BeO as a function of temperature for 0.9 wt%, 2.7 wt%, 10.2 wt%, 20.4 wt% of BeO (Ishimoto et al., 1995; Latta et al., 2008; McDeavitt, 2009; Solomon et al., 2005). For the purpose of this study, UO₂–BeO fuel with 13.6 wt% of BeO has been examined.
Figure 2.36: Thermal conductivity of UO$_2$–BeO as function of temperature (Ishimoto et al., 1995; Latta et al., 2008; McDeavitt, 2009; Solomon et al., 2005).
3 METHODS

3.1 Fuel Centerline Temperature Calculations
In order to calculate the fuel centerline temperature, steady-state one-dimensional heat-transfer analysis was conducted. The MATLAB and NIST REFPROP software were used for programming and retrieving thermophysical properties of a light-water coolant, respectively. First, the heated length of the fuel channel was divided into small segments of one-millimeter lengths. Second, a temperature profile of the coolant was calculated. Third, sheath-outer and inner surface temperatures were calculated. Fourth, the heat transfer through the gap between the sheath and the fuel was determined and used to calculate the outer surface temperature of the fuel. Finally, a temperature of the fuel in the radial and axial directions was calculated. It should be noted that the radius of the fuel pellet was divided into 20 segments. The results have been presented for fuel-sheath gap widths of zero, 20 μm and 36 μm. Moreover, the fuel centerline temperature profiles have been calculated based on a no-gap condition in order to determine the effect of gap conductance on the fuel centerline temperature. Figure 3.1 illustrates the methodology based on which fuel centerline temperature was calculated. The following section provides more information about each step shown in Fig. 3.1.

For verification purposes, the MATLAB code, which has been presented in Appendix C, has been compared against calculations conducted in an Excel spreadsheet (see Appendix D for results). The result of this comparison shows that the bulk coolant and sheath wall temperatures of the MATLAB code matched those of the Excel calculations. However, there is 5% discrepancy in fuel centerline temperatures. This discrepancy is mostly due to the fact that the radius of the fuel was divided into 10 segments in Excel calculations compared to 20 segments in the MATLAB code for simplicity. Therefore, this comparison shows the validity of the MATLAB code.
As shown in Fig. 3.1, the convective heat transfer between the sheath and the coolant is the only heat transfer mode which has directly been taken into consideration. In radiative heat transfer, energy is transferred in the form of electromagnetic waves. Unlike convection and conduction heat transfer modes in which the rate of heat transfer is linearly proportional to temperature differences, a radiative heat transfer depends on the difference between absolute temperatures to the fourth power. The sheath temperature is high\(^{18}\) at SCWR conditions; therefore, it is necessary to take into account the radiative heat transfer.

In the case of the sheath and the coolant, the radiative heat transfer has been taken into consideration through the Nusselt number correlation, which has been used to calculate the HTC. In general, the Nusselt number correlations are empirical equations, which are developed based on experiments conducted in water using either bare tubes or tubes containing electrically heated elements simulating the fuel bundles. To develop a correlation, surface temperatures of the bare tube and/or simulating rods are measured.

\(^{18}\) It might be as high as 850°C.
along the heated length of the test section by the use of thermocouples or Resistance Temperature Detectors (RTDs). These measured surface temperatures already include the effect of the radiative heat transfer; therefore, the developed Nusselt number correlations represents both radiative and convection heat transfer modes. Consequently, the radiative heat transfer has been taken indirectly into consideration in the calculations through Eq. (3.2).

3.1.1 Bulk-Fluid Temperature Profile
The temperature profile of the coolant along the heated length of the fuel channel can be calculated based on the heat balance. Equation (3.1) was used to calculate the temperature profile of the coolant. The NIST REPFROP software Version 8.0 was used to determine the thermophysical properties at a bulk-fluid temperature corresponding to each one-millimeter interval.

\[ h_2 = h_1 + \frac{n q_x}{m} \cdot \Delta x \]  

(3.1)

3.1.1.1 Axial Heat Flux Profiles
In Eq. (3.1), \( q_x \) is the axial heat flux value, which is variable along the heated length of the fuel channel if a non-uniform Axial Heat Flux Profile (AHFP) is used. In the present study, four AHFPs have been applied in order to calculate the fuel centerline temperature in fuel channels at the maximum channel thermal power. These AHFPs are cosine, upstream-skewed cosine, downstream-skewed cosine, and uniform. The aforementioned AHFPs were calculated based on profiles listed in Leung (2008) while the downstream-skewed AHFP was determined as the mirror image of the upstream-skewed AHFP.

It should be noted that there are many power profiles in a reactor core. In other words, the axial heat flux profile in each fuel channel differs from those of the other fuel channels. This variation in power profiles is due to the radial and axial power distribution, fuel burn-up, presence of reactivity control mechanisms, and refuelling scheme. However, the four examined AHFPs envelope a wide range of power profiles,
eliminating the need to determine the sheath and fuel centerline temperatures for all possible AHFPs.

The maximum channel thermal power was assumed to be +15% (10% variation and 5% uncertainties) above the average thermal power (8.5 MW). Therefore, 9.8 MW was used as the maximum channel thermal power. The power ratio has been defined as the ratio of the local heat flux to the average heat flux, where the average heat flux was calculated based on a generic 43-element fuel bundle. This fuel bundle consists of 42 fuelled elements with an outer diameter of 11.5 mm and a central unheated element (20-mm OD) filled with burnable poison. Figures 3.2 and 3.3 show the power ratios and AHFPs used in the current research, respectively.

Figure 3.2: Power ratios for fuel channel with average channel power (based on Leung (2008)).
3.1.2 Sheath Temperature

The calculation of the sheath temperature requires HTC values along the heated length of the fuel channel. In our calculation, the Mokry et al. correlation, shown as Eq. (3.2), has been used to determine HTC. The experimental data, based on which this correlation was developed, was obtained within conditions similar to those of proposed SCWR concepts. The experimental dataset was obtained for supercritical water flowing upward in a 4-m-long vertical bare tube. The data was collected at a pressure of approximately 24 MPa for several combinations of wall and bulk fluid temperatures. The temperatures were below, at, or above the pseudocritical temperature. The mass flux ranged from 200-1500 kg/m²s; coolant inlet temperature varied from 320 to 350°C, for heat flux up to 1250 kW/m² (Mokry et al., 2009). This correlation requires iterations to be solved, because it contains two unknowns, which are HTC and sheath wall temperature. To solve this problem through iterations, Newton’s law of cooling, shown as Eq. (3.3), was used.

From a safety point of view, it is necessary to know the uncertainty of a correlation in calculating the HTC and sheath wall temperature. As shown in Fig. 3.4, the uncertainty associated in the prediction of the HTC using the Mokry et al. correlation is ±25%. In
other words, the HTC values calculated by the Mokry correlation are within ±25% deviation from the corresponding experimental values. However, the uncertainty associated with wall temperature is smaller and lies within ±15%. Figure 3.5 shows the uncertainty in the prediction of the wall temperature associated with the Mokry et al. correlation.

![Graph showing uncertainty in HTC prediction](image)

**Figure 3.4:** Uncertainty in predicting HTC for the Mokry et al. correlation (Mokry et al., 2011).
Figure 3.5: Uncertainty in predicting wall temperature using the Mokry et al. correlation (Mokry et al., 2011).

3.1.2.1 Outer-Surface Temperature of Sheath
The following sequence of equations has been used in order to calculate the outer surface temperature of the sheath along the heated length of the fuel channel.

Assumption to start the iteration: $T_{sheath-wall,o} = T_{bulk} + 50^\circ\text{C}$

$$
\text{Nu}_b = 0.0061 \text{ Re}_b^{0.904} \text{ Pr}_b^{0.684} \left( \frac{\rho_w}{\rho_b} \right)^{0.564}
$$

(3.2)

$$
q = h \left( T_{sheath-wall,o} - T_{bulk} \right)
$$

(3.3)

The developed MATLAB code uses an iterative technique to determine the sheath-wall temperature. Initially, the sheath-wall temperature is unknown. Therefore, an initial guess is needed for the sheath-wall temperature (i.e., $50^\circ\text{C}$ above the bulk-fluid temperature). Then, the code calculates the HTC using Eq. (3.2), which requires the thermophysical properties of the light-water coolant at bulk-fluid and sheath-wall
temperatures. Next, the code calculates a “new” sheath-wall temperature using the Newton’s law of cooling shown as Eq. (3.3). In the next iteration, the code uses an average temperature between the guessed temperature and the most recent calculated temperature as the guess temperature. The iterations continue until the difference between the two consecutive temperatures is less than 0.1 K. It should be noted that the initial guessed sheath-wall temperature could have any value regardless of the value the temperature converges. The only difference caused by different guess sheath-wall temperatures is in the number of iterations and required time to complete the execution of the code.

As mentioned previously, the thermophysical properties of the coolant undergo significant changes as the temperature passes through the pseudocritical point. Since the operating pressure of the coolant is 25 MPa, the pseudocritical point is reached at 384.9°C. As shown in Fig. 3.6, the changes in the thermophysical properties of the coolant were captured by the Nusselt number correlation, Eq. (3.2). The Prandtl number in Eq. (3.2) is responsible for taking into account the thermophysical properties of the coolant. Figure 3.6 shows the thermophysical properties of the light-water coolant along the length of the fuel channel. The use of these thermophysical properties in the Nusselt number correlation indicates that the correlation takes into account the effect of the pseudocritical point on the HTC between the sheath and the coolant.
3.1.2.2 Sheath-Thickness Calculations

The operating pressure of the reactor coolant is approximately 25 MPa, which requires that the thickness of the sheath to be such to withstand this high pressure. Therefore, the minimum required thickness of the sheath should be determined. Equation (3.4) was used to calculate the minimum required thickness of the sheath. This calculation is based on collapse pressure and has been conducted for an empty sheath. Inconel-600 was assumed to be the material of the sheath. The Young’s modulus of elasticity and Poisson’s ratio of Inconel-600 were obtained from Special Metals (2008). Figure 3.7 shows the required thickness of the sheath as a function of temperature. As shown in Fig. 3.7, the required thickness of the sheath is approximately 0.48 mm at 850°C.

\[
P_{cr} = \frac{2E}{(1-\nu^2)} \left( \frac{t}{D_o} \right)^3
\]

(3.4)
3.1.2.3 Inner-Sheath Temperature
The inner surface temperature of the sheath was calculated using Eq. (3.5). In Eq. (3.5), $k$ is the thermal conductivity of the sheath, which was calculated based on the average temperature of the outer and inner wall surface temperatures. This calculation was conducted through the use of an iteration, which required an initial guess for the inner surface temperature of the sheath.

$$Q = \frac{T_{\text{sheath-wall}i} - T_{\text{sheath-wall}o}}{\ln\left(\frac{r_i}{r_o}\right) - \frac{2\pi L k}{}}$$  \hspace{1cm} (3.5)

3.1.3 Gap Conductance
Heat transfer through the fuel-sheath gap is governed by three primary mechanisms (Lee et al., 1995).

1- Conduction through the gas
2- Conduction due to fuel-sheath contacts
3- Radiation

There are several models for the calculation of heat transfer rate through the fuel-sheath gap. These models are as follows (Lee et al., 1995):

- Offset gap conductance model
- Relocated gap conductance model
- Ross and Stoute model
- Modified Ross and Stoute model

In the present study, the modified Ross and Stoute model has been used in order to determine the gap conductance effects on the fuel centerline temperature. In this model, the total heat transfer through the gap is calculated as the sum of the three aforementioned terms as represented in Eq. (3.6):

\[ h_{total} = h_g + h_c + h_r \] (3.6)

The heat transfer through the gas in the fuel-sheath gap is by conduction because the gap width is very small. This small gap width does not allow for the development of natural convection though the gap. The heat transfer rate through the gas is calculated using Eq. (3.7).

\[ h_g = \frac{k_g}{1.5(R_1+R_2)+t_g+g} \] (3.7)

Where, \( h_g \) is the conductance through the gas in the gap, \( k_g \) is the thermal conductivity of the gas, \( R_1 \) and \( R_2 \) are the surface roughnesses of the fuel and the sheath, and \( t_g \) is the circumferentially average fuel-sheath gap width.

The fuel-sheath gap is very small, in the range between 0 and 125 \( \mu m \) (Lassmann and Hohlefeld, 1987). CANada Deuterium Uranium (CANDU) reactors use collapsible sheath, which leads to small fuel-sheath gaps approximately 20 \( \mu m \) (Lewis et al., 2008). Moreover, Hu and Wilson (2010) have reported a fuel-sheath gap width of 36 \( \mu m \) for a
proposed PV SCWR. In the present study, the fuel centerline temperature has been calculated for both 20-µm and 36-µm gaps. In Eq. (3.7), \( g \) is the temperature jump distance, which is calculated using Eq. (3.8) (Lee et al., 1995).

\[
\frac{1}{g} = \sum_i \left[ \frac{y_i}{g_{0,i}} \right] \left( \frac{T_g}{273.15} \right)^{s+1/2} \left( \frac{0.101}{P_g} \right)
\] (3.8)

Where, \( g \) is the temperature jump distance, \( y_i \) is the mole fraction of the \( i_{th} \) component of gas, \( g_{0,i} \) is the temperature jump distance of the \( i_{th} \) component of gas at standard temperature and pressure, \( T_g \) is the gas temperature in the fuel-sheath gap, \( P_g \) is the gas pressure in the fuel-sheath gap, and \( s \) is an exponent dependent on gas type.

In reality, the fuel pellets become in contact with sheath creating contact points. These contact points are formed due to thermal expansion and volumetric swelling of fuel pellets. As a result, heat is transferred through these contact points. The conductive heat transfer rate at the contact points are calculated using Eq. (3.9) (Ainscough, 1982). In Eq. (3.9), \( A \) and \( n \) are equal to 10 and 0.5.

\[
h_c = A \frac{2k_f k_{sheath}}{(k_f + k_{sheath})(R_f^2 + R_{sheath}^2)/2}^{1/2} \left( \frac{P_a}{H} \right)^n
\] (3.9)

where, \( A \) is a constant, \( P_a \) is the apparent interfacial pressure, \( H \) is the Mayer hardness of the softer material.

The last term in Eq. (3.6) is the radiative heat transfer coefficient through the gap, which is calculated using Eq. (3.10) (Ainscough, 1982). It should be noted that the contribution of this heat transfer mode is negligible under normal operating conditions. However, the radiative heat transfer is significant in accident scenarios. Nevertheless, the radiative heat transfer through the fuel-sheath gap has been taken into account in this paper.

\[
h_r = \frac{\sigma \varepsilon_f \varepsilon_{sheath}}{\varepsilon_f + \varepsilon_{sheath} - \varepsilon_f \varepsilon_{sheath}} \frac{(T_{f,a}^4 - T_{sheath,i}^4)}{(T_{f,a} - T_{sheath,i})}
\] (3.10)
3.1.4 Fuel Centerline Temperature
Equation (3.11) has been used to calculate the fuel centerline temperature. The thermal conductivity in Eq. (3.11) is the average thermal conductivity, which varies as a function of temperature. In order to increase the accuracy of the analysis, the radius of the fuel pellet has been divided into 20 segments. Initially, the inner-surface temperature is not known, therefore, an iteration loop has been created to calculate the outer-surface temperature of the fuel and the thermal conductivity of the fuel based on corresponding average temperatures.

\[
T_{r,i+1} = \frac{q_{gen}(r^2_{i} - r^2_{i+1})}{4k_{avg}} + T_{r,i}
\]  

(3.11)

3.2 Heat-Loss Calculations
A steady-state one-dimensional heat transfer analysis has been conducted in order to determine the heat loss from the High Efficiency Channel (HEC). A code has been developed in MATLAB (see Appendix E). The code divides the length of the fuel channel into segments of one-millimeter lengths and calculates the heat loss based on Eq. (3.12). As indicated by Eq. (3.12), calculation of the heat loss from the coolant to the moderator requires the computation of the thermal resistance network of the fuel channel and the temperature difference between the coolant and the moderator.

As shown in Fig. 3.8, the thermal resistance network of HEC consists of five components, which are the thermal resistances of the coolant, liner tube, ceramic insulator, PT, and the moderator. These thermal resistances have been calculated in Sections 3.2.1 through 3.2.4. Additionally, Figure 3.9 illustrates the methodology based on which these thermal resistances have been calculated. It should be noted that the thermal resistance of the liner tube is negligible, so it has not been taken into account. Additionally, the NIST REFPROP software has been used to calculate the thermophysical properties of the light-water coolant and the heavy-water moderator as they were required (Pioro and Duffey, 2007; Gabaraev et al., 2004).

\[
Q = \frac{T_{\text{coolant}} - T_{\text{moderator}}}{R_{\text{total}}}
\]  

(3.12)
3.2.1 Thermal Resistance of Coolant
The thermal resistance of the light-water coolant can be calculated using Eq. (3.13), which requires the calculation of HEC. HTC has been calculated using Eq. (3.14). In Eq. (3.14), \( \text{Nu} \) is the Nusselt number, which has been calculated based on the Mokry et al. correlation shown as Eq. (3.2).

\[
R_{\text{conv}} = \frac{1}{hA} \tag{3.13}
\]

\[
h = \frac{\text{Nu}_D.k}{D} \tag{3.14}
\]
3.2.2 Thermal Resistance of Ceramic Insulator

The most important property of an insulator is its effective thermal conductivity, which changes as a function of temperature, percent porosity, and pore size. A higher porosity results in a lower thermal conductivity, which in turn results in less heat losses. As a result, the ceramic insulator of the proposed fuel channel design is 70% porous and made of Yttria Stabilized Zirconia (YSZ) (Chow and Khartabil, 2008). YSZ has a low neutron absorption cross-section, low thermal-conductivity and high corrosion resistance in exposure to water at supercritical conditions (Chow and Khartabil, 2008). These properties make YSZ a good candidate as an insulator.

The thermal resistance of the insulator, which is a function of its effective thermal conductivity, is the primary factor in governing the heat loss from the fuel channel. Therefore, it is necessary to calculate the effective thermal conductivity of the porous YSZ insulator as a function of temperature and percentage of porosity based on appropriate theories and available equations. Then, this effective thermal conductivity can be used to calculate the thermal resistance of the ceramic insulator based on Eq. (3.15).

\[
R_{\text{cond}} = \frac{\ln\left(\frac{r_0}{r_i}\right)}{2\pi L k_{\text{eff}}} \quad (3.15)
\]

Schlichting et al. (2001) have developed a theory, which describes the thermal conductivity of dense YSZ as a function of temperature. According to their theory, the intrinsic thermal conductivity of YSZ decreases as a result of scattering of phonons due to point defects. On the other hand, the effective thermal conductivity increases at high temperature due to irradiation effects. The theoretical and experimental results of Schlichting et al. (2001) study have shown in Fig. 3.10, which indicates that for dense YSZ and within operating temperature range of SCWRs, the thermal conductivity does not have a strong dependency on temperature.
In case of the SCWR fuel channel with a porous insulator, the pores are filled with the light-water coolant. The thermal conductivity of light-water varies significantly as a function of temperature, especially at the temperatures close to the pseudocritical point. Therefore, the effective thermal conductivity of the insulator must be determined based on theories that are applicable to porous media.

There are various theories and equations, which can be used in order to calculate the effective thermal conductivity of a porous medium. In this study, the effective thermal conductivity of the porous YSZ insulator has been calculated based on several theories and their related equations including the Maxwell theory, Maxwell-Eucken, Jiand and Sousa, Landauer, Meredith and Tobias equations.

Maxwell (1954) provides an equation, which determines the effective electric conductivity of a medium that consists of small spheres of another medium. Since the conduction of electricity through a medium is analogous to the conduction of heat through a medium, the Maxwell equation, shown as Eq. (3.16), can be used in order to
calculate the effective thermal conductivity of a medium inside which small spheres of another medium are distributed (Maxwell, 1954). Similarly, the Maxwell equation can be used in order to determine the thermal conductivity of a porous medium, where the small spheres of the second medium represent the pores. In Eq. (3.16), \( k_1, k_2, \) and \( P \) are the thermal conductivity of the primary medium, the thermal conductivity of the secondary medium or pores, and the volume fraction of pores to the total volume of the medium, respectively.

\[
\frac{k_{\text{eff}}}{k_1} = \frac{2k_1 + k_2 + 2P(k_2 - k_1)}{2k_1 + k_2 + P(k_2 - k_1)}
\]  

Hu et al. (2010) provide information on the Maxwell-Eucken model and the Effective Medium Theory (EMT) as part of the study that they conducted on the thermal conductivity of porous YSZ. Hu et al. (2010) used the Maxwell-Eucken equation, shown as Eq. (3.17), which has been developed based on EMT, Eq. (3.18), in order to theoretically calculate the thermal conductivity of porous YSZ, where pores were filled with air. Similarly, these equations have been used to determine the effective thermal conductivity of the YSZ insulator based on operating conduction of the studied SCWR.

\[
\frac{k_{\text{eff}}}{k_2} = \frac{2k_2 + k_1 - 2(k_2 - k_1)(1 - P)}{2k_2 + k_1 + (k_2 - k_1)(1 - P)}
\]  

\[
(1 - P) \frac{k_1 - k_{\text{eff}}}{k_1 + 2k_{\text{eff}}} + P \frac{k_2 - k_{\text{eff}}}{k_2 + 2k_{\text{eff}}} = 0
\]

Jiang and Sousa (2006) have developed a 2-D modeling system, which allows for the prediction of the effective thermal conductivity of heterogeneous materials containing of two or three different components. They developed an equation, shown as Eq. (3.19), based on the EMT and used it in their model in order to simulate the effective thermal conductivity of a heterogeneous porous medium. Moreover, Khan et al. (2010) provides an equation, Eq. (3.20), developed by Meredith and Tobias. In addition to the previous equations, Chow and Khartabil (2008) have provided Eq. (21) for the calculation of the thermal conductivity of porous YSZ. The latter equation is based on the volumetric fraction of the primary medium and the porous medium.
Figure 3.11 shows the effective thermal conductivity of 70% YSZ calculated based on aforementioned equations. As shown in Fig. 3.11, the thermal conductivity of porous YSZ calculated based on volumetric fraction of the solid and porous media has the highest value compared to those calculated based on other equations. As a result, the volumetric fraction method provides effective thermal conductivity, which can be considered as an upper bound for the thermal conductivities of porous media. Similarly, calculated heat-loss values would be the highest when the thermal conductivity of a porous insulator has been calculated based on the volumetric fraction method. On the other hand, Maxwell-Eucken Equation and the correlation provided by Jiang and Sousa (2007) result in the lowest effective thermal conductivity values. Therefore, the effective thermal conductivities calculated from latter equations can be considered as a lower bound on the effective thermal conductivity of porous media. Other equations provide effective thermal conductivities between the upper and the lower bounds.
Figure 3.11: Effective thermal conductivity of 70% porous YSZ as function of temperature.

3.2.3 Thermal Resistance of Pressure Tube
Two potential materials for PT are Excel (Zr-3.5\%\textsubscript{wt}Sn-0.8\%\textsubscript{wt}Mo-0.8\%\textsubscript{wt}Nb-1130 ppm O) and Zirconium 2.5 \%\textsubscript{wt} Nb [12]. The former has a high creep resistance, consequently lower creep growth rates than Zr 2.5\%\textsubscript{wt} Nb while exposed to radiation. However, more research and development are required to select the final material, but for the purpose of calculation of the heat loss from the coolant to the moderator Zirconium 2.5\%\textsubscript{wt} Nb has been selected as the material of choice. Conduction thermal resistance of the pressure has been calculated using Eq. (3.15) (Incropera et al., 2006). In Eq. (3.15), $k$ is the thermal conductivity of the pressure tube, which has been calculated using Eq. (3.22) (International Nuclear Safety Center., 2008). According to the operating temperature of the pressure, it has been estimated that the uncertainty involved in the calculation of the thermal conductivity of the PT is approximately ±4%.

$$k = 12.767 - 5.4348 \cdot 10^{-4}T + 8.9818 \cdot 10^{-6}T^2$$  (3.22)
3.2.4 Thermal Resistance of Moderator
The flow of the heavy-water moderator in the calandria vessel is very complex due to momentum forces generated by the inlet jets and buoyancy forces (Kim et al., 2006). However, the flow can be modeled as a natural circulation. Consequently, for the purpose of the calculation of the heat transfer coefficient between the outer surface of the fuel channel (e.g., the pressure tube) and the moderator, it has been assumed with the natural circulation. Further, Eqs. (3.13) and (3.14) have been used to calculate the thermal resistance. Moreover, the required HTC has been calculated based on a correlation shown as Eq. (3.23), which is recommended by Churchill and Chu (Incropera et al., 2006). In Eq. (3.23), \( Ra_D \) is Rayleigh number which was calculated using Eq. (3.24).

\[
\text{Nu} = \left\{ 0.60 + \frac{0.386 \text{Ra}_D^{1/6}}{[1+(0.559/\text{Pr})^{9/16}]^{8/27}} \right\}^2
\]

(3.23)

\[
\text{Ra}_D = \frac{g\beta(T_l-T_o)D^3}{\nu\alpha}
\]

(3.24)
4 RESULTS

4.1 Fuel Centerline and Sheath Temperatures
A steady-state one-dimensional heat transfer analysis was conducted in order to calculate the fuel centerline temperature at SCW fuel channels. Based on the proposed core configuration (see Figure 2.9) SCW fuel channels are located at the center of the core. Consequently, the thermal power in some of these fuel channels might be by a factor above the average channel power of 8.5 MW\textsubscript{th}. Therefore, in the present study, a thermal power per channel of 9.8 MW\textsubscript{th} has been considered for the SCW fuel channels with the maximum thermal power. This thermal power is approximately 15\% (i.e. 10\% above the average power and 5\% uncertainty) above the average thermal power per channel. The conditions based on which the calculations have been conducted are as follows: an average mass flow rate of 4.4 kg/s, a constant pressure of 25 MPa, a coolant inlet temperature of 350°C, a thermal power per channel of 9.8 MW\textsubscript{th}.

The presented analysis does not take into account the pressure drop of the coolant. The main reason for not taking the pressure drop into consideration is that the pressure drop is inversely proportional to the square of mass flux. In a CANDU-6 fuel channel, the pressure drop is approximately 1.75 MPa (AECL, 2005). Additionally, the mass flux in an SCWR fuel channel is approximately 5 times lower than that of a CANDU-6 reactor. Therefore, the pressure drop of a SCWR fuel channel should be significantly lower than 1.75 MPa. As a result, the pressure drop has not been taken into consideration.

Additionally, this study does not determine the sheath and the fuel centerline temperatures for the SRH fuel channels mainly due to the fact that the average thermal power in SRH channels is 5.5 MW\textsubscript{th} (see Table 2.4). Since the thermal power in SRH channels is approximately 35\% less that of the SCW channels, the sheath and the fuel centerline temperatures will be definitely lower than those of the SCW channels. As a result, if a fuel and sheath meet their corresponding temperature limits under the operating conditions of the SCW channels with the maximum thermal power, they will be suitable for the SRH channels as well.
For the SCW fuel channels, the fuel centreline temperature has been calculated at cosine, upstream-skewed cosine, downstream-skewed cosine, and uniform axial heat flux profiles. These heat flux profiles have been calculated based on a 43-element fuel bundle known as the Variant-20 fuel bundle. Each of the 42 fuel elements of the Variant-20 fuel bundle has an outer diameter of 11.5 mm while the minimum required thickness of the sheath has been determined to be 0.48 mm. Therefore, the inner diameter of the sheath is 10.54 mm. Inconel-600 was chosen as the material of the sheath. Furthermore, it was assumed that widths of the fuel-sheath gap were zero, 20μm, and 36 μm. Moreover, several nuclear fuels were examined for the purpose of this study.

The examined fuels were UO₂, MOX, ThO₂, UC, UC₂, UN, UO₂-SiC, UO₂-C, and UO₂-BeO. For each fuel, the fuel centerline temperature was analysed at the aforementioned AHFPs. Since the maximum fuel centerline temperature was reached at downstream-skewed cosine AHFP for all the examined fuels, only the results associated with this AHFP have been presented in this section. However, the results of all other examined AHFPs are available in Appendix F. Figures 4.1 through 4.9 show the coolant, sheath, and fuel centerline temperature profiles as well as the heat transfer coefficient profile along the heated length of the fuel channel for UO₂, MOX, ThO₂, UC, UC₂, UN, UO₂-SiC, UO₂-C, and UO₂-BeO fuels. Additionally, Figure 4.10 shows the maximum fuel centerline temperatures of all the examined fuels (see Table F.1 in Appendix F for details). It should be noted that the results presented in Figs. 4.1 through 4.10 are based on a 20-μm fuel-sheath gap.

As shown in Figure 4.10, the maximum fuel centerline temperatures of all examined low thermal-conductivity fuels exceed the temperature limit of 1850°C. On the other hand, enhanced thermal-conductivity fuels and high thermal-conductivity fuels show fuel centerline temperatures below the established temperature limits of 1850°C and 1500°C, respectively.
Figure 4.1: Temperature and HTC profiles for UO₂ fuel at maximum channel power with downstream-skewed cosine AHFP.

Figure 4.2: Temperature and HTC profiles for MOX fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure 4.3: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP.

Figure 4.4: Temperature and HTC profiles for UC fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure 4.5: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP.

Figure 4.6: Temperature and HTC profiles for UN fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure 4.7: Temperature and HTC profiles for UO\textsubscript{2}–SiC fuel at maximum channel power with downstream-skewed cosine AHFP.

Figure 4.8: Temperature and HTC profiles for UO\textsubscript{2}–C fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure 4.9: Temperature and HTC profiles for UO₂–BeO fuel at maximum channel power with downstream-skewed cosine AHFP.

Figure 4.10: Maximum sheath and fuel centerline temperatures of UO₂, MOX, ThO₂, UC, UC₂, UN, UO₂–SiC, UO₂–C, and UO₂–BeO based on a 20–μm fuel-sheath gap width.
In regards to sheath temperature, the sheath temperature reached its maximum at downstream-skewed cosine AHFP. Figure 4.11 provides a comparison between the sheath temperature profiles for the four studied AHFPs. Figure 4.11 also shows the HTC profiles corresponding to each examined AHFPs. As shown in Figure 4.11, unlike uniform AHFP, HTC reaches its maximum value in the beginning of the fuel channel for non-uniform AHFPs (i.e., downstream-skewed cosine, cosine, and upstream-skewed cosine AHFPs). This increase in HTC is due to the fact the sheath temperature reaches the pseudocritical temperature. In contrast, with uniform AHFP, the sheath temperature is above the pseudocritical temperature from the inlet of the fuel channel. Consequently, the peak in HTC at uniform AHFP occurs when the coolant reaches the pseudocritical temperature.

![Figure 4.11: HTC and sheath-wall temperature profiles as function of AHFP.](image)

A comparison between the examined non-uniform AHFPs shows that in terms of the sheath and fuel centerline temperatures, upstream-skewed cosine AHFP is the most ideal heat flux profile. On the other hand, the downstream-skewed cosine AHFP results in the
highest temperatures. Thus, for design purposes, it is a conservative approach to
determine the sheath and fuel centerline temperatures based on a downstream-skewed
AHFP.

As mentioned previously, the effect of the fuel-sheath gap on the fuel centerline
temperature has been studied. The results on this analysis are shown in Fig. 4.12 and are
summarized in Table 4.1. Since the downstream-skewed cosine AHFP results in the
highest fuel centerline temperature, the analyses corresponding to zero and 36-µm fuel-
sheath gap widths have been conducted at the downstream-skewed AHFP. The fuel
centerline temperature limit of each fuel (see Section 5 for details) is also included in Fig.
4.12 and Table 4.1, where temperatures are in degrees Celsius.

![Figure 4.12: Maximum fuel centerline temperatures of UO\textsubscript{2}, MOX, ThO\textsubscript{2}, UC, UC\textsubscript{2},
UN, UO\textsubscript{2}–SiC, UO\textsubscript{2}–C, and UO\textsubscript{2}–BeO as functions of fuel-sheath gap width.](image)

99
Table 4.1: Maximum fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO with fuel-sheath gap widths of 0, 20 µm, and 36 µm.

<table>
<thead>
<tr>
<th>Fuel</th>
<th>Temperature Limit, °C</th>
<th>Fuel-Sheath Gap Width, µm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>0</td>
</tr>
<tr>
<td>UO$_2$</td>
<td>1850</td>
<td>2677</td>
</tr>
<tr>
<td>MOX</td>
<td>1850</td>
<td>2731</td>
</tr>
<tr>
<td>ThO$_2$</td>
<td>1850</td>
<td>2357</td>
</tr>
<tr>
<td>UC</td>
<td>1500</td>
<td>1166</td>
</tr>
<tr>
<td>UC$_2$</td>
<td>1500</td>
<td>1238</td>
</tr>
<tr>
<td>UN</td>
<td>1500</td>
<td>1121</td>
</tr>
<tr>
<td>UO$_2$(85%) + SiC (12%)</td>
<td>1850</td>
<td>1453</td>
</tr>
<tr>
<td>UO$_2$–C</td>
<td>1850</td>
<td>1198</td>
</tr>
<tr>
<td>UO$_2$–BeO</td>
<td>1850</td>
<td>1242</td>
</tr>
</tbody>
</table>

The following section provides the results of the heat-loss analysis from the High Efficiency Channel.

4.2 Heat Loss from High Efficiency Channel
The result of this analysis shows that there is a significant variation among the effective thermal conductivity values of the porous YSZ based on various examined equations. As shown in Figure 4.37, the Volumetric Fraction method resulted in the highest heat loss and the effective thermal conductivity. On the other hand, Maxwell-Eucken and Jiang and Sousa Equations were in good agreement with each other while resulting in the lowest heat losses. Other equations, including the Maxwell equation, predict heat losses between the upper and lower bounds. Table 4.2 lists the total heat loss per fuel channel calculated based on these equations. It should be noted that heat loss values correspond to a moderator temperature and pressure of 80°C and 0.1 MPa, respectively, and a 70% porous ceramic insulator with a 7-mm thickness, unless it is indicated.
4.13: Heat loss profile along heated length of High Efficiency Channel with 7-mm of 3 mole % YSZ with 70% porosity.

Table 4.2: Total heat loss per fuel channel based on examined equations.

<table>
<thead>
<tr>
<th>Equation</th>
<th>Porosity, %</th>
<th>3 mole % YSZ</th>
<th>8 mole % YSZ</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maxwell-Eucken</td>
<td></td>
<td>70.3</td>
<td>64.0</td>
</tr>
<tr>
<td>Jiang and Sousa (2007)</td>
<td></td>
<td>71.9</td>
<td>64.6</td>
</tr>
<tr>
<td>Effective Medium Theory</td>
<td>70</td>
<td>79.2</td>
<td>68.8</td>
</tr>
<tr>
<td>Meredith and Tobias</td>
<td></td>
<td>85.2</td>
<td>71.6</td>
</tr>
<tr>
<td>Maxwell (1954)</td>
<td></td>
<td>96.0</td>
<td>77.3</td>
</tr>
<tr>
<td>Volumetric Fraction</td>
<td>0.0</td>
<td>108.1</td>
<td>84.2</td>
</tr>
<tr>
<td>Solid YSZ</td>
<td>0.0</td>
<td>218.0</td>
<td>155.5</td>
</tr>
</tbody>
</table>
As indicated in Table 4.2, the heat loss per fuel channel could be as high as 108 kW when a 70% porous insulator has been examined. In order to provide a basis for comparison, the heat loss from a CANDU-6 type fuel channel has been calculated using the same methodology. A CANDU-6 fuel channel consists of a pressure tube, a calandria tube, which are separated by an annular gap that is filled with gaseous carbon dioxide. Since gases have lower thermal conductivities compared to liquids and solids, the annulus gas provides an effective thermal insulation between the “hot” pressure tube and the calandria tube. The annulus gas also protects the protective oxide layer on the outer surface of the pressure tube and provides a method for detection of pressure tube leaks (Groom, 2003). Kim et al. (2005) have reported a heat loss value of 11.3 kW per fuel channel for a CANDU-6 fuel channel under the operating condition of a CANUD-6 reactor. This heat loss is in agreement with the heat loss (e.g., 12.7 kW per channel) that has been calculated based on the methodology presented in Section 3.2.

Since the aforementioned methodology proved to be reliable, in the next step, the CANDU-6 fuel channel has been modified based on the operating conditions of SCWRs. First, a new material was selected for the pressure tube. A suitable material must withstand temperatures as high as 625, have good corrosion resistance properties, and have very high creep strength. Further, For the purpose of calculating the heat loss from the coolant to moderator we chose Inconel-718 as the material of choice for the pressure tube. Second, the required thickness of the pressure tube was recalculated based on an operating temperature and pressure of 625°C and 25 MPa, respectively. Figure 4.38 shows a CANDU-6 type fuel channel, which has been modified based on the operating conditions of the examined SCWR. The results show that the heat loss from this fuel channel is approximately 22.7 kW per fuel channel. Figure 4.39 shows the heat loss profile along the length of the fuel channel.
Figure 4.14: 3-D view of a CANDU-6 type fuel channel.

Figure 4.15: Heat loss profile of modified CANDU-6 fuel channel at SCWR conditions.
In order to calculate the optimum thickness of the insulator, an insulator of 4 mm thickness was selected as a reference. Then reduction of the heat loss was calculated per one millimeter addition into the thickness of the insulator. As shown in Fig. 4.40, the optimum thickness of the insulator varies depending on the equation that is used to calculate the effective thermal conductivity of the insulator. However, considering all examined equations, the optimum thickness of the insulator is between 5 to 7 mm for a 70% porous, 3 mole percent, YSZ insulator.

![Graph showing heat loss saving vs. insulator thickness](image)

**Figure 4.16: Optimum thickness of ceramic insulator.**

The heat loss per fuel channel for a solid insulator (e.g., without porosity) with a 7-mm thickness is 133 kW, which is approximately 30% higher than that of the 70% porous ceramic insulator. This shows the effectiveness of the porosity in reducing the heat loss to the moderator. In other words, the higher the porosity, the lower the heat loss, but it should be noted that further research is required to investigate the potential effects of hydrogen or deuterium build-up in the porous areas of the insulator.
5 DISCUSSION

5.1 Fuel Centerline and Sheath Temperatures

There are two temperature limits that a fuel and a fuel bundle must meet. First, the sheath temperature must not exceed the design limit of 850°C (Chow and Khartabil, 2008). Second, when UO$_2$ fuel is used, the fuel centerline temperature must be below the industry accepted limit of 1850°C (Reisch, 2009) at all normal operating conditions.

The outer surface temperature of the sheath reached a maximum of approximately 900°C at the downstream-skewed cosine AHFP. However, the allowable design limit is 850°C. Therefore, the geometry of the fuel bundle or the fuel-channel operating conditions should be modified in order to comply with the design limit of 850°C. When the modification of the fuel-bundle geometry is considered, fuel bundles with smaller fuel-element diameters must be designed. This reduction in the diameter will reduce the fuel centerline temperature and the sheath temperature.

In regards to the fuel-channel operating conditions, in fuel channels with the maximum thermal power, the outlet temperature of the coolant reaches a maximum of 725°C, which is approximately 100°C above the specified outlet temperature of the coolant. The higher coolant outlet temperature is due to a higher heat flux; while the mass flux of the coolant has been assumed to be equal to the average mass flux. Therefore, it is possible to increase the coolant mass flux in the fuel channels with the maximum thermal power. This increase in mass flux should result in a coolant outlet temperature of 625°C, lower sheath temperature, and higher safety by ensuring operation below the onset of DHT.

Previously, it was mentioned that the industry accepted temperature limit for UO$_2$ fuel is 1850°C; however, this temperature limit might be different for fuels other than UO$_2$. There are several factors that may affect a fuel centerline temperature limit for a fuel. These factors include melting point, high-temperature stability, and phase change of the fuel. For instance, the accepted fuel centerline temperature limit of UO$_2$ fuel is approximately 1000°C below its melting point. As a result, the same fuel centerline temperature limit has been established for the other low thermal-conductivity fuels and enhanced thermal-conductivity fuels. In regards to ThO$_2$, the melting point is higher than that of UO$_2$, but a high uncertainty is associated with its melting point. Therefore, as a
conservative approach, the same temperature limit has been established for ThO$_2$. Similarly, the corresponding limit for UC and UC$_2$ fuel would be 1500°C since the melting points of UC and UC$_2$ are approximately 2505°C and 2560°C, respectively. In regards to UN, this fuel decomposes to uranium and gaseous nitrogen at temperatures above 1600°C. Therefore, the fuel centerline temperature limit for UN should be lower than that of UO$_2$ under normal operating conditions. Ma (1983) recommends a temperature limit of 1500°C for UN.

The maximum fuel centerline temperature values of all the low thermal-conductivity fuels were found to exceed the fuel centerline temperature limit of 1850°C. For instance, MOX fuel has the highest fuel centerline temperature at 2767°C, followed by UO$_2$ and ThO$_2$ at 2719°C and 2433°C, respectively. These fuel centerline temperatures indicate that UO$_2$ fuel, when enclosed in the Variant-20 fuel bundle, is not a suitable fuel for use in the SCW fuel channels with the maximum thermal power.

On the other hand, high thermal-conductivity and enhanced thermal-conductivity fuels show improvements in the fuel centerline temperatures. The maximum fuel centerline temperatures are 1138°C, 1186°C, and 1260°C when the UN, UC, and UC$_2$ are considered, respectively. The maximum fuel centerline temperatures of these fuels are below their corresponding temperature limits. Additionally, the maximum fuel centerline temperatures of UO$_2$-SiC, UO$_2$-C, and UO$_2$-BeO are 1495°C, 1220°C, and 1256°C, respectively. Therefore, these fuels are suitable for use in the SCW fuel channels with the maximum thermal power when the fuel centerline temperature is considered as the only decisive factor in choosing a fuel. However, other factors other than the fuel centerline temperature must be taken into account in order to determine the best fuel option(s) for future use in SCWRs.

In regards to these factors, it should be mentioned that enhanced thermal-conductivity fuels are currently under development. To the knowledge of the author, available properties of these fuels are limited. As a result, the factors, presented in the following paragraphs, have been considered only for high thermal-conductivity fuels. Among high thermal-conductivity fuels, UC$_2$ undergoes a phase change at temperatures within the range of 1765°C (Bowman et al., 1966) and 1820°C (Frost, 1963). This phase change
results in an increase in the volume of the fuel, which in turn may jeopardize the mechanical integrity of the fuel and the sheath. This phase change significantly reduces the possibility of using UC$_2$ in SCWRs or other high temperature-applications. As a result, a comparison of the following properties has been drawn mainly among UN, UC and UO$_2$. The latter fuel has been taken into account because UO$_2$ is widely used in nuclear reactors.

The most important factors associated with nuclear fuels for SCWRs and other high-temperature applications include melting point, evaporation, high temperature chemical stability, release of fission products, radiation-induced swelling, thermal-shock resistance, density, high temperature creep, and mass of fissile elements (Lundberg and Hobbins, 1992). When high thermal-conductivity fuels are considered, all examined fuels have high melting points and high thermal conductivities, which lead to lower fuel centerline temperatures than those of low thermal-conductivity fuels (e.g., UO$_2$, MOX, or ThO$_2$) for a given thermal power. Thus, the other aforementioned factors should be considered in order to determine the best fuel option(s).

The atom density of uranium is another important factor, especially in fast-neutron spectrum reactors, because fission probability is significantly lower for fast neutrons compared to those of thermal neutrons. Both UN and UC have high uranium atom density, approximately 1.40 and 1.34 times that of UO$_2$. As a result, the use of UC or UN with results in smaller core sizes compared to that of UO$_2$ fuel.

Stellrecht et al. (1968), Routbort (1972), Routbort and Singh (1975), and Hayes et al. (1990b) studied the steady-state creep strength and irradiation-induced creep of UN and UC fuels, and provided several correlations for the calculation of the steady-state and irradiation-induced creep rates. These correlations can be used in order to predict the mechanical behavior of these fuels (e.g., dimensional stability and integrity) under operating conditions. Further studies have calculated the creep rates of fully dense UN and UC for a stress of 25 MPa (see Figs. 2.34 and 2.22). In terms of irradiation-induced creep, both UN and UC have significantly lower irradiation–induced creep rates compared to UO$_2$ (Routbort and Singh, 1975). The results demonstrate that when UC and UN fuels are compared, the irradiation-induced creep rate of UC was lower than of UN at
1500°C. In other words, UC has a better creep strength and resistance to deformation than UN. With UC fuel, it is recommended to use hyper-stoichiometric UC (Routbort, 1972) because it has a lower steady-state creep rate compare to hypo-stoichiometric UC. Additionally, hyper-stoichiometric UC has a higher mechanical strength than hypo-stoichiometric UC due to higher values of long-range stress (Routbort, 1972), which result in higher proportional limit values. As a result, hyper-stoichiometric UC has better mechanical behavior at high temperatures than hypo-stoichiometric UC and UN.

In addition to creep resistance, hardness is another mechanical property, which is an indication of the resistance of a material to deformation. Routbort and Singh (1975) identify the grain size, porosity, impurity contents, C/U or N/U ratios, and temperature as the most important factors affecting the hardness. They also provide the hardness values at room temperature and 1000°C for UC and UN. For both UN and UC, the hardness decreases as the temperature increases. According to Routbort and Singh (1975), the hardness values, in kg/mm$^2$, are 100, 120, and 50 for UC$_{1.05}$, UC$_{0.98}$, and UN respectively. The result of their investigation shows that UC has a higher hardness compared to UN; therefore, UC has a higher resistance against deformation which in turn increases the mechanical integrity of the fuel under operating conditions of SCWRs and other high-temperature nuclear applications.

The fission reaction, in a nuclear fuel, results in the production of gaseous fission products. These fission products are either contained in the fuel or released, which in turn exert stress on the sheath. Additionally, the containment of the fission products in the fuel results in the swelling (e.g., a reduction in density due to a volume increase) of the fuel. Thus, it is essential to study the swelling rate of nuclear fuels to ensure that the fuel and the cladding will withstand the stresses exerted on them and maintain their mechanical integrity under the operating conditions of a nuclear reactor, especially when high burn-ups are required.

A comparison between the volumetric swelling of UN and UC fuels shows that the percent volumetric swelling of UN is higher than that of UC (see Figs. 2.33 and 2.21). For instance, the percent volumetric swelling of UN is approximately 17% and that of UC 12%, approximately at 1400°C and a burn-up of 40 GW day/Mg(U). It should be noted
that the temperature of 1400°C has been chosen because of the available experimental data related to the swelling of UC. However, the maximum fuel centerline temperatures of UC and UN are below 1300°C. Consequently, the relative volumetric swelling of these two fuels is expected to be lower than the presented values. Additionally, it should be noted that the swelling of both fuels can be reduced by increasing the porosity of the fuel (Frost, 1963). In contrast, Ma (1983) demonstrates that the fission gas release is higher for porous fuels compared with dense fuels, which have less porosity. Nevertheless, UC has a lower percent volumetric swelling compared to UN.

The thermal-shock resistance of a nuclear fuel is an indication of the degree to which the fuel withstands sudden changes in temperature. A low thermal-shock resistance may result in the formation of cracks in the fuel which in turn reduces the mechanical integrity of the fuel and increases the fission product release rate. As indicated by Eq. (5.1) (Kutz, 2005), the thermal shock-resistance of a fuel depends on its thermal conductivity, compressive strength, Poisson’s ratio, coefficient of thermal expansion, and Young’ modulus of elasticity. The thermal-shock resistances of UC, UN, and UO$_2$ have been calculated based on Eq. (5.1) for a temperature range between 800°C and 1800°C. All required properties were determined for 95% TD fuels except the linear thermal expansion coefficient, which was based on 100% TD fuels. The result shows that the thermal-shock resistances of both UN and UC are 5 to 15 times higher than those of UO$_2$ within the examined temperature range. The low thermal-shock resistance of UO$_2$ is mostly due to its low thermal conductivity, which makes this fuel vulnerable to sudden changes in temperature at high operating temperatures of SCWRs. Thus, UN and UC have significantly higher thermal-shock resistances compared with UO$_2$ and are more suitable for high-temperature applications such as SCWRs.

\[
R' = \frac{k \cdot \sigma (1 - \nu)}{\alpha \cdot E}
\] (5.1)
The chemical compatibility of a nuclear fuel with coolant, which is an essential factor that affects the integrity of the fuel, can be studied in terms of the oxidation behaviour of the fuel when exposed to the coolant. For instance, UO$_2$ fuel is stable in water and has a high oxidation resistance in light-water and heavy-water at the LWR and Heavy Water Reactor (HWR) conditions (e.g., up to 320°C). However, UO$_2$ oxidizes at temperatures above 320°C if it comes in direct contact with air or water in the case of a sheath breach (Ma, 1983). Similarly, UC has a poor oxidization resistance when it comes in contact with water even at temperatures as low as 55°C (Ma, 1983). Likewise, UN oxidizes in water at temperatures above 100°C due to the deformation of the protective layer, which is formed on the surface of UN. The protective layer on the surface of UN is eventually lost at high temperatures and cracks are formed. Additionally, the oxidization resistance of UN is highly dependent on deviation from stoichiometry (Ma, 1983). In other words, the presence of free uranium or U$_2$N$_3$ significantly increases the oxidization rate. On the other hand, Kirillov et al. (2007) imply that UC and UN have better compatibility with coolant and cladding compared to UO$_2$. Therefore, further study is required on the chemical compatibility of UC and UN with water due to the discrepancy between the two available sources.

In terms of high-temperature stability, a great number of studies have been conducted on hypo-stoichiometric and hyper-stoichiometric UN. The results of these studies indicate that hyper-stoichiometric UN co-exists with uranium sequinitride (U$_2$N$_3$) in the temperature range of 1075°C and 1375°C for hyper-stoichiometric UN with N/U atomic ratios approximately between 1.2 and 1.5 (Matthews et al., 1988). According to the phase diagram provided by Matthews et al. (1988), U$_2$N$_3$ decomposes to UN and nitrogen at temperatures approximately above 1375°C. The release of nitrogen gas results in severe cracking of the fuel. This problem can be solved by using hypo-stoichiometric UN. However, it should be noted that Matthews et al. (1988) demonstrate that the fission gas release rate is higher for hypo-stoichiometric UN than hyper-stoichiometric UN. Moreover, if UN is chosen as a nuclear fuel, hypo-stoichiometric UN with adequate porosity should be utilized in order to minimize the negative impacts of the decomposition of U$_2$N$_3$ and accommodate for the fission products.
Another issue related to UN fuel is that hypo-stoichiometric UN decomposes to uranium and nitrogen gas, which leads to cracking of the fuel due to the release of nitrogen. The results of several studies have shown that the incongruent vaporization of hypo-stoichiometric UN leads to the release of nitrogen and the formation of free uranium (Balankin et al., 1978). Balankin et al. (1978) report of the appearance of free uranium in the temperature range between 1500 and 1800°C. Moreover, Gingerich (1969) indicates that the incongruent vaporization of hypo-stoichiometric UN occurs in the temperature range between 1130 and 1800°C for N/U atomic ratios of 1.0 and 0.92, respectively. Gingerich (1969) also provides the results of experiments, which were conducted by Covert and Bonham, Vozzella and DeCrescente, and Inouye and Leitnaker on the decomposition of UN. Their experimental results, which are in agreement with Gingerich’s results, indicate that incongruent decomposition of UN occurs at temperature ranges of 1600 and 2000°C (based on Covert and Bonham), 1645 and 1992°C (based on Vozzella and DeCrescente), and 1300°C (based on Inouye and Leitnaker). Additionally, Oggianu et al. (2003) indicate that UN dissociates at temperatures higher than 1600°C, which is in agreement with other values published in the literature. Therefore, the release of nitrogen gas and formation of cracks in the fuel should be studied thoroughly if UN is chosen as the fuel of choice for SCWRs, but it should be mentioned that this effect might not be significant since the maximum fuel centerline temperature for UN fuel is below 1300°C under normal operating conditions of SCWRs.

The study of neutronic properties of a nuclear fuel is as essential as analyzing its thermodynamic and mechanical properties. Oggianu et al. (2003) draw a comparison between neutronic properties of UO₂, UC, and UN, which have been summarized in Table 5.1. According to Oggianu et al. (2003), UN has higher fission and absorption cross-sections for the thermal neutrons than UC. These two parameters can be used to calculate the fission-to-capture ratio, which indicates that 43.7% of absorbed neutrons results in fission in UN fuel compared to 54.3% in UC. This shows that a higher neutron economy is achieved when UC fuel is used. It should be noted that the fission-to-capture ratio for UO₂ is higher than that of UC. On the other hand, UO₂ has a smaller uranium atom density compared to those of UN and UC. A high uranium atom density indicates a smaller core size which in turn reduces the costs. Thus, both UN and UC result in smaller
core sizes, which in turn reduce the capital cost of the plant. Moreover, UC fuel enhances the neutron economy due its higher fission-to-capture ratio.

Table 5.1: Neutronic properties of UO$_2$, UC, and UN (Oggianu et al., 2003).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>UO$_2$</th>
<th>UC</th>
<th>UN</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fission cross section for natural uranium (at 0.025 eV), cm$^{-1}$</td>
<td>0.102</td>
<td>0.137</td>
<td>0.143</td>
</tr>
<tr>
<td>Absorption cross-section for natural uranium (at 0.025 eV), cm$^{-1}$</td>
<td>0.185</td>
<td>0.252</td>
<td>0.327</td>
</tr>
<tr>
<td>$\alpha = \Sigma_c/\Sigma_f$ (capture to fission ratio)</td>
<td>0.831</td>
<td>0.839</td>
<td>1.286</td>
</tr>
<tr>
<td>$\eta$ (average number of neutrons emitted per neutron absorbed)</td>
<td>1.34</td>
<td>1.34</td>
<td>1.08</td>
</tr>
<tr>
<td>Uranium atom density based on 100% TD, g/cm$^3$</td>
<td>9.67</td>
<td>12.97</td>
<td>13.52</td>
</tr>
</tbody>
</table>

It is beneficial to demonstrate an economic assessment among UO$_2$, UC, and UN fuels in order to provide a comparison between the fuel cycle costs of these fuels. The result of the study conducted by Oggianu et al. (2003) shows that the cost of fuel is lower for UC compared to UN (Oggianu et al., 2003). This higher fuel cost for UN might be due to the necessity to enrich nitrogen to $^{15}$N to avoid the formation of $^{14}$C. Additionally, Oggianu et al. (2003) have calculated the cost of the fuel cycle plus the cost of forced outages, which indicates that still the overall cost is lower for UC fuel. Thus, UC fuel is economically more attractive than UN fuel.

As has been noted, each fuel exhibits both desirable and detrimental properties, which should be addressed to ensure that the integrity and longevity of the fuel in the reactor is maintained. Moreover, the study of the deleterious behavior of these fuels provides the means to select the most suitable fuel for the future use in SCWRs. Consequently, the perceived issues associated with UO$_2$, UN, and UC fuels have been summarized in Table 5.2.
### Table 5.2: Issues related to UO\(_2\), UN, and UC fuels.

<table>
<thead>
<tr>
<th>Fuel</th>
<th>Problem</th>
</tr>
</thead>
<tbody>
<tr>
<td>(\text{UO}_2)</td>
<td>Low thermal conductivity and High linear thermal expansion coefficient at high temperatures</td>
</tr>
<tr>
<td></td>
<td>low thermal-shock resistance at high operating temperatures (e.g., above 1100°C)</td>
</tr>
<tr>
<td></td>
<td>Higher irradiation-induced creep than UN and UC</td>
</tr>
<tr>
<td></td>
<td>High fission product at (T &gt; 1725°C)</td>
</tr>
<tr>
<td></td>
<td>High evaporation rate</td>
</tr>
<tr>
<td></td>
<td>Lower uranium density</td>
</tr>
<tr>
<td></td>
<td>Lower fuel density</td>
</tr>
</tbody>
</table>

| UN | UN dissociates at \(T > 1130°C\), \(T > 1500°C\), and \(T > 1600°C\) | Gingerich (1969); Balankin et al. (1978); Oggianu et al. (2003) |
| | Hyper-stoichiometric UN co-exists with \(\text{U}_2\text{N}_3\), which decomposes to UN and nitrogen at temperatures approximately above 1375°C | Matthews et al. (1988) |
| | Higher irradiation-induced creep compared with UC | Routbort and Singh (1975) |
| | Oxidation reaction with water | Oggianu et al. (2003) |
| | Relatively higher volumetric swelling compared with \(\text{UO}_2\) | Ross et al. (1990) |
| | The necessity to enrich in \(^{15}\text{N}\) to minimize the \(^{14}\text{C}\) production | Oggianu et al. (2003) |
| | Lower hardness compared with UC | Routbort and Singh (1975) |
| | Relatively high gaseous fission products release from hypo-stoichiometric UN | Matthews et al. (1988) |

| UC | Speculative chemical compatibility with water (e.g., reacts with water) | Ma (1983); Kirillov et al. (2007) |
| | Relatively higher volumetric swelling compared with \(\text{UO}_2\) | Frost (1963) |
| | \(~12\%\) lower melting point compared with \(\text{UO}_2\) and UN | Cox and Cronenberg (1977); Lundberg and Hobbins (1992) |
5.2 Heat Loss from High Efficiency Channel

As indicated in Section 4.2, the total heat loss from HEC is between 70 kW and 108 kW. The difference between the two aforementioned heat loss values is due to the calculation of the effective thermal conductivity of the ceramic insulator based on various equations. Regardless of the difference among these equations, the heat loss from HEC is approximately 7 to 10 times higher than the heat loss from a CANDU-6 type fuel channel under the same operating conditions. As a result, the effects of the ceramic insulator thickness and operating pressure of the moderator on the heat loss were studied.

![Figure 5.1](image)

Figure 5.1: Heat loss profiles of High Efficiency Channel as function of insulator thickness.

The results depict that the heat loss reduces as the thickness of the insulator increases. Figure 5.1 shows the heat loss profile as a function of the insulator thickness. The total heat loss is approximately 108 kW with a 7-mm thick insulator; however, this total heat loss decreases to 88 kW, 75 kW, and 65 kW when the thickness of the insulator increases to 9 mm, 11 mm, and 13 mm, respectively. Therefore, the thickness of the insulator has a
significant impact on the heat loss from the fuel channel. However, it should be mentioned that an increase in the thickness of the insulator changes the distance between the two adjacent fuel channels (i.e., lattice pitch). This change in the value of the lattice pitch has an effect on the reactivity of the core as well as the configuration of the fuel channels. Therefore, the aforementioned factors should be taken into consideration when the thickness of the insulator is increased.

In regards to the effect of the moderator on the heat loss, a higher moderator pressure allows for an increase in the operating temperature of the moderator which in turn results in a smaller temperature difference between the coolant and the moderator. As indicated in Eq. (3.12), a smaller temperature difference between the coolant and the moderator leads into lower heat losses. Additionally, higher pressures in the moderator ensure that boiling will not occur in the moderator and minimize the potential danger associated with excess concentrations of oxygen and deuterium inside the calandria vessel. As a result, the heat loss has been determined for higher moderator pressures of 0.3, 0.5, 0.7, and 0.9 MPa. As shown in Figure 5.2, the heat loss can be decreased approximately 25% by increasing the operating pressure and temperature of the moderator to 0.9 MPa and 155°C. The heat loss profiles in Fig. 5.2 correspond to a 7-mm thick ceramic insulator with 70% porosity. As a conservative approach, the effective thermal conductivity of the ceramic insulator has been calculated using the volumetric fraction method.

In regards to the HEC design, there are two important parameters namely, the temperature gradient across the ceramic insulator and the operating temperature of the pressure tube. High temperature gradients across the insulator may result in the formation of cracks in the insulator in the case of power maneuvering. Moreover, if the operating temperature of the pressure tube exceeds the saturation temperature of the moderator, which is a function of the operating pressure of the moderator, boiling occurs in the moderator that is not favourable per previous explanation. Therefore, it is necessary to determine the temperature gradient across the ceramic insulator and the operating temperature of the pressure tube. These parameters are shown in Fig. 5.3, which are based on the inlet and outlet conditions of the fuel channel.
Figure 5.2: Heat loss profile from High Efficiency Channel as function of moderator pressure.

Figure 5.3: temperature differences and absolute temperatures of ceramic insulator and pressure tube.
6 CONCLUSION AND RECOMMENDATIONS

The possibility of using various nuclear fuels enclosed in a 43-element fuel bundle (i.e., Variant-20) at SCWR conditions was the subject of the study for this thesis. Therefore, the fuel centerline temperature profile for UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$-SiC, UO$_2$-C, and UO$_2$-BeO fuels was calculated as well as the sheath temperature of the Variant-20 fuel bundles along the heated length of the fuel channel. These fuels were examined at the operating conditions of the supercritical water fuel channels with a maximum thermal power per channel of 9.8 MW$_{th}$. The results showed that under some conditions sheath temperature exceeds the design temperature limit of 850°C when the Variant-20 fuel bundle is used. Therefore, either a new fuel bundle should be designed or fuel channel specifications should be modified in order to comply with the design temperature limit of the sheath.

In regards to the fuel centerline temperature, the maximum fuel centreline temperature exceeds the industry limit of 1850°C for UO$_2$, MOX, and ThO$_2$. On the other hand, the fuel centreline temperature was below the newly established limits when UC, UC$_2$, UN, UO$_2$-SiC, UO$_2$-C, and UO$_2$-BeO were examined. Thus, the result of fuel centerline calculation supports the potential use of high thermal-conductivity and enhanced thermal-conductivity fuels in SCWRs when the use of the Variant-20 fuel bundle is considered. The results conclude that if the use of low thermal-conductivity fuels is considered as an option, a new fuel bundle must be designed. This fuel bundle should have fuel elements with a smaller diameter size, but the number of fuel elements must be increased to compensate for the reduced volume of the fuel contained in the fuel bundle.

When high thermal-conductivity fuels are considered, factors such as thermodynamic, mechanical, and neutronic properties of these fuels were compared based on the available literature. One of the most important thermodynamic properties of a fuel is its thermal conductivity. This thesis discusses that the thermal conductivity of a low thermal-conductivity fuel such as UO$_2$ can be increased by adding a continuous solid phase or long, thin fibers of a high thermal-conductivity material to the fuel. However, further research and development is required to determine the effects of irradiation on these “new” fuels (UO$_2$-SiC, UO$_2$-C, and UO$_2$-BeO) and their thermodynamic and mechanical
properties. Additionally, similar to UO$_2$, the thermal conductivity of enhanced thermal-conductivity fuels decreases as the temperature increases in spite of the improvement on the thermal conductivity. On the other hand, unlike UO$_2$, the thermal conductivities of UC and UN increase at high temperatures (e.g., temperatures above 1000°C). The increasing trend in the thermal conductivity of these high thermal-conductivity fuels increases the margin between the operating temperature of the fuel and its established temperature limit, and enhances the safety of operation. Therefore, this thesis recommends that high thermal-conductivity fuels such as UC and UN are more suitable for future use in SCWRs.

One concern with UC fuel is its chemical compatibility with water, which remains ambiguous due to the discrepancy between the available sources. Consequently, further investigation and research is required. On the other hand, the two main concerns associated with UN fuel are its dissociation at temperatures over 1600°C and the oxidation reaction between UN and water. Furthermore, UN fuel must be enriched in $^{15}$N in order to avoid the formation of $^{14}$C. Therefore, these issues must be contemplated and reflected on the final decision.

When mechanical properties are examined, UC fuel has a higher hardness, lower volumetric swelling, lower thermal expansion, and acceptable thermal-shock resistance than those of UN. Additionally, UC is more stable at high temperatures than UN. Moreover, UC has a higher fission-to-capture ratio than of UN while its uranium atom density is comparable with that of UN and higher than that of UO$_2$. Consequently, according to the available literature survey conducted in this thesis, UC fuel demonstrates desirable mechanical, thermodynamic, and neutronic properties, which make UC fuel a promising candidate for the future use in SCWRs. However, the oxidation reaction between UC and water might be an issue; therefore, this thesis proposes a study concerning the chemical compatibility of UC with water at high temperatures should be performed for adopting UC. Additionally, it recommends further research and investigation in regards to properties of the composite fuels, specially, UO$_2$–BeO should be conducted due to its high potential as an alternative fuel to UC.

The calculations completed in this thesis indicate that the fuel-sheath gap (e.g., 20 µm or
36 \mu m) increases the fuel centerline temperature approximately 3 to 5% compared to those determined based on a no fuel-sheath gap. The temperature increase of the fuel centerline temperature due to the fuel-sheath gap is more pronounced in low thermal-conductivity fuels. Under normal operating conditions, the detrimental effect of the fuel-sheath gap is more significant with wider gaps. However, in the case of conducting any safety analysis the fuel-sheath gap should be taken into consideration.

The calculations also indicate that the heat loss analysis from the High Efficiency Channel design shows a heat loss per fuel channel between 70 kW and 108 kW. The examination of the effects of the insulator thickness and the moderator pressure indicates that the efficiency of the examined fuel-channel design can be improved. Consequently, it recommends that the thickness of the insulator and/or the operating pressure of the moderator should be increased in order to reduce the heat loss to the moderator.

In regards to the operating pressure of the moderator, it is highly recommended to increase the pressure because the operating temperature of the outer surface of the fuel channel (i.e., pressure tube) is currently above the saturation temperature of the moderator. As a result, boiling of the moderator will occur, which in turn results in the formation of high concentrations of deuterium and oxygen in the vessel. Such high concentrations of oxygen and deuterium may results in explosion inside the vessel. Therefore, the pressure of the moderator must be increased to ensure lower heat losses and to eliminate the boiling of the moderator inside the calandria vessel.

In conclusion, the results of this study recommend the modification of the fuel bundle in order to comply with the design temperature limit on the sheath. This fuel-bundle modification also allows the use of low thermal-conductivity fuels. In regards to high thermal-conductivity and enhanced thermal-conductivity fuels, this research recommends the use of UC and UO$_2$-BeO, respectively. This use is conditional on the assurance of chemical compatibility, mechanical behavior, and irradiation behavior of these fuels under the SCWR conditions.
7 FUTURE STUDIES

In the present thesis, the thermal-hydraulics of a SCWR fuel channel has been studied. As a result, a code has been developed in MATLAB, which can be used as the means for examining new fuel-bundle geometries, especially ones with a hollowed fuel. Hollowed fuel geometries have been used in MAGNOX reactors, which are gas-cooled and operate at higher temperatures compared to the conventional nuclear reactors.

Additionally, it is of interest to determine the pressure drop of the coolant along the length of the fuel channel. A pressure drop analysis will increase the accuracy of the current thermal-hydraulics code and allow for the determination of the required pump power. Moreover, a whole core calculation of the SCW CANDU is deemed necessary. This thesis study allows for the coupling of the presented thermal-hydraulics code with a neutronic code. As a result, one will be able to determine the power distribution inside the core. Consequently, the peaking factor and the maximum thermal power per channel can be determined with a whole core analysis. Furthermore, the radial power in each fuel bundle can be determined which in turn opens the possibility for conducting safety analysis on the fuel and sheath.
REFERENCES


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Appendix A – Modeling Fluids

Development of the SCWR concepts requires experimental data on heat transfer properties of water at supercritical conditions. It is a common practice to use other fluids which have lower critical temperatures and pressures compared to those of water, which allows experiments to be performed at lower operating conditions and reduces experimental costs. Additionally, experiments can be conducted at a wider experimental range (Grabezhnaya and Kirillov, 2006).

Carbon dioxide (CO$_2$), R-12, and R134a are the most commonly used modeling fluids, as an alternative to SCW. However, cryogenic fluids such as hydrogen, helium, and nitrogen are also used as modeling fluids (Cheng and Schulenberg, 2001). Nevertheless, the operating conditions of a modeling fluid must be scaled to those of water in order to provide a degree of comparison between the two fluids. Therefore, scaling parameters are required to convert a modeling fluid’s operating conditions such as pressure, bulk-fluid temperature, mass flux, and heat flux to equivalent values of water. Pioro and Duffey (2007) provided scaling parameters for fluid-to-fluid modeling; these parameters are listed in Table A.1.

Table A.1: Scaling parameters for fluid-to-fluid modeling at supercritical conditions (Pioro and Duffey, 2007).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pressure</td>
<td>$(P/P_{cr})<em>A = (P/P</em>{cr})_B$</td>
</tr>
<tr>
<td>Bulk-fluid Temperature (K)</td>
<td>$(T_b/T_{cr})<em>A = (T_b/T</em>{cr})_B$</td>
</tr>
<tr>
<td>Heat Flux</td>
<td>$(qD/k_b \cdot T_b)_A = (qD/k_b \cdot T_b)_B$</td>
</tr>
<tr>
<td>Mass Flux</td>
<td>$(G \cdot D/\mu_b)_A = (G \cdot D/\mu_b)_B$</td>
</tr>
<tr>
<td>Heat Transfer Coefficient</td>
<td>$Nu_A = Nu_B$</td>
</tr>
</tbody>
</table>
Appendix B – Effect of Lanthanum (La) on Thermal Conductivity of ThO$_2$

Figure B.0.1: Thermal conductivity of ThO$_2$ as a function of percent lanthanum.
Appendix C – MATLAB Code for Fuel Centerline Calculation

clear all;
clc;
% This program calculates the temperature profiles of the coolant, sheath
% or cladding as well as the fuel centerline temperature profile.
% The inputs to this code are as follows: the inlet temperature of the
% coolant, the mass flow rate of the coolant, the operating pressure of
% the coolant, and various axial heat flux profiles which are uniform,
% cosine shaped, downstream-skewed, and upstream-skewed cosine. Additionally,
% the thermal conductivities of several nuclear fuels as a function of
% temperature have been included into this code in order to calculate the
% fuel centerline temperature of the following nuclear fuels: UO2, ThO2,
% MOX, UC, UC2, UN, UO2_SiC, and UO2_C. Moreover, appropriate correlations
% have been added to the code in order to calculate the thermal
% conductivities of the aforementioned fuels as a function of porosity.

%*********************************************************************** constant parameters ***********************************************************************

m =4.4;                  % mass flow rate(kg/s);
pressure = 25000 ;      % Operating pressure of the coolant (kPa)
D1=0.10345 ;   r1=D1/2 ;    % The inner diameter of the liner tube (m)
ne = 43 ;              % number of fuel elements of the examined fuel bundle
CLD_OD = 0.0115 ;   % Outer diameter of the fuel elements (m)
CLD_t = 0.48*10^-3 ; % FIND A REFERENCE FOR THIS AND CHANGE IT , THIS IS CANDU6 CLADDING THICKNESS
CLD_ID = CLD_OD - 2 * CLD_t ; % inner diameter of the fuel elements
% tg = (0.01 * CLD_ID)/2 ;  % thickness of the gap between the cladding and the fuel
% tg= 36 * 10^-6 ; % Hu and Wilson (2009)
% Thickness of the gap between the cladding and the fuel (m), Lewis et al. (2008)
tg= 20 * 10^-6 ;
R_Fuel = (CLD_ID - 2*tg)/2 ; % Radius of the fuel pellets

% Power factor to determine the maximum channel power
PF = 1.15 ; % input(’Please enter a power factor: ’) ;
Q = PF*8.5*10^6 ; % Thermal power per channel , W
n = 999; %input(’Into how many segments do you want to divide the fuel-channel: ’);
x = linspace(0.5,7.72,n);
T_in = 350+273.15 ; %Inlet temperature of the coolant, K
R_f = 1 *10^-6 ; % roughness of the fuel in meter
R_c = 1 *10^-6 ; % roughness of the cladding in meter
go_He = 5.2*10^-6 ; % constant for Helium gas (m)
go_Ar = 0.57*10^-6 ; % constant for Argon gas (m)
go_FP = 0.26*10^-6 ; % constant for gaseous fission products(m)

% constants used in order to calculate the thermal conductivity of the MOX
% fuel
xx=2-(2/1); % x = 2-O/M
A = 2.58*xx + 0.035; %m.K/W
C = -0.715*xx+0.286; % m/W
% constants used in order to calculate the thermal conductivity of the
% UO2_BeO fuel. k = y0_beo + a_beo * exp(-1*b_beo * t) + c_beo * exp(-1* d_beo * t) + g_beo * exp(-1* h_beo * t)
% where t is in C
y0_beo = 0.63 ;
a_beo = 31.543 ;
b_beo = 0.007 ;
c_beo = 20.201 ;
d_beo = 0.002 ;
g_beo = 21.032 ;
h_beo = 0.002 ;

%**************************************************************************
%y = 6E-11x^4 - 2E-07x^3 + 0.0003x^2 - 0.2204x + 71.977
% k = abeo *(t^4) + bbeo * (t^3) + cbeo * (t^2) + dbeo * t + fbeo ;
abeo = 6*(10^-11);
bbeo = -1* 2 *(10^-7);
cbeo = 0.0003 ;
dbeo = -1 * 0.2204;
fbeo = 71.977 ;
%**************************************************************************
%**************************************************************************
% CANFLEX variant fuel bundle: 42 elements with an outer diameter of
% 11.5 mm diameter and the centeral element with an OD of 18 or 20 mm
% The centeral element is filled with neutron absorber
FE1_OD = 20*10^-3 ;   FE_OD = 11.5*10^-3 ;
% heated perimeter of the fuel bundle, m
perimeter_FB = pi* (ne-1)*FE_OD ;
% heated area of the fuel bundle, m^2
heated_area = perimeter_FB * 5.772 ;
% cross sectional area of the fuel bundle, m^2
A_bundle = (pi/4)*(1*FE1_OD^2+ (ne-1)*FE_OD^2);
%************************ Liner Tube **************************************
A_tube = (pi/4)*D1^2 ;
% cross sectional area of the liner tube, m^2
A_fl = (A_tube - A_bundle);  % flow area, m^2
p_wet = pi*((ne-1)*FE_OD + 1*FE1_OD + D1);  % wetted perimeter, m
D_hy = 4*A_fl/p_wet;  % hydraulic diameter, m
q = (Q/ heated_area)/1000;  % univorm heat flux, kw/m^2
% heated equivalent diameter, m
D_heated_equivalent_18 = (4*A_fl/perimeter_FB);
% mass flux, kg/(m^2.s)
G = m/A_fl;
%
% The following statements allow the user to choose among various axial
% heat flux profiles, fuels, correlation and etc. for the purpose of
% calculating the fuel centerline temperature profile.
Method1 = input('Choose a heat flux: cosine, upskewed, downskewed, uniform >>> ','s');
Method2 = input('Choose a fuel: uo2, tho2, uc2, uc, un, mox, sic_89%uo2, sic_85%uo2, uo2c_vol1, uo2c_vol2, uo2_beo >>> ','s');
Method3 = 'Mokry';%input('Choose a correlation to calculate Nu, Bishop or Mokry: ','s');
Method4 = input('What is the inside pressure of the cladding in MPa,10, 15, 20, 25 : ','s');
Pg = input('What is the inside pressure of the cladding in MPa,10, 15, 20, 25 : '); switch(Method2)
case 'uo2'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'tho2'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'uc2'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'uc'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'un'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'mox'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'sic_89%uo2'
    disp('The fuel Density is:    97% TD');
case 'sic_85%uo2'
    disp('The fuel Density is:    97% TD');
case 'uo2c_vol1'
    disp('The fuel Density is:    95% TD');
case 'uo2c_vol2'
    disp('The fuel Density is:    95% TD');
case 'uo2_beo'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
case 'sic_89%uo2'
    disp('The fuel Density is:    97% TD');
case 'sic_85%uo2'
    disp('The fuel Density is:    97% TD');
case 'uo2c_vol1'
    disp('The fuel Density is:    95% TD');
case 'uo2c_vol2'
    disp('The fuel Density is:    95% TD');
case 'uo2_beo'
    p=input('Enter percent Porosity of the fuel (i.e., 5 for 5%): ')'/100;
end

switch (Method1)
case 'upskedew'
    disp('These are some constants that are used in the code in order to take into account the effects of the gap conductance on the fuel centerline temperature. These constants are used in order to calculate the gap conductance.
switch (Method4)
case '10'
    sg = 0.5708 ;
    ag = 0.0003 ;
case '15'
    sg = 0.4376 ;
    ag = 0.0009 ;
case '20'
sg = 0.3333 ;
ag = 0.0020 ;
case '25'
sg = 0.2824 ;
ag = 0.0028 ;
end

for i = 1:(n-1);
X(i) = (x(i)+x(i+1))/2;
L(i) = x(i+1)-x(i);
end
L1 = [L,L(1)];

% the following statement calculate the axiom heat flux profile along the
% heated length of the fuel channel.
for i = 1:n;
qx(i)=q*(b0+b1 * (x(i)^1)+b2 * (x(i)^2)+b3 * (x(i)^3)+b4 * (x(i)^4)+b5 * (x(i)^5)+b6 * (x(i)^6)+b7 *
(x(i)^7)); % kW/m^2
end
q_linear = qx * pi * CLD_OD ;% linear heat rating, kW/m
Q_gen = 1000 * 4 *qx / CLD_ID ;% volumetric heat generated, W/m^3

%**************************************************************************
%************** Temperature profile of coolant ***************************
%**************************************************************************
T_c(1) = T_in;
H_c(1) = refpropm('H', 'T', T_c(1), 'P', pressure, 'water');

ef = 0.8 ;
ec = 0.8 ;
c_rad = (5.67*10^-8)/(1/ef+(1/ec)-1);
t=20; % increments of the fuel radius
% this statement divides the radius of the fuel into t segments
r_f = linspace(R_Fuel,0,t);

% the following for loop calculates the outer surface temperature of the
% cladding.then, it determines the inner surface temperature of the
% cladding due to conduction through the thickness of the cladding.next,
% the code calculates the heat transfer coefficient across the gap between
% the fuel and the sheath in order to calculate the outer surface
% temperature of the fuel.finally, the radius of the fuel is divided into
% small segments in order to calculate the fuel centerline temperature.

for i = 1:n;
    delta_T = 4;
    delta_T = 4;
% initially, the sheath or cladding temperature is not known,
% therefore, an initial guess of the surface temperature of the clading
% is required. having made an initial guess, the code calculates the
% cladding temperature using the Newton's law of cooling.then, the
% code compares the initial temperature guess and the new calculated
% values and calculates the temperature difference between these two
% values.next, the code takes the average value of the two succeeding
% temperatures and repeats the calculation until the difference between
% to succeeding calculations is less than 0.1 K.

\[ T_{\text{sheath}}(i) = T_{c}(i) + 300; \]  \% Initial guess of cladding Temperature
\[ T_{f}(1,i) = T_{\text{sheath}}(i) + 500; \]  \% Initial guess of fuel Temperature
if i==n;
    1;
else
    H_c(i+1) = (H_c(i)+(perimeter_FB * L(i)* 1000*qx(i))/(m));
    T_c(i+1) = refpropm('T','H',H_c(i+1),'P',pressure,'water');
end

%************************** To be checked**************************
switch(Method2)
  case'uo2'
    alpha_uo2 = 2.6-0.5*(10^(-3)*T_f(1,i));
    k_f(1,i) = ((1-alpha_uo2*p)/(1-alpha_uo2*0.05))*((100/(7.5408+17.692*(10^(-3))*T_f(1,i))+3.6142*((10^(-3))*T_f(1,i))^2))+((6400/((10^(-3))*T_f(1,i))^(5/2)))*(exp((-16.35)/(10^(-3))*T_f(1,i)))); % UO2
  case'tho2'
    k_f(1,i) = ((1-p)^(2/3))*(1/(0.0327 + 1.603*10^(-4) * T_f(1,i)))); % ThO2
  case'uc2'
    k_f(1,i) = ((1-p)/(1+p))*100*(0.115+2.7*10^(-5)*(T_f(1,i)-273.15)+2.8*10^(-10)*(T_f(1,i)-273.15)^2+3.035*10^(-12)*(T_f(1,i)-273.15)^3); % UC2
  case'uc'
    k_f(1,i) = ((1-p)/(1+p))*100*(1.95*0.1+3.57*10^(-8)*(T_f(1,i)-273.15-850)^2); % UC
  case'un'
    k_f(1,i) = 1.864*(exp(-2.14*p))*(T_f(1,i)^0.361); % UN
  case'mox'
    k_f(1,i) = ((1-p)/(1+2*p))*1.158*((1/(A+C*(10^(-3))*T_f(1,i))))+((6400/((10^(-3))*T_f(1,i))^(5/2)))*(exp((-16.35)/(10^(-3))*T_f(1,i)))); % MOX
  case'sic_85%uo2'
    k_f(1,i) = -9.59*(10^-9) * T_f(1,i)^3 + 4.29*(10^-5) * T_f(1,i)^2 - 6.87*(10^-2) * T_f(1,i) + 46.8; % UO2 + SiC (85% UO2)
  case'sic_89%uo2'
    k_f(1,i) = -1.16*(10^-8) * T_f(1,i)^3 + 5.03*(10^-5) * T_f(1,i)^2 - 7.76*(10^-2) * T_f(1,i) + 49.1; % UO2 + SiC (89% UO2)
  case'uo2_c_vol1'
    k_f(1,i) =15.7; % UO2 + 1 vol % graphite SiC (95% TD UO2)
  case'uo2_c_vol2'
    k_f(1,i) =35.7; % UO2 + 2 vol % graphite SiC (95% TD UO2)
% case 'uo2_beo'
%     k_f(1,i) = y0_beo + a_beo * exp(-1*b_beo * (T_f(1,i) - 273.15)) + c_beo * exp(-1*d_beo * (T_f(1,i) - 273.15)) + g_beo * exp(-1*h_beo * (T_f(1,i) - 273.15)); % uo2_beo
  case'uo2_beo'

137
\[
k_{(1,i)} = \frac{(1-p)}{(1+p)}((1+0.01)/(1-0.01)) \cdot (abeo \cdot ((T_{(1,i)}-273.15)^4) + bbeo \cdot ((T_{(1,i)}-273.15)^3) + cbeo \cdot ((T_{(1,i)}-273.15)^2) + dbeo \cdot (T_{(1,i)}-273.15) + fbeo)
\]

\%

% The radius of the fuel is divided into "t" segments in the process of calculating the fuel centerline temperature. The calculation of the fuel temperature in the radial direction starts from the outer surface temperature of the fuel. Since for each step towards the fuel centerline the inner surface temperature of the fuel is not known, the code uses an iteration in order to calculate the thermal conductivity of the fuel as a function of the average temperature of the inner and outer surfaces of the fuel. Therefore, an initial guess is required as the temperature of the inner surface of the fuel. In each iteration the thermal conductivity is compared with the thermal conductivity calculated based on an initial guess. An iteration converges when the difference between the two successive thermal conductivities is less than 0.5 W/mK.

while (delta_T > 0.1 || delta_k_fuel > .5)
    Enthalpy_w = refpropm('H', 'T', T_sheath(i), 'P', pressure, 'water');
    Enthalpy_b = refpropm('H', 'T', T_c(i), 'P', pressure, 'water');
    Cp_b = refpropm('C', 'T', T_c(i), 'P', pressure, 'water');
    Cp_avg = (Enthalpy_w - Enthalpy_b)/(T_sheath(i)-T_c(i));
    k = refpropm('L', 'T', T_c(i), 'P', pressure, 'water');
    viscosity = refpropm('V', 'T', T_c(i), 'P', pressure, 'water');
    Pr_avg = (viscosity * Cp_avg) / k;
    Pr_b = viscosity * Cp_b / k;
    Density_b = refpropm('D', 'T', T_c(i), 'P', pressure, 'water');
    Density_w = refpropm('D', 'T', T_sheath(i), 'P', pressure, 'water');
    G = m / A_fl; \% mass flux, kg/(m^2.s)
    Re = G * D_hy / viscosity;

switch(Method3)
    case('Mokry')
        Nu = 0.0061 * Re^0.904 * Pr_avg^0.684 * (Density_w/Density_b)^0.564;
    case('Bishop')
        Nu = 0.0069 * Re^0.9 * Pr_avg^0.66 * (Density_w/Density_b)^0.43;
end

% Convection heat transfer coeff. is used to calculate the sheath temperature
htc = Nu * k / D_hy;

% Validate the initial guessing of the cladding temperature
T_sheath_new = (1000*qx(i)/htc)+T_c(i);
delta_T = abs(T_sheath_new - T_sheath(i));
T_min = min(T_sheath(i), T_sheath_new);
T_sheath(i) = T_min + delta_T/2;

% Outer surface temperature of the cladding/sheath
T_CLD_OD(i)=T_sheath(i);
% Thermal conductivity of Inconel-600

\[ k_{\text{CLD}}(i) = 0.0163 \times T_{\text{CLD OD}}(i) + 9.7653; \]

\[ T_{\text{CLD ID}}(i) = T_{\text{CLD OD}}(i) + 1000 \times q_x(i) \times (\text{CLD OD}/2) \times (\log(\text{CLD OD}/\text{CLD ID}))/k_{\text{CLD}}(i); \]

% Gap conductance

\[ T_g(i) = (T_{\text{CLD ID}}(i) + T_f(1,i))/2; \]

\[ k_g(i) = 20 \times a_g \times T_g(i)^{s_g}; \]

\[ g(i) = (1/(1/go_FP)^6((T_g(i)/273)^{s_g + 5.5})*(0.101/P_g)); \]

\[ h_g(i) = k_g(i)/(1.5*(R_f+R_c)+t_g + g(i)); \]

\[ h_{\text{rad}}(i) = c_{\text{rad}} \times ((T_f(1,i)^4 - T_{\text{CLD ID}}(i)^4)/(T_f(1,i) - T_{\text{CLD ID}}(i)))); \]

As = 10; \hspace{1cm} \% (m^5)
rf = 0.54 \times 10^{-6}; \hspace{1cm} \% (m)
rc = 0.24 \times 10^{-6}; \hspace{1cm} \% (m)
r_{\text{eff}} = \sqrt{(rf^2 + rc^2)/2}; \hspace{1cm} \% (m)

\[ \text{Pa}_{\text{Hc}} = 0.002; \]
nc = 0.5;
hs(i) = As * ((2*k_{\text{CLD}}(i)*k_f(1,i))/(k_{\text{CLD}}(i)+k_f(1,i))) \times (\text{Pa}_{\text{Hc}}^{nc} \times (1 / (1 * r_{\text{eff}}^{0.5})); \]

\[ h_{\text{cond}}(i) = h_g(i)+h_{\text{rad}}(i)+hs(i); \]

\[ T_f(1,i)=T_{\text{CLD ID}}(i) + ((q_{\text{linear}}(i)\times 1000)/((\pi \times \text{CLD OD} \times h_{\text{cond}}(i)))); \hspace{1cm} \% with gap \]

% without gap

% Fuel

xx = 2/(2/1); \hspace{1cm} \% x = 2.58
A = 2.58*xx + 0.035; \hspace{1cm} \% m.K/W
C = -0.715*xx+0.286; \hspace{1cm} \% m.W
for s=1:t-1
    delta_k_fuel=10;
    T_f_guess = T_f(s,i)+200;
    while (delta_k_fuel > .5)

switch(Method2)

case'uo2'
    alphauo2 = 2.6-0.5*(10^(-3)*T_f(s,i));
    k_f1(s,i) = ((1-alphauo2*p)/(1-alphauo2*0.05))*((100/(7.5408+17.692*(10^(-3))*T_f(s,i)+3.6142*(10^(-3))*T_f(s,i))^2)+((6400/((10^(-3))*T_f(s,i))^2/52))+(exp(-16.35)/(10^(-3))*T_f(s,i)))% UO2

case'tho2'
    k_f1(s,i) = ((1-p)^2/3)^{(1/(0.0327+1.603*10^(-4) \times T_f(s,i)))}; \hspace{1cm} \% ThO2

case'uc2'
    k_f1(s,i) = ((1-p)/(1+p)*100*0.115+2.7*10^(-5)*T_f(s,i)-273.15+2.8*10^(-10)*T_f(s,i)-273.15)/2+3.305*10^(-12)*T_f(s,i)-273.15)^3; \hspace{1cm} \% UC2

end

end

end
case 'uc''
  k_f(s,i) = ((1-p)/(1+p))^100*(1.95*0.1+3.57*10^(-8))*(T_f(s,i)-273.15-850)^2); % UC

case 'un''
  k_f(s,i) = 1.864*(exp(-2.14*p))*(T_f(s,i)^0.361); % UN

case 'mox''
  k_f(s,i) = ((1-p)/(1+2*p))^1.158*((1/(A+C*(10^(-3)*T_f(s,i))))+((6400/((10^(-3))*T_f(s,i))^5/2)))*(exp((-16.35)/(10^(-3)*T_f(s,i)))); % MOX

%                     case 'sic_85%uo2''
%                         k_f(s,i) = y0_beo + a_beo * exp(-1*b_beo * (T_f(s,i) - 273.15)) + c_beo * exp(-1*h_beo * (T_f(s,i) - 273.15)) + g_beo * exp(-1*d_beo * (T_f(s,i) - 273.15)); % uo2_beo

end

%                     switch (Method2)
%                     case 'uo2''
%                         alphauo2 = 2.6 - 0.5*(10^(-3)*T_f_guess);
%                         k_f_guess(s+1,i) = ((1-p)/(1+p))^100*(10^(-2.8)*T_f_guess) + 2.8*10^(-5)*(T_f_guess-273.15)^3; % UC
%                     case 'un''
%                         k_f_guess(s+1,i) = 1.864*(exp(-2.14*p))*(T_f_guess^0.361); % UN
%                     case 'mox''
%                         k_f_guess(s+1,i) = ((1-p)/(1+2*p))^1.158*((1/(A+C*(10^(-3)*T_f_guess))))+((6400/((10^(-3))*T_f_guess)^5/2)))*(exp((-16.35)/(10^(-3)*T_f_guess))); % MOX
%                     case 'sic_85%uo2''
%                         k_f_guess(s+1,i) = -9.59*(10^(-9) * T_f_guess)^3 + 4.29*(10^(-5) * T_f_guess)^2 - 6.87*(10^(-2)) * T_f_guess + 46.8; % UO2 + SiC ( 85% UO2)
%                     case 'sic_89%uo2''
%                         k_f_guess(s+1,i) = -1.16*(10^(-8) * T_f_guess)^3 + 5.03*(10^(-5) * T_f_guess)^2 - 7.76*(10^(-2)) * T_f_guess + 49.1; % UO2 + SiC ( 89% UO2)
%                     case 'uo2c_vol1''
%                         k_f_guess(s+1,i) = 15.7; % UO2 + 1 vol % graphite SiC ( 95% TD UO2)
%                     case 'uo2c_vol2''
%                         k_f_guess(s+1,i) = 35.7; % UO2 + 2 vol % graphite SiC ( 95% TD UO2)
%                     case 'uo2_beo''
%                         k_f_guess(s+1,i) = ((1-p)/(1+p))^100*(1.95*0.1+3.57*10^(-8))*(T_f_guess-273.15-850)^2); % UC
%                         k_f_guess(s+1,i) = 1.864*(exp(-2.14*p))*(T_f_guess^0.361); % UN
%                         k_f_guess(s+1,i) = ((1-p)/(1+2*p))^1.158*((1/(A+C*(10^(-3)*T_f_guess))))+((6400/((10^(-3))*T_f_guess)^5/2)))*(exp((-16.35)/(10^(-3)*T_f_guess))); % MOX
%                         k_f_guess(s+1,i) = -9.59*(10^(-9) * T_f_guess)^3 + 4.29*(10^(-5) * T_f_guess)^2 - 6.87*(10^(-2)) * T_f_guess + 46.8; % UO2 + SiC ( 85% UO2)
%                         k_f_guess(s+1,i) = -1.16*(10^(-8) * T_f_guess)^3 + 5.03*(10^(-5) * T_f_guess)^2 - 7.76*(10^(-2)) * T_f_guess + 49.1; % UO2 + SiC ( 89% UO2)
%                         case 'uo2c_vol1''
k_f_guess(s+1,i) = 15.7;  % UO2 + 1 vol % graphite
case 'uo2c_vol1'
  k_f_guess(s+1,i) = 35.7;  % UO2 + 2 vol % graphite
% case 'uo2_beo'
% k_f_guess(s+1,i) = y0_beo + a_beo * exp(-1*b_beo * (T_f_guess - 273.15)) + c_beo * exp(-1*d_beo * (T_f_guess - 273.15)) + g_beo * exp(-1*h_beo * (T_f_guess - 273.15));  % uo2_beo
case 'uo2_beo'

k_f_guess(s+1,i) = ((1-p)/(1+p))*((1+0.01)/(1-0.01))* (abeo *((T_f_guess - 273.15)^4) + bbeo * ((T_f_guess-273.15)^3) + cbeo * ((T_f_guess-273.15)^2) + dbeo * (T_f_guess-273.15) + fbeo) ;  % uo2_beo
end

k_f(s,i)=(k_f_guess(s+1,i)+k_f1(s,i))/2;
T_f(s+1,i) = (Q_gen(i) * (r_f(s)^2 - r_f(s+1)^2))/(4*k_f(s,i))+T_f(s,i);
T_f_avg = (T_f(s+1,i)+T_f(s,i))/2;
switch(Method2)
case 'uo2'
  alphauo2 = 2.6-0.5*(10^(-3)*T_f_avg);
  k_f2(s+1,i) = ((1-alphauo2*p)/(1-alphauo2*0.05))*(100/(7.5408+17.692*(10^(-3)*T_f_avg)+3.6142*((10^(-3))*T_f_avg)^2)+6400/(((10^(-3))*T_f_avg)^5/2)))*exp((-16.35)/(10^(-3)*T_f_avg)));  % UO2
-case 'tho2'
  k_f2(s+1,i)= ((1-p)/(2/3))*((1/(0.0327 + 1.603*10^(-4) * T_f_avg));  % ThO2
case 'uc2'
  k_f2(s+1,i) = ((1-p)/(1+p))*(100*0.115+2.7*10^(-5)*(T_f_avg-273.15)+2.8*10^(-10)*(T_f_avg-273.15)^2+3.035*10^(-10)*(T_f_avg-273.15)^3);  % UC2
-case 'uc'
  k_f2(s+1,i) = ((1-p)/(1+p)))*(T_f_avg-273.15)^2+3.035*10^(-10)*(T_f_avg-273.15)^3);
-case 'un'
  k_f2(s+1,i) = 1.95*0.1+3.57*10^(-8)*(T_f_avg-273.15-850)^2);
-case 'mox'
  k_f2(s+1,i) = ((1-p)/(1+2*p))*1.158*((1/(A+C*(10^(-3)*T_f_avg)))+((6400/(((10^(-3))*T_f_avg)^5/2)))*exp((-16.35)/(10^(-3)*T_f_avg)))));  % MOX
-case 'sic_85%uo2'
  k_f2(s+1)= -9.59*(10^(-9) * T_f_avg^3 + 4.29*(10^(-5) * T_f_avg^2 - 6.87*(10^(-2) * T_f_avg + 46.8;  % UO2 + SiC (85% UO2)
case 'sic_89%uo2'
  k_f2(s+1)= -1.16*(10^(-8) * T_f_avg^3 + 5.03*(10^(-5) * T_f_avg^2 - 7.76*(10^(-2) * T_f_avg + 49.1;  % UO2 + SiC (89% UO2)
case 'uo2c_vol1'
  k_f2(s+1,i) = 15.7;  % UO2 + 1 vol% graphite
case 'uo2c_vol2'
  k_f2(s+1,i) = 35.7;  % UO2 + 1 vol% graphite
% case 'uo2_beo'
% k_f2(s+1,i) = y0_beo + a_beo * exp(-1*b_beo * (T_f_avg - 273.15)) + c_beo * exp(-1*d_beo * (T_f_avg - 273.15)) + g_beo * exp(-1*h_beo * (T_f_avg - 273.15));  % uo2_beo
\[ \delta k_f = |k_f(s+1,i) - k_f\text{ guess}(s+1,i)|; \]
\[ T_f\text{ guess} = \min(T_f(s+1,i), T_f\text{ guess}) + \frac{|T_f(s+1,i) - T_f\text{ guess}|}{2}; \]

\begin{verbatim}
for i = 1:n
    Density_c_b(i) = Density_b;
    Density_c_w(i) = Density_w;
    Reynolds(i) = Re;
    Nusselt(i) = Nu;
    Prandtl(i) = Pr_avg;
    HTC_h(i) = htc;
    Viscosity(i) = viscosity;
    ThermalConductivity_coolant(i) = k;
    Specific_Heat_Water(i) = Cp_avg;
    ENTHAL(i) = Enthalpy_b;
    Cp_B(i) = Cp_b; % kJ/kgK
    Pr_B(i) = Pr_b;
end

plot(x, qx);
figure(2); plot(x, T_c - 273.15); hold on; plot(x, T_CLD_OD - 273.15, 'g'); plot(x, T_Fuel_Centerline - 273.15, 'r'); hold off;
figure(3); plot(x, q_linear1, 'r'); hold on; plot(x, q_linear2);

\end{verbatim}

\begin{verbatim}
% TEST to check the power of the channel
Hin = refpropm('H', 'T', T_c(1), 'P', pressure, 'water');
Hout = refpropm('H', 'T', T_c(n), 'P', pressure, 'water');
Q_TEST = m * (Hout - Hin);
T_Max = max(T_Fuel_Centerline) - 273
\end{verbatim}
RATIO_DENSITY = (Density_c_w ./Density_c_b)';
DENSITY_B = Density_c_b';
DENSITY_W = Density_c_w';
REYNOLDS = Reynolds';
NUSSELT = Nusselt';

DYNAMIC_VISCOSITY = (10^6 * Viscosity)';  % (micro Pa.s)
THERMALCONDUCTIVITY = ThermalConductivity_coolant';
ENTHALPY = (ENTHAL/1000)';  %kJ/kg
CP_NIST = (Cp_B/1000)';  % kJ/kgK
PR_NIST = Pr_B';
PRANDTL_AVG = Prandtl';
CP_AVG = (Specific_Heat_Water/1000)';% kJ/kgK
M4 = [T_COOLANT,T_CLADDING,T_FUEL_C,HTC];
M10 =
[T_COOLANT,T_CLADDING,T_FUEL_C,HTC,THERMALCONDUCTIVITY,DYNAMIC_VISCOSITY,
DENSITY_B,DENSITY_W,RATIO_DENSITY,PRANDTL_AVG];
Appendix D – Verification of MATLAB Code

Figure D.1: Comparison of coolant, sheath, and fuel centerline temperature profiles of MATLAB and EXCEL calculations.
Appendix E – MATLAB Code for Heat Loss Calculation

% This program calculates the heat loss from the High Efficiency Channel design at the SCWR conditions.

clear all;
cld

%*********** Constant Parameters
m = 4.4; % Mass flow rate, (kg/s)
pressure = 25000; % Coolant pressure, (kPa)
pressure_m = 200; % Moderator pressure, (kPa)
Tmod = 80 + 273; % Moderator temperature, K
nscw = 220; % Number of SCW channels
nsh = 80; % Number of Steam Re-Heat (SRH) channels
T1 = 350 + 273.15; % Inlet temperature of the coolant in SCW channels(K)
FT_ID = 0.10345; FT_IR = FT_ID/2; % Inner diameter and radius of the Flow Tube (FT)
Pthscw = 8.5*10^6; % Thermal power per channel in SCW channel
Lch = 5.772; % Heated length of the fuel channel, m

%**************************************************************************
%**************************************************************************
F1_OD = 20*10^-3; F42_OD = 11.5*10^-3; % Variant-18 fuel bundle: 42 elements with 11.5 mm diameter and central element with 18 mm diameter (this is not fuel but poison)
pFB = pi*42*F42_OD;
AFB = (pi/4)*(1*F1_OD^2 + 42*F42_OD^2);

%**************************************************************************
%**************************************************************************

Method1 = input('Choose a heat flux: cosine, upskewed, downskewed, uniform >>> ','s');
switch (Method1)
case 'upskewed'
% Villamere et al. UP-Stream Skewed
b0=0.0131929303; b1=1.5915371326; b2=-0.2119130363; b3=-0.3695601674;
b4=0.1795787014; b5=-0.0313491961; b6=1.9800082669e-3; b7=-1.2660441982e-5;
case 'cosine'
% Villamere et al. Cosine
b0=0.0826674395; b1=0.870995913; b2=0.1768748998; b3=-0.3226217824;
b4=0.1220705069; b5=-0.0207148461; b6=1.4036013137e-3; b7=-1.6731615192e-5;
case 'downskewed'
% Villamere et al. Down-Stream Skewed
b0=0.0921920266; b1=0.7309822355; b2=0.3752256758; b3=-0.5331702337;

145
b4=0.206326024; b5=-0.0332278541; b6=1.8826011775e-3; b7=0.0;

\[
\begin{align*}
\text{case 'uniform'} \\
\% Uniform \\
b0=1; b1=0; b2=0; b3=0; b4=0; b5=0; b6=0; b7=0;
\end{align*}
\]

Method2 = input('Choose an equation for the calculation of the effective thermal conductivity of the YSZ Insulator: Jiang, Maxwell, Maxwell-Eucken, Landauere, Meredith, vf >>> ');

space = input('Please enter the number of segments you wish to divide the length of the fuel channel: '); x = linspace(0, Lch, space); n = length(x); T_coolant_K = T1*ones(1, n);

for i = 1:n;
    qx(i) = q_avg*(b0 + b1*(x(i)^1) + b2*(x(i)^2) + b3*(x(i)^3) + b4*(x(i)^4) + b5*(x(i)^5) + b6*(x(i)^6) + b7*(x(i)^7)); % kW/m^2
end

G = m/A_fl; % Mass flux, kg/m2s

for i = 1:n-1;
    L(i) = x(i+1)-x(i);

    Cp = refpropm('C', 'T', T_coolant_K(i), 'P', pressure, 'water'); % duteus boilter
    T_coolant_K(i+1) = T_coolant_K(i) + L(i)*(qx(i)*p)/(m*Cp);
end

plot(x, T_coolant_K - 273.15); % Coolant Temperature in Celsius

Tw = 370+273.15; Tw2 = 350+273.15; % Seath Temperature Initial Guess

T_min = min(Tw, Tw2); T_max = max(Tw, Tw2);
Delta_T = T_max - T_min; s1 = length(T_coolant_K);
for i = 1:s1;
    count = 0; % just to calculate the number of iterrations
    while (Delta_T > 0.1);
        Tb = T_coolant_K(i);

        mu = refpropm('V', 'T', Tb, 'P', pressure, 'water'); % coolnat temperatur (K)
        Cp = (Hw - Hb)/(Tw - Tb); % specific heat (J/kg.K)
        Pr = Cp*mu/k;

        Hw = refpropm('H', 'T', Tw, 'P', pressure, 'water'); % enthalpy
        Hb = refpropm('H', 'T', Tb, 'P', pressure, 'water'); % enthalpy@ bulk
        T = (J/kg)
        row = refpropm('D', 'T', Tw, 'P', pressure, 'water'); % density at wall
        T = (kg/m^3)
        rob = refpropm('D', 'T', Tb, 'P', pressure, 'water'); % density at bulk
        T = (kg/m^3)
        k = refpropm('L', 'T', Tb, 'P', pressure, 'water'); % thermal conductivity
        (W/m-K)
        Cp_DB = refpropm('C', 'T', Tb, 'P', pressure, 'water');
    end
end

146
\( \text{Re} = \frac{m \cdot D_{hy}}{(\mu \cdot A_{fl})} \)

\( \text{Nu} = 0.0061 \times (\text{Re}^{0.904}) \times (\text{Pr}^{0.684}) \times ((\text{row} / \text{rob})^{0.564}) \); \hspace{1cm} \text{Mikry et al.(2009) correlation}

\( \text{Nu}_{pr} = \frac{C_p \cdot DB \cdot \mu}{k} \)

\( \text{Nu}_{DB} = 0.023 \times (\text{Re}^{0.8}) \times (\text{Nu}_{pr}^{0.4}) \);

\( h = \frac{\text{Nu} \cdot k}{D_{hy}} \);

\( h_{DB} = \frac{\text{Nu}_{DB} \cdot k}{D_{hy}} \);

\[ T_w = \frac{(qx(i)/h)+ T_b}{2} \]

\( \text{ST}(i) = T_w - 273.15; \)

\[ \text{L}(i) = \frac{(x(i+1)-x(i))}{2}; \]

\[ X(i) = \frac{(x(i)+x(i+1))}{2}; \]

\[ h_{convection}(i) = \frac{(h_{\text{Bishop}}(i)+h_{\text{Bishop}}(i+1))}{2}; \]

\[ R_{\text{coolant}}(i) = \frac{1}{(h_{\text{convection}}(i) \times \pi \times \text{FT_ID} \times L(i))}; \]

\[ H1A1_{\text{INVERSE}}(i) = \frac{1}{(h_{\text{convection}}(i) \times \pi \times \text{FT_ID} \times L(i))}; \]
n=length(x);
Delta_Ts2 = 7*ones(1,n);    Delta_Ts3 = 7*ones(1,n);
T_guess1 = 760;             Ts2 = linspace(T_guess1, T_guess1+50, n);
T_guess2 = 363;             Ts3 = linspace(T_guess2, T_guess2+50, n);

Method3 = input('What is the mole percent of YSZ: 3 or 8 >>> ','s');

switch (Method3)
    case '3'
y0ins = 3.0182; ains = 4.8394E-005; bins = -1.9593E-006; cins = 1.2489E-009;
    case '8'
y0ins = 1.725; ains = 0.001; bins = -1.734E-006; cins = 8.005E-010;
end

% coefficient for the calculation of the thermal conductivity of solid 3 mole % YSZ
% 8 mole % YSZ

insulator_thickness = input('Please enter the thickness of the insulator in mm:   ')/1000;
porosity = input('What is the percent porosity of the insulator (i.e. 70% = 0.7):    ');

insulator_thickness = 7/1000;
porosity = .7;

v2 = porosity;
v1 = 1-v2;

A = 2*v1 - v2;
B = 2*v2 - v1;

for i=1:n-1;
    while(Delta_Ts2(i) > .2 || Delta_Ts3(i) > .2);
        Ts1 = T_coolant_K;
        % LENGTH (n-1)
        Ins_IR = 0.106/2; Ins_OR = Ins_IR + insulator_thickness; PT_OR = Ins_OR + 0.0075; PT_OD = 2*PT_OR;
        r = Ins_IR:0.0001:Ins_OR; rs = length(r);
        for i=1:n;
            for j=1:rs;
                TIns(i,j) = (Ts1(i) - Ts2(i))*(1/log(Ins_IR/Ins_OR))*(log(r(j)/Ins_OR))+Ts2(i);
                K_WATER = refpropm('L', T, TIns(i,j), 'pressure', 'water');
                K_YSZ = y0ins + ains * (TIns(i,j) - 273.15)+ bins * (TIns(i,j) - 273.15)^2 + cins * (TIns(i,j) - 273.15)^3;
            end
        end
    end
end

switch (Method2)
case 'Jiang'
f = 4.5;  % A constant
\[ k_{\text{eff}}(i,j) = \frac{1}{(f^2 - 2)} \left[ \frac{(f \cdot v_2 \cdot 0.5 - 1) \cdot K_{\text{WATER}}}{2} + (f \cdot v_1 \cdot 0.5 - 1) \cdot K_{\text{YSZ}} + \sqrt{\left( (f \cdot v_2 \cdot 0.5 - 1) \cdot K_{\text{WATER}} + (f \cdot v_1 \cdot 0.5 - 1) \cdot K_{\text{YSZ}} \right)^2} + (2 \cdot f - 4) \cdot K_{\text{WATER}} \cdot K_{\text{YSZ}} \right]; \]

**case 'Maxwell'**

\[ k_{\text{eff}}(i,j) = K_{\text{YSZ}} \left( \frac{(2 \cdot K_{\text{YSZ}} + K_{\text{WATER}} + 2 \cdot v_2 \cdot (K_{\text{WATER}} - K_{\text{YSZ}}))^v_1}{(2 \cdot K_{\text{YSZ}} + K_{\text{WATER}} - v_2 \cdot (K_{\text{WATER}} - K_{\text{YSZ}}))^v_1} \right); \]

**case 'Maxwell-Eucken'**

\[ k_{\text{eff}}(i,j) = K_{\text{YSZ}} \left( \frac{(2 \cdot K_{\text{WATER}} + K_{\text{YSZ}} - 2 \cdot (K_{\text{WATER}} - K_{\text{YSZ}}) \cdot v_1)}{(2 \cdot K_{\text{WATER}} + K_{\text{YSZ}} + (K_{\text{WATER}} - K_{\text{YSZ}}) \cdot v_1)} \right); \]

**case 'Landaure'**

\[ B_1 = A \cdot K_{\text{YSZ}} + B \cdot K_{\text{WATER}}; \]
\[ C_1 = K_{\text{YSZ}} \cdot K_{\text{WATER}}; \]

\[ k_{\text{eff}}(i,j) = (-B_1 - \sqrt{(B_1^2 - 4 \cdot (-2) \cdot C_1)}/(-4)); \]

**case 'Meredith'**

\[ k_m = K_{\text{WATER}} / K_{\text{YSZ}}; \]
\[ a_m = (2 + k_m)/(1 - k_m); \]
\[ b_m = (6 + 3 \cdot k_m)/(4 + 3 \cdot k_m); \]
\[ c_m = (3 - 3 \cdot k_m)/(4 + 3 \cdot k_m); \]

\[ k_{\text{eff}}(i,j) = K_{\text{YSZ}} \left( \frac{a_m - 2 \cdot v_2 + 0.409 \cdot b_m \cdot (v_2^{7/3}) - 2.133 \cdot c_m \cdot v_2^{10/3}}{a_m + v_2 + 0.409 \cdot b_m \cdot (v_2^{7/3}) - 0.906 \cdot c_m \cdot v_2^{10/3}} \right); \]

**case 'vf'**

\[ k_{\text{eff}}(i,j) = v_1 \cdot K_{\text{YSZ}} + v_2 \cdot K_{\text{WATER}}; \]
for i=1:n;
    k_PT(i) = 16.85-2.186*(10^-3)*T_k_PT(i)+8.899*(10^-6)*(T_k_PT(i))^2;
    k2(i)= 16.85-2.186*(10^-3)*Ts2(i)+8.899*(10^-6)*Ts2(i)^2;
    k3(i)= 16.85-2.186*(10^-3)*Ts3(i)+8.899*(10^-6)*Ts3(i)^2;
end

for i=1:(n-1);
    k_PT_R(i) = (k_PT(i)+k_PT(i+1))./2;
    L(i)= x(i+1)-x(i);
    R_PT(i) = (log(PT_OR/Ins_OR))/(2*pi*L(i)*k_PT_R(i));
    convection_hh(i)=2*k_PT_R(i)/(2*Ins_OR*log(PT_OR/Ins_OR));
end

for i=1:n;
    mu_m(i)= refpropm('V', 'T', T_avg(i), 'P', pressure_m, 'D2O');
    density_m(i)= refpropm('D', 'T', T_avg(i), 'P', pressure_m, 'D2O');
    v(i)= mu_m(i)/density_m(i);
    ThermalConductivity_m(i) = refpropm('L', 'T', T_avg(i), 'P', pressure_m, 'D2O');
    SpecificHeat_m(i)= refpropm('C', 'T', T_avg(i), 'P', pressure_m, 'D2O');
    thermal_diffusivity(i)= ThermalConductivity_m(i)/(density_m(i)*SpecificHeat_m(i));
    prandtl(i)= (mu_m(i)*SpecificHeat_m(i))/ThermalConductivity_m(i);
    k(i)= ThermalConductivity_m(i);
    Ra(i)=(9.8*(1/T_avg(i))*(Ts3(i)-Tm(i))*PT_OD^3)/(v(i)*thermal_diffusivity(i));
    Nu(i)=(0.6+((0.386*Ra(i)^(1/6))/((1+(0.559/prandtl(i))^(9/16))^(8/27))))^2;
    h_m(i)=Nu(i)*k(i)/PT_OD;
end

for i=1:(n-1);
    h_R(i)= (h_m(i)+h_m(i+1))./2;
    R_m(i) = 1/(pi*PT_OD*h_R(i)*L(i));
end

UA_inverse = H1A1_INVERSE + RIns_tot+ R_PT+ R_m;
UA = 1./UA_inverse;
dQ=UA.*Delta_T;

for i=1:n-1;
    Tc_new(i) = T_coolant_K(i) - dQ(i)*(H1A1_INVERSE(i)+RIns_tot(i));
    Tc_min(i)= min(Tc_new(i),Ts2(i));
    Tc_max(i)= max(Tc_new(i),Ts2(i));
    Delta_Ts2(i) = Tc_max(i) - Tc_min(i);
end

Delta_Ts2(i) = Tc_max(i) - Tc_min(i);
Ts3_max(i) = max(Ts3_new(i), Ts3(i));
Delta_Ts3(i) = Ts3_max(i) - Ts3_min(i);
end
for i = 1:n-2;
TTs2(i) = (Ts2_new(i) + Ts2_new(i+1))/2;
TTs3(i) = (Ts3_new(i) + Ts3_new(i+1))/2;
end
DTS1 = Ts2_new(2) - Ts2_new(1);
DTS2 = Ts2_new(n-2) - Ts2_new(n-3);
T2_one = TTs2(1) - DTS1;
T2_end = TTs2(n-2) + DTS2;
Ts2 = [T2_one, TTs2, T2_end];
DTS3 = Ts3_new(2) - Ts3_new(1);
DTS4 = Ts3_new(n-2) - Ts3_new(n-3);
T3_one = TTs3(1) - DTS3;
T3_end = TTs3(n-2) + DTS4;
Ts3 = [T3_one, TTs3, T3_end];
end
end
disp('******************************************************************')
disp('The total heat transferred from the coolant to the moderator is: ');
disp(num2str(Q), ' (MW)');
disp('******************************************************************')

HeatLoss = dQ/5.772; % heat loss profile per channel, W
HeatLoss_Ch = Q_one % total heat loss per channel, W

figure(2); plot(X,RIns_tot)
figure(3); plot(X,dQ)
Appendix F – Fuel Centerline Temperature Profiles

Figure F.1: Temperature and HTC profiles for UO$_2$ Fuel at maximum channel power with uniform AHFP.

Figure F.2: Temperature and HTC profiles for UO$_2$ fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.3: Temperature and HTC profiles for UO₂ fuel at maximum channel power with cosine AHFP.

Figure F.4: Temperature and HTC profiles for UO₂ fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.5: Temperature and HTC profiles for MOX fuel at maximum channel power with uniform AHFP.

Figure F.6: Temperature and HTC profiles for MOX fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.7: Temperature and HTC profiles for MOX fuel at maximum channel power with cosine AHFP.

Figure F.8: Temperature and HTC profiles for MOX fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.9: Temperature and HTC profiles for ThO\textsubscript{2} fuel at maximum channel power with uniform AHFP.

Figure F.10: Temperature and HTC profiles for ThO\textsubscript{2} fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.11: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with cosine AHFP.

Figure F.12: Temperature and HTC profiles for ThO$_2$ fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.13: Temperature and HTC profiles for UC fuel at maximum channel power with uniform AHFP.

Figure F.14: Temperature and HTC profiles for UC fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.15: Temperature and HTC profiles for UC Fuel at Maximum Channel Power with Cosine AHFP.

Figure F.16: Temperature and HTC profiles for UC fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.17: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with uniform AHFP.

Figure F.18: Temperature and HTC profiles for UC$_2$ fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.19: Temperature and HTC profiles for UC₂ fuel at maximum channel power with cosine AHFP.

Figure F.20: Temperature and HTC profiles for UC₂ fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.21: Temperature and HTC profiles for UN fuel at maximum channel power with uniform AHFP.

Figure F.22: Temperature and HTC profiles for UN fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.23: Temperature and HTC profiles for UN fuel at maximum channel power with cosine AHFP.

Figure F.24: Temperature and HTC profiles for UN fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.25: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with uniform AHFP.

Figure F.26: Temperature and HTC profiles for UO$_2$–SiC fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.27: Temperature and HTC profiles for UO₂–SiC fuel at maximum channel power with cosine AHFP.

Figure F.28: Temperature and HTC profiles for UO₂–SiC fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.29: Temperature and HTC profiles for UO₂–C fuel at maximum channel power with uniform AHFP.

Figure F.30: Temperature and HTC profiles for UO₂–C fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.31: Temperature and HTC profiles for UO₂–C fuel at maximum channel power with cosine AHFP.

Figure F.32: Temperature and HTC profiles for UO₂–C fuel at maximum channel power with downstream-skewed cosine AHFP.
Figure F.33: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with uniform AHFP.

Figure F.34: Temperature and HTC profiles for UO$_2$–BeO fuel at maximum channel power with upstream-skewed cosine AHFP.
Figure F.35: Temperature and HTC profiles for UO₂–BeO fuel at maximum channel power with cosine AHFP.

Figure F.36: Temperature and HTC profiles for UO₂–BeO fuel at maximum channel power with downstream-skewed cosine AHFP.
Table F.1: Maximum sheath and fuel centerline temperatures of UO$_2$, MOX, ThO$_2$, UC, UC$_2$, UN, UO$_2$–SiC, UO$_2$–C, and UO$_2$–BeO based on a 20-μm fuel-sheath gap width.

<table>
<thead>
<tr>
<th>Sheath $T_{\text{max}}$, °C</th>
<th>Heat Flux</th>
<th>Cosine</th>
<th>Up-Skewed</th>
<th>Down-Skewed</th>
<th>Uniform</th>
</tr>
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<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fuel</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>UO$_2$</td>
<td>2646</td>
<td>2489</td>
<td>2719</td>
<td>2328</td>
<td></td>
</tr>
<tr>
<td>MOX</td>
<td>2704</td>
<td>2569</td>
<td>2767</td>
<td>2383</td>
<td></td>
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<tr>
<td>ThO$_2$</td>
<td>2290</td>
<td>2019</td>
<td>2433</td>
<td>1991</td>
<td></td>
</tr>
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<td>UC</td>
<td>1121</td>
<td>1058</td>
<td>1186</td>
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<td>UC$_2$</td>
<td>1197</td>
<td>1123</td>
<td>1260</td>
<td>1213</td>
<td></td>
</tr>
<tr>
<td>UN</td>
<td>1077</td>
<td>1023</td>
<td>1138</td>
<td>1121</td>
<td></td>
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<tr>
<td>UO$_2$(85%) + SiC (12%)</td>
<td>1394</td>
<td>1263</td>
<td>1495</td>
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<td>UO$_2$–BeO</td>
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</table>
Appendix G – W. Peiman Publications

In total: 1 paper in refereed journal, 21 papers in refereed proceedings of international and national conferences and 4 technical reports.

Refereed Journal Papers


Refereed Conference Proceedings


Conference On Nuclear Engineering (ICONE–18), Xi’an, China, May 17–21, Paper 29990, 10 pages.


of the 18th International Conference On Nuclear Engineering (ICONE-18), Xi’an, China, May 17-21, Paper 29993, 9 pages.


Technical Reports:


Appendix H – W. Peiman Conference Attendance

In total five conferences have been attended.

1. 5th International symposium of Supercritical Water-Cooled Reactors (ISSCWR-5), Vancouver, Canada, March 13-16, 2011.


3. 18th International Conference on Nuclear Engineering (ICONE-18), Xi’an, China, May 17 – 21st, 2010.


5. 17th International Conference on Nuclear Engineering (ICONE-17), Brussels, Belgium, July 12 – 16, 2009.
Appendix I – W. Peiman Awards and Honours

Recipient of ICONE-18 “Best Poster of North American Student Track” for:


Recipient of ICONE-17 “Best Paper of Student Track” for: