The Pennsylvania State University
The Graduate School
College of Engineering

DATA ANALYSIS AND MODELING OF NESTOR SSG HEATED ROD BUNDLE
EXPERIMENTS USING VIPRE-I FOR THE ASSESSMENT OF THE ONSET OF
NUCLEATE BOILING CRITERION

A Thesis in
Nuclear Engineering
by
Robert K. Salko

© 2010 Robert K. Salko

Submitted in Partial Fulfillment
of the Requirements
for the Degree of

Master of Science

May 2010
The thesis of Robert K. Salko was reviewed and approved* by the following:

Maria N. Avramova
Assistant Professor of Nuclear Engineering
Thesis Advisor

Kostadin N. Ivanov
Distinguished Professor of Nuclear Engineering

Jack Brenizer
J. “Lee” Everett Professor of Mechanical and Nuclear Engineering
Chair of Nuclear Engineering

*Signatures are on file in the Graduate School
ABSTRACT

The modeling and understanding of Axial Offset Anomaly (AOA) and its imposed risks in Pressurized Water Reactor (PWR) cores is important because of the severe restrictions it places on the operation of the core. AOA involves a skewing of the axial core power profile most likely due to boron depositing in the crud layer often present on higher powered fuel rods. Because crud deposition is pronounced in areas of localized sub-cooled boiling, it is important to have accurate prediction models for determining the localization of boiling zones in PWR cores in order to understand and model AOA. Due to these concerns and the need for accurate Onset of Nucleate Boiling (ONB) modeling, an experimental program known as NESTOR was performed by Commissariat à l’Energie Atomique (CEA, France), Electricité de France (EDF, France) and Electric Power Research Institute (EPRI, USA). The aim of the NESTOR program was to assess and, if necessary, develop an accurate ONB model based on wall superheat criterion. The experiment involved using two identical 5x5 rod-bundle flow loops, one heated and one unheated, for the purpose of gathering detailed rod surface temperature and sub-channel axial velocity measurements at prototypical PWR conditions. Each of the flow loops included tests run on both a bare-bundle configuration, which contained only Simple Support Grids (SSG), and a prototypical rod-bundle configuration, which contained alternating SSGs and Mixing Vane Grids (MVG). Test data analyses in this configuration have since been jointly carried out by the three NESTOR partners, each using its own T/H core code (FLICA IV for CEA, THYC-COEUR for EDF and VIPRE-I for US Penn State University on behalf of EPRI). This thesis describes the analyses results and conclusions based on SSG configuration data from the PSU perspective.

Three stages of analysis were completed, which included:
(i) VIPRE-I code calibration to experimental flow loops, including loss coefficient and mixing model input data determination

(ii) Single-phase heat transfer test analysis and development of a model for grid enhancement of single-phase heat transfer dedicated to the SSG bundle configuration

(iii) ONB test analysis and assessment of VIPRE-I capabilities of modeling ONB

The conclusions of the SSG test analysis showed that heat transfer models correctly represented the single-phase test data, but led to an overprediction of ONB wall superheat by 1-3.5 K. The over-prediction led to an uncertainty of 21-26 cm in ONB location calculation in the SSG configuration using VIPRE-I. However, due to testing concerns, it was not possible to use this insight for refinement of the ONB model.
## TABLE OF CONTENTS

<table>
<thead>
<tr>
<th>Section</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>LIST OF FIGURES</td>
<td>VII</td>
</tr>
<tr>
<td>LIST OF TABLES</td>
<td>X</td>
</tr>
<tr>
<td>ACKNOWLEDGEMENTS</td>
<td>XII</td>
</tr>
<tr>
<td><strong>CHAPTER 1: INTRODUCTION</strong></td>
<td></td>
</tr>
<tr>
<td>1.1 NESTOR PROGRAM BACKGROUND</td>
<td>1</td>
</tr>
<tr>
<td>1.2 NESTOR EXPERIMENTS OVERVIEW</td>
<td>2</td>
</tr>
<tr>
<td>1.3 CONCURRENT ANALYSIS AND DATA ANALYSIS OVERVIEW</td>
<td>7</td>
</tr>
<tr>
<td>1.4 VIPRE-I CODE LOGIC AND MODELS</td>
<td>8</td>
</tr>
<tr>
<td>1.4.1 Code Background</td>
<td>8</td>
</tr>
<tr>
<td>1.4.2 Code Basis for Thermal Hydraulic Analysis</td>
<td>9</td>
</tr>
<tr>
<td><strong>CHAPTER 2: NESTOR TEST DATA PRE-PROCESSING</strong></td>
<td></td>
</tr>
<tr>
<td>2.1 CORRECTION OF MANIVEL AVERAGE SUB-CHANNEL VELOCITIES</td>
<td>13</td>
</tr>
<tr>
<td>2.2 CORRECTION OF OMEGA TEMPERATURE MEASUREMENTS</td>
<td>17</td>
</tr>
<tr>
<td>2.3 ROD THICKNESS CORRECTION</td>
<td>20</td>
</tr>
<tr>
<td>2.4 CALCULATION OF HEATER ROD OUTER WALL SURFACE TEMPERATURES</td>
<td>24</td>
</tr>
<tr>
<td>2.5 CALCULATION OF PRESSURE DROP IN OMEGA FLOW LOOP OUTLET SECTION</td>
<td>25</td>
</tr>
<tr>
<td><strong>CHAPTER 3: MODELING OF NESTOR TESTS WITH VIPRE-I</strong></td>
<td></td>
</tr>
<tr>
<td>3.1 GEOMETRY MODELING CONSIDERATIONS (GEOM CARD)</td>
<td>33</td>
</tr>
<tr>
<td>3.2 RODS MODELING CONSIDERATIONS (ROD CARD)</td>
<td>41</td>
</tr>
<tr>
<td>3.3 SIMULATION OPERATING CONDITION INPUT (OPER CARD)</td>
<td>42</td>
</tr>
<tr>
<td>3.4 FLOW-FIELD LOSS COEFFICIENTS (DRAG AND GRID CARDS)</td>
<td>44</td>
</tr>
<tr>
<td>3.5 CORRELATIONS AND CONSTITUTIVE MODELS (CORR CARD)</td>
<td>45</td>
</tr>
</tbody>
</table>
3.6 Turbulent Mixing Model (MIXX Card) ................................................................. 46

CHAPTER 4 : VIPRE-I MODEL OPTIMIZATION ...................................................... 49

4.1 Friction and Grid-Loss Calibration ............................................................... 49
4.2 Turbulent Viscosity Optimization .................................................................. 55
4.3 Turbulent Conductivity Optimization ............................................................ 58
4.4 Velocity, Temperature and Pressure Sensitivity to Calibration Parameters .... 64
  4.4.1 Computed Pressure Drop Sensitivity ......................................................... 65
  4.4.2 Computed Axial Velocity Sensitivity for Unheated Test Conditions .......... 67
  4.4.3 Computed Temperature and Velocity Sensitivity Analysis for Heated Tests .... 69

CHAPTER 5 : OMEGA SSG SINGLE-PHASE HEAT TRANSFER ANALYSIS ............. 71

5.1 Local Single-Phase Heat Transfer Analysis ............................................... 76
5.2 Circumferentially-Averaged Heat Transfer Analysis ................................... 84

CHAPTER 6 : SSG ONB ANALYSIS ......................................................................... 91

6.1 Experimental ONB Location and Wall Superheat Determination ................. 91
6.2 Assessment of VIPRE-Computed Wall Superheat at ONB ......................... 93

CHAPTER 7 : SUMMARY AND CONCLUSIONS .................................................... 100

REFERENCES ......................................................................................................... 104

APPENDIX A : Turbulent Viscosity Optimization ............................................... 106
APPENDIX B : Velocity and Temperature Sensitivity Analysis ......................... 110
APPENDIX C : VIPRE-I Computed Velocity for Manivel Phase 3 ....................... 117
APPENDIX D : Local Single-Phase Heat Transfer Results .................................. 121
APPENDIX E : ONB Test Experimental Axial Temperature Profiles ................. 128
APPENDIX F : THYC and VIPRE-I Consistency Analysis ................................... 134
LIST OF FIGURES

Figure 1.1: General Schematic of NESTOR Experimental Flow Loop Configurations........3
Figure 1.2: SSG and MVG Type Grids used in NESTOR Test Sections .........................4
Figure 1.3: Pressure Transducer Locations in SSG and MVG Configurations ...............5
Figure 1.4: LDV Instrumentation Setup ..................................................................6
Figure 1.5: OMEGA Rod Numbering Sequence (left) and Symmetric Sub-Channel Type Numbering Sequence (right) .........................................................7
Figure 2.1: LDV Velocity Measurement Locations in MANIVEL Cross-Section ..........14
Figure 2.2: LDV Correction Factors for Grid Span 2a and 2b ..................................16
Figure 2.3: Averaged LDV Correction Factors ......................................................17
Figure 2.4: Circumferential Temperature Distribution for All Rods taken during Boiling Test 4 .................................................................22
Figure 2.5: Corrected Temperature Measurements for all Rods during Boiling Test 4 ..24
Figure 2.6: OMEGA Test Section Exit Region ......................................................26
Figure 2.7: Elbow with Recess .............................................................................30
Figure 3.1: Corner (top left), Side (bottom left), and Inner (right) NESTOR Sub-channel Types ..........................................................34
Figure 3.2: MANIVEL SSG Nodal Mesh ..............................................................36
Figure 3.3: OMEGA SSG Mesh 1 ........................................................................37
Figure 3.4: OMEGA SSG Mesh 2 (Run 4.1) .........................................................38
Figure 3.5: OMEGA SSG Mesh 3 (Run 4.2) .........................................................39
Figure 3.6: Mesh for OMEGA SSG ONB Tests ....................................................40
Figure 4.1: $C_{T\beta}$ Optimization for Arithmetic-Averaged Data and Correlation $f-K$ Set 3 ....57
Figure 4.2: $C_f\beta$ Optimization for Arithmetic-Averaged Data and Correlation $f$-$K$ Set 1

Figure 4.3: $\beta$ Optimization for $f$-$K$ Set 1

Figure 4.4: $\beta$ Optimization for $f$-$K$ Set 2

Figure 4.5: $\beta$ Optimization for $f$-$K$ Set 3

Figure 4.6: Minimum $D_c$ Value for all Single-Phase Tests

Figure 4.7: Minimum $D_c$ Value for all Single-Phase Tests plotted with respect to Bundle Re

Figure 4.8: Comparison of Measured and Predicted Pressure Drops for MANIVEL SSG Phase 6 Results

Figure 5.1: OMEGA SSG Test Chronology

Figure 5.2: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 2b

Figure 5.3: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 2a

Figure 5.4: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 1b

Figure 5.5: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 1a

Figure 5.6: Rod 5 Axial HTC Evolution for Test 4.2

Figure 5.7: Rod 7 Axial HTC Evolution for Test 4.2

Figure 5.8: Sub-Channel Type 6 HTC Ratio predicted using Adjacent Rods 1, 2 and 5

Figure 5.9: Test 4.2 Local HTC Ratio Axial Evolution in Grid Spans 1a and 1b for Rod 5

Figure 5.10: Test 4.2 Local HTC Ratio Axial Evolution for Grid Spans 1a and 2b

Figure 5.11: Rod 2 Circumferential HTC Ratio Evolution for Test 4.2

Figure 5.12: Rod 5 Circumferential HTC Ratio Evolution for Test 4.2

Figure 5.13: Local HTC Ratio with respect to Code-Calculated Local Re for All Single-Phase Tests
Figure 5.14: Local HTC Ratio Segregated by Axial Measurement Location with respect to Local Reynolds number for all Single-Phase Tests .........................................................83

Figure 5.15: Axial Evolution of Averaged HTC Ratios for All Single-Phase Tests ..........84

Figure 5.16: Averaged HTC Ratio Evolution Downstream and Upstream of the SSG for All Single-Phase Tests .................................................................................................85

Figure 5.17: Grid Enhancement Data from Yao (Yao, 1982)........................................86

Figure 5.18: Axial Evolution of Averaged HTC Ratio for All Single-Phase Test Data with Exponential Grid-Enhancement Function Applied ................................................87

Figure 5.19: Axial Evolution of HTC Ratio in Span 1a with Polynomial Curve Fit ..........88

Figure 5.20: Axial Evolution of HTC Ratio in Span 1a with Polynomial Grid-Enhancement Function Applied ........................................................................................................88

Figure 5.21: Difference between Predicted and Measured Rod Surface Temperatures for All Single-Phase Tests without Grid Heat Transfer Enhancement Correction Factor ....89

Figure 5.22: Difference between Predicted and Measured Rod Surface Temperatures for All Single-Phase Tests with Grid Heat Transfer Enhancement Correction Factor ..........90

Figure 6.1: Experimental and Computed Wall Superheats at Experimental ONB ..........94

Figure 6.2: HTC Ratio for ONB Tests with and without Polynomial Grid Enhancement Function .................................................................................................................................95

Figure 6.3: HTC Ratio for ONB Tests with and without Exponential Grid Enhancement Function .................................................................................................................................95

Figure 6.4: Difference between Experimental and Predicted Rod Surface Temperature with EDF-Produced Grid Enhancement Function Applied for Single-Phase and ONB Test Runs .............................................................................................................................96

Figure 6.5: Predicted and Experimental Wall Superheat at Experimental ONB Location as Calculated by EDF, CEA and PSU ..............................................................................97

Figure 6.6: Computed and Experimental Wall Superheat using VIPRE-I and THYC and PSU and EDF Single-Phase Heat Transfer Models for Test 1.6-1 ....................................98

Figure 6.7: Computed and Experimental Wall Superheat using VIPRE-I and THYC and PSU and EDF Single-Phase Heat Transfer Models for Test 2.5-2 ....................................99
LIST OF TABLES

Table 2.1: Correction Factors Based on Mass Balance.......................... 15
Table 2.2: Averaged LDV Correction Factors.................................. 16
Table 2.3: Parameters for Wall Thickness Variation Correlation in SSG Test Setup ........... 23
Table 2.4: OMEGA SSG ONB Test Exit Length Pressure Drops and Operating Condition Properties ................................................................. 32
Table 3.1: MANIVEL SSG Phase 6 Runs ........................................ 43
Table 3.2: MANIVEL SSG Phase 3 Run......................................... 43
Table 3.3: OMEGA SSG Single-Phase Runs .................................... 43
Table 3.4: OMEGA SSG ONB Runs................................................ 44
Table 4.1: Reynolds Numbers in Different Sub-channels and Bundle Locations for MANIVEL SSG Phase 6 Tests ................................................................. 54
Table 4.2: SSG Individual Sub-Channel Geometries in Bare-Bundle and In-Grid Sections... 54
Table 4.3: Summary of f-K sets Developed for the SSG Bundle .................. 55
Table 4.4: MANIVEL LDV Measurement Locations Relative to Upstream Grid ........ 55
Table 4.5: Optimum $C_f \beta$ Values Found for all Correlation Sets and Averaging Techniques .......................................................... 58
Table 4.6: Correlation Sets for OMEGA Mixing Parameters Optimization ............ 60
Table 4.7: Optimum $\beta$ Value for OMEGA Single Phase Tests .................. 64
Table 4.8: Optimized f-K Sets used in Sensitivity Analysis .......................... 65
Table 4.9: Measured and Predicted Pressure Drop Statistics for MANIVEL SSG Phase 6 Data ............................................................................. 66
Table 4.10: VIPRE-I Input Variables for Axial Velocity Profile Sensitivity Analysis ......... 67
Table 4.11: Maximum Sensitivities to Optimized f-k Set and $\pm 1\sigma$ Variation of $C_f \beta$ (Range Shown in Parentheses) .................................................. 68
Table 4.12: Maximum Deviations between Predicted and Measured Velocities using Optimized $f$-$k$ Correlation Sets.................................................................69

Table 4.13: Correlation Sets used for VIPRE-I Input in Temperature and Velocity Sensitivity Analysis for Heated Tests .............................................................................69

Table 4.14: Maximum Velocity and Temperature Sensitivities to Optimized $f$-$k$ Correlations and ±2 $\sigma$ Variation of $\beta$ (Range Provided in Parentheses) .................70

Table 6.1: Summary of Experimental ONB Locations and Corresponding Wall Superheat for SSG ONB Tests ........................................................................................................93

Table 6.2: Comparison of Experimental and Computed ONB Wall Superheat with and without Grid Enhancement Corrections..........................................................94
ACKNOWLEDGEMENTS

Penn State funding for work on this project was provided by the Electric Power Research Institute. Special thanks are in line for Dr. Suresh Yagnik of EPRI for his continuous, generous support over the 3 year duration of this project, and also for Professors Maria Avramova and Kostadin Ivanov of the Pennsylvania State University for joining this project and offering their much appreciated assistance and guidance when it was needed. The author would also like to express his gratitude to Mr. Pierre Péturaud of EDF for his help during concurrent analysis – his insightful leadership during the project provided an education in itself, comparable to any course here at Penn State. Last, but certainly not least, the author would like to express his sincere appreciation to the late Professor Lawrence Hochreiter, who secured this project and provided guidance before his passing. Dr. Hochreiter was an intelligent, supportive and shrewd advisor with a passion for passing on his lifetime of accumulated knowledge to the next generation of nuclear engineers. Certainly, he will not be forgotten.
Chapter 1: Introduction

1.1 NESTOR Program Background

NESTOR, which is an acronym for New Experimental Studies of Thermal-hydraulics of Rod bundles, consisted of a series of tests performed by the Electric Power Research Institute (EPRI), Electricité de France (EDF), and Commissariat à l’Energie Atomique (CEA). The overall goal of the project was to support the development of an Axial Offset Anomaly (AOA) model (EPRI, Palo Alto, CA, EDF, France, and CEA, France, 2005). The AOA phenomenon causes the axial power distribution in a reactor core to skew most likely due to the buildup of crud in certain core sections. Specifically, the absorption of boron compounds into the crud diminishes the local fission rate and changes the power distribution, which can be a limiting factor in the operation of PWRs (EPRI, Palo Alto, CA, EDF, France, and CEA, France, 2005). It is known that the crud deposition rate is dependent on temperature and especially the existence of localized sub-cooled boiling. Therefore, the improvement of AOA modeling was to be achieved through accurate modeling of ONB using the high-fidelity NESTOR experimental data.

The need for such high-fidelity experiments was determined, in part, using a comprehensive literary search performed by Harrison and Hughes (Harrison, 2000). The literary search pointed out that the widely-used Dittus-Boelter single-phase heat transfer correlation was in excellent agreement with more recent correlations (Petukhoy, 1974), (Gnielinski, 1976), (Kays, 1993); however, the correlation does not perform well when compared to in-bundle heat transfer data (Dingee, 1957), (Miller, 1956), (Marek, 1973) or analytical developments (Mohanty, 1988). Additionally, there is little validation data for the grid-induced heat transfer enhancement correlation developed by Yao, et al (Yao, 1982). The NESTOR program was developed in order to address these concerns.
The NESTOR Project was comprised of experiments run at prototypical PWR thermal-hydraulic conditions in 5x5 rod bundles which provided thermal hydraulic behavior including detailed sub-channel local velocity measurements and heater rod surface temperature maps. The data was collected on two separate, but geometrically identical flow loops. Pressure drop and axial velocity measurement data was collected on the unheated EDF-Chatou MANIVEL flow loop and End of Heated Length (EOHL) sub-channel temperature measurements and rod surface temperature measurements were obtained on the heated CEA-Grenoble OMEGA test loop. This thermal hydraulic behavior was to be used to develop an accurate single-phase heat transfer model for PWR operating conditions with the help of thermal hydraulic core codes as well as to assess the Dittus-Boelter single-phase heat transfer correlation. The codes were necessary to provide bulk sub-channel average temperatures for calculation of the experimental Heat Transfer Coefficient (HTC). Three different core codes were used by three NESTOR partners during the analysis, which included THYC-COEUR (Mur, 2000) used by EDF, FLICA-IV (Aniel, 2005) used by CEA, and VIPRE-I (Srikantiah, 1992) used by Penn State University (PSU). Before the codes could be used to aid in experimental HTC calculation, it was necessary to calibrate specific code input parameters to the NESTOR flow loop geometry and test conditions. Resulting heat transfer models and sub-channel code capabilities were then to be further assessed through analysis of ONB experiments.

1.2 NESTOR Experiments Overview

The NESTOR experiments consisted, in total, of four experimental flow loops named MANIVEL SSG, MANIVEL MVG, OMEGA SSG and OMEGA MVG. The MANIVEL flow loops were unheated tests used to gather pressure drop and axial velocity measurements. The
OMEGA flow loops were heated and were used to gather EOHL sub-channel average temperature and rod surface temperature measurements. In each flow loop, SSG and alternating SSG/MVG grid-types were utilized. A schematic of the MVG flow loop is presented in Figure 1.1.

![Figure 1.1: General Schematic of NESTOR Experimental Flow Loop Configurations](image)

The SSG flow loop had SSGs in place of the MVGs denoted in the figure. The SSG grids were designed to have a minimum impact on the flow, whereas the MVG grids were comparable to industrial spacer grids. It can be noted from Figure 1.1 that the MVG grid spans were approximately half of the industrial span lengths due to the inclusion of the SSGs. It was actually desirable to perform experiments without the use of the intermediate SSGs; however,
they were necessary due to Laplace forces caused by the electrical currents in the heater rods. Absent of the SSGs, it was determined that rod bow would have been significant (EPRI, Palo Alto, CA, EDF, France, and CEA, France, 2005). The bundle configurations containing only SSGs were run to investigate single-phase heat transfer and ONB in a configuration resembling, as closely as possible, a bare-bundle configuration (EPRI, Palo Alto, CA, EDF, France, and CEA, France, 2006). Images of the two grid types are shown in Figure 1.2.

![SSG and MVG Type Grids used in NESTOR Test Sections](image)

Figure 1.2: SSG and MVG Type Grids used in NESTOR Test Sections

As seen in Figure 1.1, the total bundle axial length was about 3.6 m. In the MANIVEL experiments, pressure measurements were taken by six pressure transducers placed across four grid spans as shown in Figure 1.3.
It can be seen that transducer 5 and 6 measurements were redundant – measurements were actually taken on an adjacent test section wall with these transducers. The bottom of the first pressure transducer was located about 0.8 m from the Beginning of Heated Length (BOHL).

Axial velocity measurements were taken in grid spans 1a and 1b for the SSG tests and grid spans 1a, 1b, 2a and 2b for the MVG tests. Measurements were made using two Laser Doppler Velocimetry (LDV) devices mounted to a movable platform. The units were able to move in x-, y- and z- directions, allowing for full 3-D sweeps of the MANIVEL test sections. A schematic of the velocity measurement device is shown in Figure 1.4.
Figure 1.4: LDV Instrumentation Setup

Rod surface temperature measurements were obtained for the inner 9 heater tubes using a sliding thermocouple mechanism in the OMEGA test loops. The mechanism was a combination of 9 probes with four thermocouples on each probe which was capable of moving axially into the bundle 1.2 m and rotating circumferentially 360°. The probes were capable of moving in unison and collecting temperature measurements on the interior surface of the rods. Because the probes operated inside the rods, it meant the flow was not disturbed by the temperature collection process. In addition to the rod surface temperature measurements during testing, sub-channel center outlet temperatures were obtained by thermocouples positioned at the EOHL in each sub-channel. The labeling convention for both the OMEGA heater rods and the sub-channel types is shown in Figure 1.5. Note that the sub-channel numbering sequence shown in Figure 1.5 defines sub-channel types based on 1/8th bundle symmetry, which was used for the SSG tests. For MVG tests, symmetry wasn’t used due to the directional cross-flow cause by the mixing vanes.
Additional test-specific data is provided in the discussions of associated tests in following chapters.

1.3 Concurrent Analysis and Data Analysis Overview

The NESTOR project was a collaborative effort between Commissariat à l’Energie Atomique (CEA, France), Electricité de France (EDF, France) and Electric Power Research Institute (EPRI, USA). OMEGA tests were performed by CEA at the Grenoble facility and MANIVEL tests were performed by EDF at the Chatou facility. The three separate partners were to perform independent analyses on the test data using three separate sub-channel analysis codes; CEA was to use FLICA-IV, EDF was to use THYC-COEUR, and EPRI was to use VIPRE-I. PSU was sub-contracted on behalf of EPRI to perform analysis with VIPRE-I. Throughout the data analysis, the three partners kept regular contact and exchanged results as the different stages of analysis were concurrently performed.

Sub-channel codes were required during the analysis for predicting the bulk sub-channel temperatures, which would, in-turn, be used for calculating the experimental HTCs.
Additionally, code capabilities at predicting ONB would be assessed. Prior to using the codes for test modeling, it was necessary to calibrate the code models to the geometry and operating conditions of the NESTOR tests. Specifically, it was necessary to determine the bundle frictional losses and grid loss coefficients. It was also necessary to determine the input parameters for the turbulent mixing model. The primary purpose of the MANIVEL tests was to provide necessary information for these code calibrations. Pressure drop test results were to be used for calculating the friction and grid-loss coefficients and velocity measurements were to be used for the partial calibration of the mixing model. Single-phase test sub-channel temperature measurements at EOHL of the OMEGA test facility were to be used for completing the mixing model optimization.

Using the optimized input parameters, the partners would then model the single-phase heat transfer tests with the aim of developing a single-phase heat transfer model which is dedicated to the OMEGA test section configuration. The final task of the data analysis involved determining experimental ONB locations in the ONB test results and then comparing experimental wall superheat at ONB to code-predicted wall superheat using the previously-developed single-phase heat transfer model.

### 1.4 VIPRE-I Code Logic and Models

#### 1.4.1 Code Background

VIPRE-I was the sub-channel analysis code used by PSU for NESTOR data analysis and ONB assessment. It was developed by Battelle Pacific Northwest Labs for EPRI. The version of the code used during this project was VIPRE-I MOD 2.3. VIPRE-I determines the three-
dimensional pressure, velocity, and temperature fields in heated or un-heated channels by solving the finite-difference equations for mass, momentum, and energy (Battelle Pacific Northwest Laboratories). The code is primarily used for modeling reactor cores and test bundles, but can be used to model other flow geometries like pipes due to its flexibility. The user inputs the flow area, heated perimeter and wetted perimeter of sub-channels and then specifies how the sub-channels are connected. The user then inputs rod geometry, number of rods, rod power and power distribution, and rod material makeup. The system can be fully modeled after adding in the operating conditions of the geometry and what models will be used for analysis.

VIPRE-I was built off of the strengths of COBRA-IIIC. Sub-channels communicate through turbulent mixing and diversion cross-flow and the flow field is assumed homogeneous and incompressible; although, models were added for sub-cooled boiling and co-current liquid/vapor slip. Instead of marching up the test section, solving for fluid properties based only on upstream data, VIPRE-I constantly sweeps the computational mesh and uses an implicit boundary value solution method. An improvement over COBRA allows VIPRE-I to internally generate fluid properties instead of only accepting user-inputted values (Battelle Pacific Northwest Laboratories).

1.4.2 Code Basis for Thermal Hydraulic Analysis

Like all sub-channel analysis codes, the mass, energy, and momentum balance equations are paramount to modeling fluid systems. The mass balance equation used by VIPRE-I is shown by Equation (1.1).
The three terms in this equation represent the time rate of change of mass per unit axial length, the axial variation in mass flow rate, and the sum of lateral mass flow rate in all the gaps of the control volume, respectively. The summation function in the third term is responsible for summing all interfaces of the control volume in which lateral mass flow exists with \( S \) representing the gap width. Energy is accounted for in the control volume by use of the energy balance equation shown by Equation (1.2).

\[
A \frac{\partial}{\partial t} \langle \rho \rangle + \frac{\partial}{\partial x} \langle \rho U \rangle A + \sum_{k \in l} e_{ik} \langle \rho V \rangle S = 0
\]  

(1.1)

Energy will enter a sub-channel through surface heat transfer (rods and walls), internal heat generation (neutrons slowing down), turbulent mixing between sub-channels, and by advection into the sub-channel. The first term on the right-hand-side (RHS) of the equation represents energy input from a surface like a fuel rod. \( P_w \) is the wetted perimeter of the surface and \( \Phi_{in} \) is the fraction of the surface that faces the control volume of interest. The heat flux, \( q'' \), will be defined in terms of an empirical heat transfer coefficient if the conduction model is used for modeling the rods of the assembly. If dummy rods are used the heat flux is only a function of the linear heat rate.

\[
A \frac{\partial}{\partial t} \langle \rho h \rangle + \frac{\partial}{\partial x} \langle \rho U h \rangle A + \sum_{k \in l} e_{ik} \langle \rho V h \rangle S
\]

\[
= \sum_{n \in l} P_w \Phi_{in} \langle q'' \rangle - \sum_{k \in l} w' \Delta h + \sum_{n \in l} C_q \Phi_{in} q'
\]  

(1.2)

This leaves the conservation of momentum, which is considered by an axial and a lateral momentum balance. The axial momentum balance is given by Equation (1.3).
The Left-Hand-Side (LHS) terms of Equation (1.3) represent the change of mass flux with time and the advection of momentum through all interfaces of the control volume. The rate of momentum transfer in and out of a control volume balances with forces acting on the volume resulting from pressure, wall friction and form losses, gravity, and turbulent mixing. These terms appear on the RHS of Equation (1.3). The first term represents the axially varying pressure, the second represents losses caused by wall friction and spacer grids, and the third term represents gravitational losses with \( \theta \) being the angle of inclination of the test section. The fourth term represents the axial force on the control volume caused by turbulent mixing and it is accounted for in the same manner as it was in the energy balance. Instead of the enthalpy difference, the turbulent mixing per unit length is multiplied by the axial velocity difference between the two sub-channels of interest. There is an additional factor, \( C_T \), which is known as the turbulent momentum factor. It is similar to the turbulent Prandtl number and it makes up for the imperfect analogy between turbulent transfer of momentum and energy. A value of 1.0 will represent equal strength in momentum and energy mixing while a value of 0.0 will represent only energy mixing. This value is chosen by the user and was determined by a sensitivity analysis for the NESTOR facilities.

The final balance is the lateral momentum equation which is shown in Equation (1.4).

\[
\frac{\partial}{\partial t} \langle \rho U \rangle A + \frac{\partial}{\partial x} \langle \rho U^2 \rangle A + \sum_{k \in l} e_{ik} \langle \rho UV \rangle S = -A \frac{\partial}{\partial x} \langle P \rangle - \frac{1}{2} \left( \frac{f}{D_h} + K' \right) \langle \rho U^2 \rangle A - \Lambda \langle \rho \rangle g \cos(\theta) - C_T \sum_{k \in l} w' \Delta U
\]  

\[
(1.3)
\]

\[
\frac{\partial}{\partial t} \langle \rho V \rangle S + \frac{\partial}{\partial x} \langle \rho V U \rangle S = \frac{S}{l} \left( \langle P \rangle_{iij} - \langle P \rangle_{jj} \right) - \frac{1}{2} \frac{S}{l} K_6 \langle \rho V^2 \rangle
\]  

\[
(1.4)
\]
VIPRE-I assumes that the direction for lateral flow is determined by the gap orientation and that it loses its identity and vanishes once it leaves the gap and enters the interior of the sub-channel. Shear forces, turbulent momentum diffusion, and body forces are neglected by VIPRE-I. This leaves two driving forces for lateral momentum exchange: channel pressure differences and form losses through the gaps. The first term of the RHS of the equation represents the pressure difference between two sub-channels, \( i \) and \( j \). The second term is the resistance to flow between channels with \( K_G \) being a user-defined loss coefficient (usually 0.5 for rod bundles). The LHS terms represent the time rate of change of lateral momentum and the flux of lateral momentum through the interface carried by axial velocity. The lateral momentum transferred through the axial faces of the control volume is ignored.

These four balance equations are used by the sub-channel analysis code to model the behavior of a flow system. In order to solve these formulas, the code requires some user-input information such as bundle geometry, power, flow rate, etc. Other parameters - like amount of turbulent mixing, fluid drag and heat transfer into or out of the fluid - are required for closure of the conservation equations and are obtained by a number of models available in the code.

Detailed information on all of the models available in VIPRE-I is available in the Mathematical Models section of the VIPRE-I manual (Battelle Pacific Northwest Laboratories, 2007).
Chapter 2: NESTOR Test Data Pre-Processing

Prior to using the experimental test data for analysis and model development, it was necessary to perform several tasks. Both the MANIVEL average sub-channel velocity measurements and the OMEGA temperature measurements required corrections. Additionally, since OMEGA rod temperature measurements were made on the interior surface of the rods, it was necessary to calculated exterior surface temperatures using the 1-D conduction equation. It was discovered during four boiling tests performed in the OMEGA test section that the rod thickness was not a constant value around the entire azimuth and so it was necessary to correct for thickness variations before calculating the outer wall rod temperatures. Finally, because the ONB local pressure was required during the ONB analysis, it was necessary to calculate the pressure drop between the EOHL and the actual location of test section exit pressure tap. These necessary pre-processing tasks are further discussed in detail in the following sections.

2.1 Correction of MANIVEL Average Sub-Channel Velocities

During MANIVEL SSG Phase 3, LDV was used to take a detailed portrait of the local velocity profile throughout the bundle cross section at selected axial levels. A high level of detail was obtained by taking a total of 1908 measurements at each axial level. The velocity measurements were to be used for optimization of the turbulent viscosity parameter of the VIPRE-I mixing model. Since VIPRE-I only predicts bulk sub-channel velocities, it was necessary to average the local measurement data in order to obtain single velocities for each sub-channel at each measurement location. Three averaging processes were used – details of which can be found in the NESTOR B-Report (EPRI, Palo Alto, CA, EDF, France, and CEA, France,
However, because of the capabilities of the LDV measurement system, the average of the velocity measurements in the sub-channel was not true to the actual sub-channel average velocity. This can be demonstrated with the use of a graphic showing how LDV measurements were taken in the cross-section (Figure 2.1).

![Figure 2.1: LDV Velocity Measurement Locations in MANIVEL Cross-Section](image)

The local velocity measurements were incomplete, as they did not measure the velocity of locations which were closest to the heater rods except for in the gap locations. Due to rod friction, the fluid velocity was lower near the rods. Since the measurement data did not include these lower velocity measurements, the calculated sub-channel averaged velocity was always slightly higher than the actual average sub-channel velocity. Corrections were made for this shortcoming using a mass balance. The average measured sub-channel velocities of all 36 sub-channels were used to calculate the cross-section mass flow rate using the fluid density and cross-sectional area. Since mass was conserved in the bundle, the ratio between this calculated mass flow rate and the known experimental inlet mass flow rate was taken to quantify the over-prediction in average sub-channel velocity. These ratios were then multiplied by the measured average sub-channel velocities to obtain the true average sub-channel measured values.
The correction factors were calculated for each axial measurement location and for each averaging technique. The resulting calculated correction factors are summarized in Table 2.1 with a graphical representation shown in Figure 2.2. Since, in theory, the axial velocity profile should be the same in both spans (and because the sub-channel analysis code predict similar velocity profiles in different spans), similar measurements in Span 1a and 1b were averaged together and only the averaged values were used for correcting the LDV measurements. The averaged correction factors are shown in Table 2.2. A graphical representation of the averaged correction factors is shown in Figure 2.3.

Table 2.1: Correction Factors Based on Mass Balance

<table>
<thead>
<tr>
<th>Z (mm)</th>
<th>Q_{actual}</th>
<th>Q (m³/hr)</th>
<th>Q_{calc}/Q_{act}</th>
<th>Q (m³/hr)</th>
<th>Q_{calc}/Q_{act}</th>
<th>Q (m³/hr)</th>
<th>Q_{calc}/Q_{act}</th>
</tr>
</thead>
<tbody>
<tr>
<td>35</td>
<td>60.6800</td>
<td>65.5339</td>
<td>1.0800</td>
<td>63.2351</td>
<td>1.0421</td>
<td>64.4491</td>
<td>1.0621</td>
</tr>
<tr>
<td>50</td>
<td>60.6800</td>
<td>65.9184</td>
<td>1.0863</td>
<td>63.6290</td>
<td>1.0486</td>
<td>64.7508</td>
<td>1.0671</td>
</tr>
<tr>
<td>100</td>
<td>60.6800</td>
<td>66.4629</td>
<td>1.0953</td>
<td>64.1949</td>
<td>1.0579</td>
<td>65.0944</td>
<td>1.0727</td>
</tr>
<tr>
<td>160</td>
<td>60.6800</td>
<td>66.0492</td>
<td>1.0885</td>
<td>64.0753</td>
<td>1.0560</td>
<td>64.7975</td>
<td>1.0679</td>
</tr>
<tr>
<td>235</td>
<td>60.6800</td>
<td>66.2741</td>
<td>1.0922</td>
<td>64.5405</td>
<td>1.0636</td>
<td>65.2122</td>
<td>1.0747</td>
</tr>
<tr>
<td>304</td>
<td>60.6800</td>
<td>65.2090</td>
<td>1.0746</td>
<td>62.2648</td>
<td>1.0261</td>
<td>63.8678</td>
<td>1.0525</td>
</tr>
<tr>
<td>329</td>
<td>60.6800</td>
<td>65.8093</td>
<td>1.0845</td>
<td>63.1838</td>
<td>1.0413</td>
<td>64.4743</td>
<td>1.0625</td>
</tr>
<tr>
<td>379</td>
<td>60.6800</td>
<td>66.0714</td>
<td>1.0889</td>
<td>63.5810</td>
<td>1.0478</td>
<td>64.5988</td>
<td>1.0646</td>
</tr>
<tr>
<td>439</td>
<td>60.6800</td>
<td>66.2875</td>
<td>1.0924</td>
<td>64.1946</td>
<td>1.0579</td>
<td>64.9869</td>
<td>1.0710</td>
</tr>
<tr>
<td>514</td>
<td>60.6800</td>
<td>66.1578</td>
<td>1.0903</td>
<td>64.3520</td>
<td>1.0605</td>
<td>65.0299</td>
<td>1.0717</td>
</tr>
</tbody>
</table>
Figure 2.2: LDV Correction Factors for Grid Span 2a and 2b

Table 2.2: Averaged LDV Correction Factors

<table>
<thead>
<tr>
<th>Z (mm)</th>
<th>Triangular</th>
<th>Arithmetic</th>
<th>Voronoï</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>1.0746</td>
<td>1.0261</td>
<td>1.0525</td>
</tr>
<tr>
<td>35</td>
<td>1.0800</td>
<td>1.0421</td>
<td>1.0621</td>
</tr>
<tr>
<td>50</td>
<td>1.0854</td>
<td>1.0449</td>
<td>1.0648</td>
</tr>
<tr>
<td>100</td>
<td>1.0921</td>
<td>1.0529</td>
<td>1.0687</td>
</tr>
<tr>
<td>160</td>
<td>1.0904</td>
<td>1.0569</td>
<td>1.0694</td>
</tr>
<tr>
<td>235</td>
<td>1.0912</td>
<td>1.0621</td>
<td>1.0732</td>
</tr>
</tbody>
</table>
Detailed rod surface temperature measurements as well as outlet sub-channel temperature measurements were taken during testing. The high level of detail was obtained by taking a large number of measurements, which caused testing times to be on the order of hours. During the testing time, there were small variations in flow conditions, which were recorded along with the inner tube temperature measurements. Because the tests were to be modeled with the sub-channel analysis codes using nominal operating conditions, inner tube temperature measurements needed to be corrected when flow conditions strayed from nominal flow conditions.

The correction process, developed by EDF and CEA (EPRI, Palo Alto, Ca, EDF, France, and CEA, France, 2008), involved using linear approximations of the equations describing thermal hydraulic behavior of the bundle. These equations include temperature drop from bundle entrance to measurement location (Equation (2.1)), temperature drop from bulk fluid to tube
outside (Equation (2.2)), and temperature drop across the tube thickness (Equation (2.3)). This set of equations covered the propagation of the impact of flow variations to the temperature measurements.

\[ T_F(z) = T_{in} + k_1 \left( \frac{W}{G} \right) (z - z_0) \]  

(2.1)

where, \( T_F \)=fluid temperature, \( W \)=bundle power, and \( G \)=mass flux

\[ h = k_3 Re^{0.8} \rightarrow h = k_3 G^{0.8} \]

\[ q'' = k_2 W \]  

(2.2)

\[ T_{wo}(Z, \theta) = T_F(z) + k_4 \left( \frac{q''}{h} \right) \]

where, \( h \)=convective HTC, \( q'' \)=heat flux, and \( T_{wo} \)=tube outside temperature

\[ T_{wi}(Z, \theta) = T_{wo}(Z, \theta) + k_5 W \]  

(2.3)

where, \( T_{wi} \)=tube inside temperature

In the above equation, \( k_4 \) through \( k_5 \) are proportionality coefficients. Combining these equations together yields,

\[ T_F(z) = T_{in} + k_1 \left( \frac{W}{G} \right) (z - z_0) \]

\[ T_{wo}(Z, \theta) = T_F(z) + \frac{k_4(k_2 W)}{k_3 G^{0.8}} \]  

(2.4)

\[ T_{wi}(Z, \theta) = T_{wo}(Z, \theta) + k_5 W \]
Since it is the variations in operating conditions and their effects on temperature measurements that were of interest, the logarithmic derivative of the three equations was taken. Additionally, the proportionality coefficients were replaced with appropriate linear approximations.

\[ dT_F(z) = dT_{in} + \Delta T_1(z) \left( \frac{dW}{W} - \frac{dG}{G} \right) \]

\[ dT_{wo}(z, \theta) = dT_F(z) + \Delta T_2 \left( \frac{dW}{W} - 0.8dG \right) \]

\[ dT_{wl}(z, \theta) = dT_{wo}(z, \theta) + \Delta T_3 \left( \frac{dW}{W} \right) \]

(2.5)

where, \( \Delta T_1(z) = (T_{out,ref} - T_{in,ref}) \times \frac{z-z_0}{l_h} \)

\[ \Delta T_2 = T_{(wo,ave)} - T_{(F,ave)} = \frac{q''_{ave}}{h_{DB,ave}} = \frac{q''_{ave}}{0.023 \times Re_{ave}^{0.8} \times Pr_{ave}^{0.4} \times \frac{k_{ave}}{D_h}} \]

\[ \Delta T_3 = T_{wl,ave} - T_{wo,ave} \]

\( \Delta T_1 \) represents the temperature increase from inlet to the elevation of interest, \( z \). It is a function of \( z \) and was calculated for each axial measurement location. \( \Delta T_2 \) represents the temperature rise from bulk fluid to the tube outside using the Dittus-Boelter equation to estimate the heat transfer coefficient. Since the temperature corrections were very low, there was no need to have a high level of accuracy when calculating the correction. For this reason, only one value of \( \Delta T_2 \) was calculated per test at the middle of the instrumented area (\( z = -0.58 \) m). Likewise, one value of \( \Delta T_3 \) was calculated per test using flow conditions at the middle of the instrumented area. \( \Delta T_3 \) represents the temperature drop across the tube wall. Since accuracy was not an issue here, the nominal tube thickness of 0.9 mm was used. Combining the equations shown in Equation (2.5) leads to the equations used for the temperature correction process.
The terms $\Delta W$ and $\Delta G$ represent the variation in power and mass flux at the temperature measurement time ($\Delta T = \Delta T_{in} + \Delta T_1 \left( \frac{\Delta W}{W} - \frac{\Delta G}{G} \right)$).

$$\Delta T_{wi} = \Delta T_{in} + \frac{\Delta W}{W} (\Delta T_1 + \Delta T_2 + \Delta T_3) - \frac{\Delta G}{G} (\Delta T_1 + 0.8 \times \Delta T_2)$$

(2.6)

The terms $\Delta W$ and $\Delta G$ represent the variation in power and mass flux at the temperature measurement time ($\Delta W = \frac{W_{ref} - W(t)}{w_{ref}}$ and $\Delta G = \frac{G_{ref} - G(t)}{g_{ref}}$). $\Delta T_{in}$ is the variation in inlet temperature during the temperature measurement and $\Delta T_{wi}$ is the correction that is applied to the temperature measurement. This correction method was applied to all OMEGA temperature measurements before performing any analysis heat transfer or ONB analysis.

2.3 Rod Thickness Correction

In addition to single-phase and ONB tests, boiling tests were also run in the OMEGA test section. The Boiling tests were added to the testing phase in order to determine the conditions suitable for the ONB tests, check the instrumentation capabilities and evaluate data consistency (EPRI, Palo Alto, Ca, EDF, France, and CEA, France, 2008). During the boiling tests, it was found that there was a sinusoidal variation in measured rod temperature around the rod circumference. Figure 2.4, taken from the C-1 NESTOR Report (EPRI, Palo Alto, Ca, EDF, France, and CEA, France, 2008), demonstrates the angular dependence of rod temperature during the constant temperature process of boiling for the SSG tests. Since such a trend was indicative of an eccentricity in the tube thickness, ultrasonic measurements were performed. The ultrasonic measurements verified that there was indeed an eccentricity present in the tubes of the OMEGA test section that would need to be corrected during future analysis. It was necessary to have an
accurate measurement of local rod thickness because it would be used in the 1-D conduction equation to calculate the outer rod surface temperature from the inner surface measurements.
A correlation was developed for each rod that provided the tube thickness as a function of angular location. The correlations were of the form of Equation (2.7).
To determine the tube-specific parameters, \( a \) and \( \theta_0 \), the 1-D conduction equation was used along with the temperature drop results from the boiling tests. This was done by assuming that the tube-outside temperature during the boiling tests was constant around the tube circumference. It was additionally assumed that the outside radius was constant (thickness variation resulted from inner radius variation), that the mean wall thickness was equal to the design thickness (0.9 mm), and that the mean measured rod temperature drop corresponded to the mean tube thickness. Corresponding to these assumptions, any difference between the local measured inside temperature and the mean inside temperature would be caused by tube thickness variation. Using this process, a set of parameters was developed for each rod, which is shown in Table 2.3. Rod 6 measurements became unavailable during SSG testing since the rod had bowed. Results are shown here for the SSG rods – MVG rod eccentricity varied from SSG eccentricity.

Table 2.3: Parameters for Wall Thickness Variation Correlation in SSG Test Setup

<table>
<thead>
<tr>
<th>Rod</th>
<th>( a ) (( \mu )m)</th>
<th>( \theta_0 ) (°)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>11.8</td>
<td>19</td>
</tr>
<tr>
<td>2</td>
<td>7.8</td>
<td>119</td>
</tr>
<tr>
<td>3</td>
<td>18.4</td>
<td>81</td>
</tr>
<tr>
<td>4</td>
<td>2.7</td>
<td>137</td>
</tr>
<tr>
<td>5</td>
<td>12.0</td>
<td>150</td>
</tr>
<tr>
<td>7</td>
<td>-0.5</td>
<td>-57</td>
</tr>
<tr>
<td>8</td>
<td>21.2</td>
<td>32</td>
</tr>
<tr>
<td>9</td>
<td>5.8</td>
<td>20</td>
</tr>
</tbody>
</table>

Application of the wall thickness correction to boiling test measurements improved the predicted outside temperature accuracy – data dispersion was reduced to less than 0.3 K for 90% of the data. Figure 2.5 shows the circumferential temperature distribution for all rods after the wall thickness correction was applied to Boiling Test 4 data. See the NESTOR C-1 Report for more detailed information on the wall thickness correction process (EPRI, Palo Alto, Ca, EDF, France, and CEA, France, 2008).
2.4 Calculation of Heater Rod Outer Wall Surface Temperatures

The rod surface temperature measurements were made on the inner surface of the 9 inner heater rods via a sliding thermocouple mechanism. Since the outer-wall rod surface temperatures were needed for calculating the experimental HTCs, the 1-D conduction equation was used to obtain the outer-wall temperature from the inner-wall temperature measurements recorded during the OMEGA single-phase and ONB tests. The 1-D conduction equation is formulated in Equation (2.8) for the curved wall geometry of the heater rod.

\[
T_{w_o} = T_{w_l} - \frac{q'''}{2k} \left[ e^2 \left( \ln \left( 1 - \frac{e}{R_0} \right) - \frac{1}{2} \right) + eR_0 \left( 1 - 2 \ln \left( 1 - \frac{e}{R_0} \right) \right) \right] + R_0^2 \ln \left( 1 - \frac{e}{R_0} \right) 
\]

(2.8)

The corrected wall thickness was used in the equation, calculated as described in the previous section. Additionally, tube dimensions were corrected for thermal expansion using a
thermal expansion coefficient of 13E-6 m/m°K and the average bundle temperature calculated from the inlet and outlet temperatures. The volumetric heat generation was calculated using Equation (2.9). The design tube thickness (0.9E-3 m) was used in the calculation. It is important to note that thermal expansion was only accounted for when calculating the experimental heat transfer coefficient (e.g. accounted for when calculating experimental heat flux). VIPRE-I does not account for thermal expansion and the experimental bundle power was entered along with the cold bundle dimensions. Thus, experimental heat transfer coefficients included thermal expansion effects, while calculated results did not.

\[
q'''' = \frac{0.995 \times \frac{\text{Power}}{L_h} \times 1.164311}{\pi \times \left( R_0^2 - (R_0 - \epsilon_{design})^2 \right)}
\]  

(2.9)

The thermal conductivity of the rod was calculated using Equation (2.10).

\[
k = 12.191 + 0.01712T_{ave} + 1.22e - 6T_{ave}^2
\]  

(2.10)

Using the previously mentioned equations, the outside rod temperature was iteratively determined for each temperature measurement using a FORTRAN data analysis code. This process was performed for each of the single-phase and ONB OMEGA tests.

2.5 Calculation of Pressure Drop in OMEGA Flow Loop Outlet Section

In order to determine the experimental and calculated wall superheat at the ONB location, which was used during ONB analysis, it was necessary to know the saturation temperature during the tests. The saturation temperature is a function of the operating pressure. The test data files gave the pressure at the test section exit; however, the actual location of the pressure tap was not at the End of Heated Length (EOHL). In order to have an accurate saturation temperature (and
thus, ONB location estimation), it was necessary to know the pressure near the location where ONB occurred. In order to determine this value, the pressure drop from the EOHL to the pressure tap location needed to be estimated and added onto the operating pressure stated in the ONB data files.

The exit section of the OMEGA test section is shown in Figure 2.6. During tests, after the flow left the heated test section area, it opened up into a circular chamber, made a 90° bend, contracted into a pipe and traveled over 1 m to where the pressure tap was located.

![Figure 2.6: OMEGA Test Section Exit Region](image)

Calculations of the exit region pressure drop used the following two assumptions: 1) the test section materials were treated as smooth surfaces when considering frictional losses and 2) the chamber at the test section exit was treated as an elbow with recess, which could be found on pg. 366 of Idelchik (Idelchik, 2005).
The mechanical energy balance equation used for the pressure drop calculations is shown in Equation (2.11).

\[
\frac{P_2 - P_1}{\rho} + \frac{u_2^2 - u_1^2}{2} + (z_2 - z_1)g + \frac{f_m * L * u_2^2}{2D_e} + \frac{k_e u_2^2}{2} + w = 0
\]  \hspace{1em} (2.11)

The test section exit was considered in four separate sections: the square test section exit section, the large cylindrical chamber that houses the end of the bundle, the short 85 mm diameter pipe, and the longer 87 mm diameter pipe. There was a flow acceleration loss associated with the contraction from section 1 to section 4, a potential energy loss from EOHL to the height of sections 3 and 4, frictional losses in each section, contraction/expansion losses between each section, and a flow redirection loss in section 2. Section 2 was broken up into two subsections when considering friction: the frictional loss along the bundle was considered up to the location of the outlet pipe, and the frictional loss from the bundle to the outlet pipe was considered 90° from the direction of the bundle.

Applying the mechanical energy balance to the test section resulted in Equation (2.12).

\[
\Delta P = \rho \left[ \frac{u_4^2 - u_1^2}{2} + (z_2 - z_1)g + \frac{f_1 L_1 u_1^2}{2D_e} + \frac{f_2 a L_2 a u_2^2}{2D_e} + \frac{f_2 b L_2 b u_2^2}{2D_e} + \frac{f_3 L_3 u_3^2}{2D_e} \right.
\]

\[
+ \frac{f_4 L_4 u_4^2}{2D_e} + \frac{k_{e12} u_1^2}{2} + \frac{k_{e23} u_3^2}{2} + \frac{k_{e34} u_3^2}{2} + \frac{k_{bend} u_3^2}{2} \right] \hspace{1em} (2.12)
\]

To solve this equation, several variables needed to be solved. The flow velocities in each section were determined based on the test operating conditions. Equation (2.13) shows the calculation of velocity in section 1. Other test section velocities were calculated using the result of Equation (2.14).

\[
u_1 = \frac{G}{\rho} \hspace{1em} (2.13)\]
\[ u_{2a} = \frac{u_1 A_1}{A_{2a}}; \quad u_{2b} = \frac{u_1 A_1}{A_{2b}}; \quad u_3 = \frac{u_1 A_1}{u_3}; \quad u_4 = \frac{u_1 A_1}{A_4} \]  \hspace{1cm} (2.14)

\( G \) was the mass flux given for each test in the OMEGA data report. The density needed to be determined for each test based on the EOHL temperature. The section cross-sectional areas were determined next. The cross-sectional area of section 2b varied with height since the chamber was cylindrical – its area was estimated by using the chamber length (0.36 m) multiplied by half of the chamber diameter. Cross-sectional area calculations are shown by Equations (2.15) through (2.19).

\[ A_1 = (66.1E - 3m)^2 - 25 \times \frac{\pi}{4} \times (9.5E - 3m)^2 = 2.597E - 3m^2 \]  \hspace{1cm} (2.15)

\[ A_{2a} = \frac{\pi}{4} (0.173m)^2 - 25 \times \frac{\pi}{4} (9.5E - 3m)^2 = 21.73E - 3m^2 \]  \hspace{1cm} (2.16)

\[ A_{2b} = 0.36m \times 86.5E - 3m = 31.14E - 3m^2 \]  \hspace{1cm} (2.17)

\[ A_3 = \frac{\pi}{4} \times 0.085^2m^2 = 5.675E - 3m^2 \]  \hspace{1cm} (2.18)

\[ A_4 = \frac{\pi}{4} \times 0.087^2m^2 = 5.945E - 3m^2 \]  \hspace{1cm} (2.19)

For the potential energy loss, the distance between section 1 and 2 was 0.5 m. Since the flow was horizontal in the other sections, there was no additional potential energy loss. The lengths of all the sections, for friction loss, were:

\[ L_1 = 0.32 \text{ m}; \quad L_{2a} = 0.18 \text{ m}; \quad L_{2b} = 86.5 \times 10^{-3} \text{ m}; \quad L_3 = 0.3 \text{ m}; \quad L_4 = 0.77 \text{ m} \]  \hspace{1cm} (2.20)

The friction factors were calculated using the Coleburn equation – they were dependent on Reynolds number (i.e. test operating conditions). The formulations for the friction factors are shown in Equations (2.21) through (2.25).
As stated before, the density and dynamic viscosity was dependent on the test number and were obtained using the EOHL temperature. The hydraulic diameters were calculated for each section using Equation (2.26).

\[ D_e = \frac{4A}{P_w} \]  

(2.26)

The hydraulic diameters for the 5 different sections were:

\begin{align*}
D_{e1} &= 10.28 \times 10^{-3} \, m; \quad D_{e2a} = 67.09 \times 10^{-3} \, m; \quad D_{e2b} = 0.1395 \, m; \\
D_{e3} &= 0.08905 \, m; \quad D_{e4} = 0.08700 \, m
\end{align*}

(2.27)

The contraction and expansion form losses were obtained from Kays and London (W. M. Kays, 1998) assuming \( Re = \infty \). The form losses were:

\[ k_{e12} = 0.77; \quad k_{e23} = 0.35; \quad k_{e34} = 0.0 \]

(2.28)
The expansion loss from section 3 to 4 was zero because the area change was negligible.

The last loss to be accounted for was the turning loss in section 2. As stated in the assumptions, this loss was modeled as a sharp elbow bend with a recess (see Figure 2.7).

![Figure 2.7: Elbow with Recess](image)

This geometry was modeled as stated in Idelchik (Idelchik, 2005), repeated here as Equation (2.29).

\[
 k \approx 1.2k_{\text{without recess}} 
\]  

(2.29)

Parameters needed to solve this equation were given in Idelchick and are repeated here in Equation (2.30). For smooth surfaces,
The former calculations resulted in the following loss coefficient,

\[ k_{w.r.} = C_1 A k_{loc} \]

\[ C_1 = 1 \text{ for } 90^\circ \]

\[ A \approx 0.95 + \frac{33.5}{\theta} = 1.32 \quad (2.30) \]

\[ k_{loc} = 0.95 \sin^2 \left( \frac{\theta}{2} \right) + 2.05 \sin^4 \left( \frac{\theta}{2} \right) = 0.9875 \]

The former calculations resulted in the following loss coefficient,

\[ k_{bend} \approx 1.564 \quad (2.31) \]

The former equations were solved simultaneously using an iterative equation solving package. The formulas were entered into the program and a parametric table was used to vary the mass flux \( G \), density \( \rho \), and dynamic viscosity \( \mu \) for each test. Results for the operating conditions of the ONB tests are shown in Table 2.4. The exit section pressure drops were added to stated test operating pressures before performing ONB analysis.
Table 2.4: OMEGA SSG ONB Test Exit Length Pressure Drops and Operating Condition Properties

<table>
<thead>
<tr>
<th>Run</th>
<th>G (kg/m$^2$·s)</th>
<th>$\mu$ (Pa·s)</th>
<th>$\rho$ (kg/m$^3$)</th>
<th>$\Delta P$ (Pa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>3560</td>
<td>8.036e-5</td>
<td>678.6</td>
<td>18491</td>
</tr>
<tr>
<td>1.2</td>
<td>3540</td>
<td>7.89E-05</td>
<td>669.6</td>
<td>18351</td>
</tr>
<tr>
<td>1.3</td>
<td>3510</td>
<td>7.83E-05</td>
<td>666.2</td>
<td>18208</td>
</tr>
<tr>
<td>1.4</td>
<td>3510</td>
<td>7.79E-05</td>
<td>663.3</td>
<td>18190</td>
</tr>
<tr>
<td>1.5</td>
<td>3540</td>
<td>7.78E-05</td>
<td>662.4</td>
<td>18306</td>
</tr>
<tr>
<td>1.6-1</td>
<td>3520</td>
<td>7.67E-05</td>
<td>655.3</td>
<td>18180</td>
</tr>
<tr>
<td>1.6-2</td>
<td>3510</td>
<td>7.62E-05</td>
<td>651.9</td>
<td>18119</td>
</tr>
<tr>
<td>2.1</td>
<td>4520</td>
<td>7.87E-05</td>
<td>668.8</td>
<td>22797</td>
</tr>
<tr>
<td>2.2</td>
<td>4520</td>
<td>7.75E-05</td>
<td>660.4</td>
<td>22794</td>
</tr>
<tr>
<td>2.3</td>
<td>4520</td>
<td>7.66E-05</td>
<td>654.7</td>
<td>22793</td>
</tr>
<tr>
<td>2.4</td>
<td>4510</td>
<td>7.54E-05</td>
<td>646.1</td>
<td>22744</td>
</tr>
<tr>
<td>2.5-1</td>
<td>4500</td>
<td>7.42E-05</td>
<td>638.1</td>
<td>22697</td>
</tr>
<tr>
<td>2.5-2</td>
<td>4540</td>
<td>7.44E-05</td>
<td>639.1</td>
<td>22906</td>
</tr>
<tr>
<td>2.5-3</td>
<td>4550</td>
<td>7.43E-05</td>
<td>638.4</td>
<td>22959</td>
</tr>
<tr>
<td>3.1</td>
<td>3520</td>
<td>8.21E-05</td>
<td>689.9</td>
<td>18410</td>
</tr>
<tr>
<td>3.2</td>
<td>3500</td>
<td>8.11E-05</td>
<td>683.6</td>
<td>18286</td>
</tr>
<tr>
<td>3.3</td>
<td>3510</td>
<td>8.01E-05</td>
<td>677.6</td>
<td>18284</td>
</tr>
</tbody>
</table>
Chapter 3: Modeling of NESTOR Tests with VIPRE-I

The use of thermal hydraulic codes was necessary during data analysis to provide bulk sub-channel temperatures in the test section for calculation of the local experimental HTCs. With experimental HTC data, it would be possible for the NESTOR partners to develop single-phase heat transfer models and ONB models. Before this could be achieved, it was necessary to first generate appropriate input decks for modeling the experiments. Secondly, it would be required to calibrate specific user-input parameters to the specific test configurations – these parameters included surface friction factors, grid loss coefficients and turbulent mixing parameters. This chapter covers the VIPRE-I input deck generation process, including details on the test section geometry, experimental operating conditions, and model selections.

3.1 Geometry Modeling Considerations (GEOM Card)

Section 1.2 provided a basic overview of the geometry and dimensions of the NESTOR flow loops. There were 36 sub-channels altogether, with three sub-channel types: corner (1), side (2, 3), and inner (4, 5, 6), with numbers corresponding to the values given in Figure 1.5.

Figure 3.1 displays a detailed cross-section view of the three sub-channel types, complete with channel dimensions. Since MANIVEL and OMEGA test section geometries were identical, this information remained the same for all tests.
In addition to the flow geometry, the axial mesh geometry was required by VIPRE-I.

The goal for the mesh sequence was to have a node at the beginning and end of each grid; however, it should be noted that VIPRE-I accounts for flow blockage losses (i.e. grids) by applying a loss coefficient at a single node – the grid loss coefficient was applied at the upstream node of the spacer grid mesh cell. Secondly, it was desirable to have a node near each measurement point in the experimental test sections so that equivalent comparisons between VIPRE-I and the test data could be made. For this reason, it was necessary to have different nodal maps for each test type. Additionally, because temperature measurement locations were not consistent between all OMEGA tests, it was required to generate multiple meshes for the OMEGA SSG and OMEGA MVG tests.

Grid nodes were first placed according to their actual bundle axial location. In the MANIVEL test sections, equal nodal spacing was used in all grid spans with 9 mesh cells being used per span in LDV measurement spans and 7 mesh cells per span being used in all other spans.
In the OMEGA test sections, equal nodal spacing was used in non-measurement areas of the test section with 7 mesh cells per span. In temperature measurement spans, mesh cell size was varied to best match axial nodal location and axial measurement location.

The following figures (Figure 3.2 through Figure 3.6) show the actual SSG mesh configurations implemented into VIPRE-I for the associated tests. The meshes show the node locations in both millimeters and inches. The meshes also show the actual measurement and grid locations for comparison; however, only mesh cell size and nodal placement was entered into the GEOM card of the VIPRE-I input deck – grid loss coefficient data was entered later on the GRID card. Figure 3.4 shows the mesh used for single-phase test run 4.1 and Figure 3.5 shows the mesh used for single-phase run 4.2. All other OMEGA runs were modeled with the mesh shown in Figure 3.3. Figure 3.6 was used for all of the OMEGA SSG ONB tests.
Figure 3.2: MANIVEL SSG Nodal Mesh
Figure 3.3: OMEGA SSG Mesh 1
Figure 3.4: OMEGA SSG Mesh 2 (Run 4.1)
Figure 3.5: OMEGA SSG Mesh 3 (Run 4.2)
Figure 3.6: Mesh for OMEGA SSG ONB Tests
3.2 Rods Modeling Considerations (ROD Card)

The heater tubes in the NESTOR test section were modeled in two different ways for the heated vs. unheated tests. For the MANIVEL tests, the rods were modeled as *dummy* rods, where only rod geometry and heat flux was specified. In the case of the MANIVEL tests, the heat flux was zero. Modeling the tubes as *dummy* rods not only made code computations quicker, but also required less user input. The only information required by the user, in this case, was the number of rods (25), the different types of rods (only one type for MANIVEL), the heating length, the rod diameter, and the way in which each rod fits into the assembly (the sub-channels with which the rod interacts).

For the OMEGA tests, the rods were modeled with the conduction model in order for the code to output the rod outside temperatures. Because of this, additional information was required. The outside diameter of all tubes (both inner and outer) was equal to 9.5 mm. In addition to the tube geometry, the conduction model also required the node location in the tube wall. It was only necessary to place two nodes with one node being on the rod-outside surface since this was the desired temperature measurement location. Material properties for Zircaloy were arbitrarily entered for the rod material, but since the internal temperature distribution of the heater rod was not required from VIPRE-I, this information had no bearing on results to be used in the analysis.

The rod power to average power peaking factor for each rod was the final piece of information required by VIPRE-I. The NESTOR B-Report (EPRI, Palo Alto, CA, EDF, France, and CEA, France, 2006) declared a rod inner-to-outer peaking factor of 1.3, but the actual measured value was closer to 1.283. This was used to calculate the necessary input information, as follows,
\[
\frac{inner}{outer} = 1.283
\]

\[16 \cdot outer + 9 \cdot inner = 25\]

\[inner = 1.16437 \text{ and } outer = 0.907540\]

The most accurate peaking factors used by CEA and EDF were 1.164311 and 0.907575. For consistency’s sake, these were the actual values used in VIPRE-I.

### 3.3 Simulation Operating Condition Input (OPER Card)

Nine tests were run on the MANIVEL experimental loop which required modeling by VIPRE-I. Eight tests provided pressure drop measurement information and one provided detailed axial velocity measurement data. Actually, several sets of test conditions were provided during the long process of collecting velocity measurements; however, the targeted Reynolds number remained the same and so the slight variations in test conditions were averaged together into one set of VIPRE-I input conditions. Table 3.1 and Table 3.2 show the operating conditions for the MANIVEL Phase 6 pressure drop experiments and the Phase 3 velocity measurement experiments. The data given in NESTOR data reports was in SI units; however, data is presented here in English units since that is the required input method of VIPRE-I. Power was set to zero for the MANIVEL test decks. Fifteen single-phase tests were run on the OMEGA SSG test loop, as well as 17 ONB tests. The stated bundle power was multiplied by 0.995 due to resistances in the rod connections.
Table 3.1: MANIVEL SSG Phase 6 Runs

<table>
<thead>
<tr>
<th>Run</th>
<th>P (psia)</th>
<th>Tin (°F)</th>
<th>G (lbm/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2264</td>
<td>63.68</td>
<td>12.44565</td>
</tr>
<tr>
<td>2</td>
<td>2264</td>
<td>63.14</td>
<td>18.63155</td>
</tr>
<tr>
<td>3</td>
<td>2264</td>
<td>62.78</td>
<td>24.35581</td>
</tr>
<tr>
<td>4</td>
<td>2264</td>
<td>62.6</td>
<td>31.15107</td>
</tr>
<tr>
<td>5</td>
<td>2264</td>
<td>62.6</td>
<td>37.24464</td>
</tr>
<tr>
<td>6</td>
<td>2264</td>
<td>62.42</td>
<td>43.52286</td>
</tr>
<tr>
<td>7</td>
<td>2264</td>
<td>86.72</td>
<td>37.40467</td>
</tr>
<tr>
<td>8</td>
<td>2264</td>
<td>86.72</td>
<td>40.34066</td>
</tr>
</tbody>
</table>

Table 3.2: MANIVEL SSG Phase 3 Run

<table>
<thead>
<tr>
<th>Run</th>
<th>P (psia)</th>
<th>Tin (°F)</th>
<th>G (lbm/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Average</td>
<td>2264</td>
<td>30.4</td>
<td>37.34312</td>
</tr>
</tbody>
</table>

Table 3.3: OMEGA SSG Single-Phase Runs

<table>
<thead>
<tr>
<th>Run</th>
<th>P (psia)</th>
<th>G (Mlbm/hr-ft^2)</th>
<th>Tin (°F)</th>
<th>Pave (kW/ft)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2264</td>
<td>2.24888</td>
<td>366.44</td>
<td>6.82238</td>
</tr>
<tr>
<td>2</td>
<td>2262.6</td>
<td>2.21939</td>
<td>500.54</td>
<td>6.75605</td>
</tr>
<tr>
<td>3.1</td>
<td>2266.9</td>
<td>2.57331</td>
<td>379.22</td>
<td>6.79253</td>
</tr>
<tr>
<td>3.2</td>
<td>2262.6</td>
<td>2.58806</td>
<td>379.58</td>
<td>6.78590</td>
</tr>
<tr>
<td>4.1</td>
<td>2264.0</td>
<td>2.65442</td>
<td>520.16</td>
<td>6.75605</td>
</tr>
<tr>
<td>4.2</td>
<td>2262.6</td>
<td>2.62492</td>
<td>520.16</td>
<td>6.75605</td>
</tr>
<tr>
<td>4.3</td>
<td>2262.6</td>
<td>2.59543</td>
<td>519.08</td>
<td>6.76268</td>
</tr>
<tr>
<td>4.4</td>
<td>2264.0</td>
<td>2.61018</td>
<td>520.16</td>
<td>6.91857</td>
</tr>
<tr>
<td>5</td>
<td>2262.6</td>
<td>2.94198</td>
<td>386.6</td>
<td>6.78590</td>
</tr>
<tr>
<td>6</td>
<td>2264.0</td>
<td>2.97147</td>
<td>428</td>
<td>6.78590</td>
</tr>
<tr>
<td>7</td>
<td>2264.0</td>
<td>2.95673</td>
<td>466.52</td>
<td>6.78590</td>
</tr>
<tr>
<td>8</td>
<td>2264.0</td>
<td>2.95673</td>
<td>498.02</td>
<td>6.78590</td>
</tr>
<tr>
<td>9</td>
<td>2264.0</td>
<td>3.34752</td>
<td>482.9</td>
<td>6.78590</td>
</tr>
<tr>
<td>10</td>
<td>2264.0</td>
<td>3.36226</td>
<td>523.94</td>
<td>6.76268</td>
</tr>
<tr>
<td>11</td>
<td>2259.7</td>
<td>2.58806</td>
<td>540.14</td>
<td>4.51730</td>
</tr>
</tbody>
</table>
Table 3.4: OMEGA SSG ONB Runs

<table>
<thead>
<tr>
<th>Run</th>
<th>P (psia)</th>
<th>G (Mlbm/hr-ft^2)</th>
<th>Ti (F)</th>
<th>Pave (kW/ft)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>2262.6</td>
<td>2.62492</td>
<td>536.54</td>
<td>6.91857</td>
</tr>
<tr>
<td>1.2</td>
<td>2264.0</td>
<td>2.61018</td>
<td>543.38</td>
<td>6.91857</td>
</tr>
<tr>
<td>1.3</td>
<td>2264.0</td>
<td>2.58806</td>
<td>545.54</td>
<td>6.92520</td>
</tr>
<tr>
<td>1.4</td>
<td>2264.0</td>
<td>2.58806</td>
<td>547.52</td>
<td>6.91857</td>
</tr>
<tr>
<td>1.5</td>
<td>2264.0</td>
<td>2.61018</td>
<td>549.68</td>
<td>6.85555</td>
</tr>
<tr>
<td>1.6-1</td>
<td>2262.6</td>
<td>2.59543</td>
<td>554.18</td>
<td>6.91857</td>
</tr>
<tr>
<td>1.6-2</td>
<td>2264.0</td>
<td>2.58806</td>
<td>554.36</td>
<td>6.91857</td>
</tr>
<tr>
<td>2.1</td>
<td>2264.0</td>
<td>3.33277</td>
<td>560.84</td>
<td>6.92520</td>
</tr>
<tr>
<td>2.2</td>
<td>2264.0</td>
<td>3.33277</td>
<td>567.14</td>
<td>6.91857</td>
</tr>
<tr>
<td>2.3</td>
<td>2264.0</td>
<td>3.33277</td>
<td>571.28</td>
<td>6.91857</td>
</tr>
<tr>
<td>2.4</td>
<td>2264.0</td>
<td>3.32539</td>
<td>577.4</td>
<td>6.92520</td>
</tr>
<tr>
<td>2.5-1</td>
<td>2265.5</td>
<td>3.31802</td>
<td>582.8</td>
<td>6.92852</td>
</tr>
<tr>
<td>2.5-2</td>
<td>2265.5</td>
<td>3.34752</td>
<td>582.8</td>
<td>6.91857</td>
</tr>
<tr>
<td>2.5-3</td>
<td>2265.5</td>
<td>3.35489</td>
<td>583.34</td>
<td>6.91857</td>
</tr>
<tr>
<td>3.1</td>
<td>2264.0</td>
<td>2.59543</td>
<td>500.54</td>
<td>8.99812</td>
</tr>
<tr>
<td>3.2</td>
<td>2264.0</td>
<td>2.58068</td>
<td>505.76</td>
<td>8.99812</td>
</tr>
<tr>
<td>3.3</td>
<td>2264.0</td>
<td>2.58806</td>
<td>511.16</td>
<td>8.99812</td>
</tr>
</tbody>
</table>

3.4 Flow-Field Loss Coefficients (DRAG and GRID Cards)

Pressure loss caused by bundle surface friction and flow blockages appears in the axial momentum equation. The frictional pressure loss is calculated using the Darcy formulation for frictional pressure drop show by Equation (3.1).

\[
\frac{\Delta P}{\Delta X} = f \rho u^2 \\
2D_e g_c
\]

(3.1)

The friction factor, \( f \), can either be modeled by the default Blasius smooth tube correlation in VIPRE-I or by user input, provided in the DRAG section of the input deck. Another cause of flow resistance, form losses due to flow blockages, is modeled using Equation (3.2).
The term $k$ is the orifice loss coefficient or the grid loss coefficient. The grid loss coefficients are input by the user in the GRID section of the input deck either as a constant or a Reynolds-dependent correlation. The previous two losses provide resistance to the direction of the flow; however, there can also be resistance lateral flow going through gaps. This is modeled as a form loss in VIPRE-I using Equation (3.3).

$$\frac{\Delta P}{\Delta X} = \frac{k_p u^2}{2g_c}$$  

(3.2)

In this equation, $w$ represents the cross-flow ($lbm/ft$-$s$), $v'$ represents the specific volume ($ft^3/lbm$), and $s$ represents the gap width ($ft$). The gap loss coefficient is denoted with $k_{gap}$, which must be supplied by user input either as a constant or as a Reynolds dependent correlation. The gap loss coefficient was set to the VIPRE-I manual-suggested value of 0.5, but the other flow-field loss coefficients, $f$ and $k$, were dependent on the specific test section materials and SSG design and were determined using the MANIVEL SSG pressure drop experiments, the operating conditions of which were presented in Table 3.1. The loss coefficient determination process and results are described in the following chapter on code calibration.

3.5 Correlations and Constitutive Models (CORR Card)

VIPRE-I uses the homogeneous model for two-phase flow, which means that flow field models use two-phase mixture fluid properties and calculates the flow solution as if one homogenously mixed fluid existed. This model is suitable for high pressure, high mass velocity flows, like the case of the NESTOR tests. However, VIPRE-I does allow for modeling of some
non-homogeneous aspects of the two-phase region with the use of two-phase flow multipliers, sub-cooled void correlations, and slip ratios between phases. Inclusion of these adjustments and associated models are specified by user input; however, because only single-phase flow was to be modeled for the NESTOR tests, it was possible to forgo the selection of two-phase flow models. Instead, the input flag for specifying the modeling of single-phase flow, only, was made.

Subsequently, the Dittus-Boelter model, shown in Equation (3.4) was selected for determining single-phase heat transfer.

\[ h = (0.023Re^{0.8} Pr^{0.4}) \left( \frac{D_e}{k} \right) \]  
(3.4)

### 3.6 Turbulent Mixing Model (MIXX Card)

The final modeling consideration that was required in VIPRE-I was the modeling of turbulent mixing. As discussed in Section 1.4, VIPRE-I uses four conservation equations to model mass, energy and momentum changes in control volumes specified by the user. Mixing of energy and momentum between sub-channels caused by flow turbulence is considered in the energy and axial momentum conservation equations using simple turbulent mixing coefficients.

For convenience, the energy and mass conservation equations are repeated here in Equations (3.5) and (3.6), respectively.

\[ A \frac{\partial}{\partial t} \langle (\rho h) \rangle + \frac{\partial}{\partial x} (\rho U h) A + \sum_{k \in l} e_{ik} \langle \rho V h \rangle S \]

\[ = \sum_{n \in l} P_{w} \Phi_{in} \langle q'' \rangle - \sum_{k \in l} w' \Delta h + \sum_{n \in l} C_{Q} \Phi_{in} q' \]  
(3.5)
\[
\frac{\partial}{\partial t} \langle (pU) \rangle A + \frac{\partial}{\partial x} \langle (pU^2) \rangle A + \sum_{k \in i} e_{ik} \langle pUV \rangle S = -A \frac{\partial}{\partial x} \langle P \rangle - \frac{1}{2} \left( \frac{f}{D_h} + K' \right) \langle pU^2 \rangle A - A \langle \langle \rho \rangle \rangle g \cos(\theta) \\
- c_T \sum_{k \in i} w' \Delta U
\] (3.6)

The turbulent mixing of energy is accounted for with the second term of the right-hand-side of the energy conservation equation and the turbulent mixing of momentum is accounted for with the last term on the right-hand-side of the axial momentum equation. The \( w' \) term represents the amount of mass fluctuation between adjacent sub-channels resulting from turbulence. In the energy equation, this fluctuation term is multiplied by the sub-channel enthalpy difference, while in the momentum equation it is multiplied by the sub-channel velocity difference. There is an additional factor \( C_T \), which is multiplied by this turbulent mixing term and tells the code how efficiently flow turbulence mixes momentum. The term is similar to the turbulent Prandtl number. Physically, a value of 0.0 means that turbulence only mixes enthalpy and a value of 1.0 means that turbulence mixes enthalpy and momentum with equal strength.

According to the VIPRE-I user’s manual, the choice of the turbulent momentum factor, \( C_T \), has little effect on the code solution (Battelle Pacific Northwest Laboratories, 2007). It can be noted that the turbulent mixing terms are summed over all gaps connected to the control volume of interest.

The calculation of the turbulent mixing \( w' \) is accomplished using one, user-specified equation out of four equations included in VIPRE-I. These equations are shown in Equations (3.7) through (3.10).

\[ w' = \beta_m \tilde{S} \tilde{G} \] (3.7)
In these equations, $\beta$ is the mixing coefficient, $S$ is the gap width between the sub-channels of interest, and $G$ is the average of the mass velocities in the adjacent sub-channels. For modeling the NESTOR test sections, Equation (3.7) was used. Physically, the mixing coefficient is defined as the eddy diffusivity divided by the average axial velocity. Both the mixing coefficient, $\beta$, as well as the turbulent momentum factor, $C_T$, are required by user-input in the MIXX card. Since these terms are dependent on the flow conditions, an optimization was performed to determine the values that should be used. The MANIVEL velocity measurement data was used to optimize the $C_T\beta$ product which appears in the axial momentum equation when Equation (3.7) is substituted into Equation (3.6). The OMEGA EOHL temperature measurement data was used to optimize the $\beta$ coefficient appearing in both the energy and momentum equations. The optimization process is further described in the following chapter.
Chapter 4: VIPRE-I Model Optimization

It was necessary to calibrate the sub-channel analysis codes to the test section geometry and operating conditions before any data analysis could be successfully performed. This calibration process involved using MANIVEL pressure drop results to develop grid loss and friction loss coefficients for the test sections, using MANIVEL LDV measurement results for calibrating the turbulent viscosity parameter of the mixing model (see Section 0), and using OMEGA EOHL temperature measurements for optimization of the turbulent conductivity parameter of the VIPRE-I mixing model.

4.1 Friction and Grid-Loss Calibration

Friction ($f$) and grid-loss coefficient ($K$) correlations were developed for the SSG tests using the MANIVEL Phase 6 pressure drop results. Since the measured pressure drop was a combination of wall friction and flow blockage effects, it was possible to calculate friction and grid-loss coefficients from the measured results. The $f$-$K$ set was developed in three different ways: 1) using a pre-defined friction loss correlation and directly calculating the grid-loss coefficient required to match experimental pressure drop results, 2) using a pre-defined friction loss correlation in THYC-COEUR and performing a parametric study to converge on the appropriate grid-loss coefficient required to match computed and experimental pressure drop, and 3) calculating the grid-loss coefficient using an empirical correlation and directly calculating the friction loss coefficient required to match experimental and calculated pressure drop results. The first two methods involved using the well-known Coleburn friction correlation as the pre-defined equation and these methods were executed by EDF. The third method was a combined effort.

The first method calculation of the \( f-K \) set involved using the measured pressure drop data of the MANIVEL Phase 6 tests and Equation (4.1), which sums pressure drop across a test section segment caused by wall friction and flow-blockage effects.

\[
\Delta P = \frac{f \rho u^2}{2D_e g_c} + \frac{k \rho u^2}{2g_c} \tag{4.1}
\]

The Coleburn equation, shown by Equation (4.2), was assumed to accurately represent the frictional losses in the SSG tests for both \( f-K \) development methods 1 and 2.

\[
f = 0.184 \times Re^{-0.2} \tag{4.2}
\]

Substituting the bundle friction factor into Equation (4.1), it was possible to back-calculate the required grid-loss coefficient. The Phase 6 tests were run at different Reynolds numbers for the purpose of calculating a Re-dependent correlation for modeling the SSG loss coefficients. The calculated grid-loss coefficient correlation using the direct-calculation method is shown in Equation (4.3).

\[
k = 2.752 \times Re^{-0.161} \tag{4.3}
\]

The second \( f-K \) determination method involved inputting Equation (4.2) into THYC-COEUR and performing a parametric study to converge on the appropriate grid loss coefficient correlation, which is shown in Equation (4.4).

\[
k = 2.603 \times Re^{-0.163} \tag{4.4}
\]

The drawback of the previous two methods was two-fold: 1) it was not know if the Coleburn equation accurately models the actual friction factor of the NESTOR flow loops and 2) only a bundle-average grid loss coefficient was obtained. The third \( f-K \) calculation method
involved using the empirical Lahey-Shiralkar technique for modeling grid loss (Lahey, 1970). The method involved calculating grid loss caused by friction in the grid and by the contraction and expansion of the flow passing through the grid. The local loss coefficient caused by the contraction and expansion of the flow was calculated by Equation (4.5).

\[ K_L = \left( \frac{\sqrt{k'(1-\sigma)} + (1-\sigma)}{\sigma} \right)^2 \]  

(4.5)

The terms \( k' \) and \( \sigma \) represent a geometry-dependent loss coefficient and the contraction ratio of the grid, respectively. The value of \( k' \) for a sharp-edged orifice, like that present in a spacer grid, is 0.5. The frictional loss was calculated as shown in Equation (4.6).

\[ K_f = \frac{f L P_u}{4 A_u} \left[ \frac{P_R}{P_u} \left( \frac{1}{\sigma} \right)^3 - 1 \right] \]  

(4.6)

Here, \( P_U \) and \( P_R \) are the upstream and downstream wetted perimeters and \( A_U \) is the upstream flow area. When expanded, this equation becomes,

\[ K_f = \frac{f L P_R}{4 A_u} \left( \frac{1}{\sigma} \right)^2 \left( \frac{A_u}{A_R} \right) - \frac{f L P_u}{4 A_u} \]  

(4.7)

\[ K_f = \frac{f L P_R}{4 A_R} \left( \frac{1}{\sigma} \right)^2 - \frac{f L P_u}{4 A_u} \]

The hydraulic diameter is defined as,

\[ D_e = \frac{4A}{P_w} \]  

(4.8)

Using the definition of Equation (4.8) in Equation (4.7) yields,

\[ K_f = \frac{f R_L}{D_{e,R}} \left( \frac{1}{\sigma} \right)^2 - \frac{f_u L}{D_{e,u}} \]  

(4.9)
The two terms of this equation represent the frictional loss inside the bundle caused by the increased speed of the flow minus the normal frictional loss of the bundle that is experienced outside of the grid. Subtracting off this second term prevents double counting of the frictional loss inside the grid region. In VIPRE-I, the grid loss is modeled by applying a loss coefficient where the grid is located. The loss coefficient is applied at one node and not over the actual length of the grid. The code will then calculate the frictional loss of the bundle with outside-of-grid conditions over the remainder of the grid region. For this reason, the grid loss coefficient applied should have the frictional loss caused by upstream conditions subtracted off since the code will compute that value separately.

Instead of using a single, constant friction factor in Equation (4.9), the method was altered to use a friction factor based on *in grid* conditions for the first term of Equation (4.9) and *upstream* conditions for the second term. It was assumed that the same friction factor correlation applied both *upstream* and *in* the grid, but the Reynolds numbers used in calculating the friction factor for the two separate locations were calculated based on the local test section geometry (i.e. *upstream* of the grid or *in* the grid).

The Coleburn equation was used for providing an *initial guess* for the bundle and grid friction factors and the grid-loss coefficient was calculated. Then the grid loss coefficient was used with Equation (4.1) to calculate a new friction factor, which was in-turn used to calculate a new grid loss coefficient. This iterative process was repeated until appropriate friction and grid-loss coefficients were converged upon. This process was performed for each of the MANIVEL Phase 6 tests to develop friction and grid-loss coefficient correlations.

Because the Lahey-Shiralkar method used the grid geometry to calculate the loss coefficient, it was possible to calculate separate loss coefficients for each of the three sub-channel
types which were shown in Figure 1.5. A bundle-average grid loss correlation was also calculated using Equation (4.10).

\[
\bar{K} = \frac{(16A_I + 16A_S + 4A_c)^2}{\left(4A_c K_c^{-\frac{1}{2}} + 16A_s K_s^{-\frac{1}{2}} + 4A_I K_I^{-\frac{1}{2}}\right)^2}
\]  

As previously mentioned, it was necessary to calculate both upstream and in grid Reynolds numbers for this method. Furthermore, since the grid loss coefficients of different sub-channel types were calculated separately, it was also necessary to calculate the Reynolds number for the separate sub-channel types. The Reynolds numbers given in the MANIVEL SSG report (Decossin, 2007) were calculated based on inner sub-channel geometry with a hydraulic diameter of 11.778 mm. The correlations, however, should be given in terms of Reynolds numbers based on the bundle hydraulic diameter of 10.28 mm. Since the hydraulic diameters were the only parameters that differ in the Re calculation, the bundle Reynolds number was calculated by multiplying the inner channel Reynolds number by the ratio of the bundle-to-inner channel hydraulic diameters. With these Reynolds numbers, the friction factor was calculated by using the Reynolds number of the individual sub-channel being analyzed. The Reynolds numbers for the different sub-channels and bundle locations are given in Table 4.1 for the MANIVEL SSG Phase 6 tests. The sub-channel geometries both upstream and in grid were also needed for calculation of Equation (4.9), which are provided in Table 4.2.
Table 4.1: Reynolds Numbers in Different Sub-channels and Bundle Locations for MANIVEL SSG Phase 6 Tests

<table>
<thead>
<tr>
<th>From Report</th>
<th>Upstream</th>
<th>In Grid</th>
</tr>
</thead>
<tbody>
<tr>
<td>Re (typical SC)</td>
<td>Re (bundle)</td>
<td>Re (inner)</td>
</tr>
<tr>
<td>23939</td>
<td>20895</td>
<td>23939</td>
</tr>
<tr>
<td>35500</td>
<td>30986</td>
<td>35500</td>
</tr>
<tr>
<td>46237</td>
<td>40358</td>
<td>46237</td>
</tr>
<tr>
<td>58967</td>
<td>51469</td>
<td>58967</td>
</tr>
<tr>
<td>70442</td>
<td>61485</td>
<td>70442</td>
</tr>
<tr>
<td>82244</td>
<td>71786</td>
<td>82244</td>
</tr>
<tr>
<td>96301</td>
<td>84056</td>
<td>96301</td>
</tr>
<tr>
<td>103974</td>
<td>90753</td>
<td>103974</td>
</tr>
</tbody>
</table>

Table 4.2: SSG Individual Sub-Channel Geometries in Bare-Bundle and In-Grid Sections

<table>
<thead>
<tr>
<th>Subchannel Type</th>
<th>A_u (mm^2)</th>
<th>A_R (mm^2)</th>
<th>σ</th>
<th>L (mm)</th>
<th>P_u (mm)</th>
<th>P_R (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Internal</td>
<td>87.88</td>
<td>8.484</td>
<td>0.903</td>
<td>8</td>
<td>29.845</td>
<td>114.288</td>
</tr>
<tr>
<td>Side</td>
<td>63.47</td>
<td>6.373</td>
<td>0.900</td>
<td>8</td>
<td>27.52</td>
<td>91.054</td>
</tr>
<tr>
<td>Corner</td>
<td>43.9</td>
<td>4.262</td>
<td>0.903</td>
<td>8</td>
<td>23.16</td>
<td>65.783</td>
</tr>
</tbody>
</table>

Correlations resulting from the Method 3 f-K determination are summarized in Table 4.3 along with the results from the other f-K determination methods. Because it was more accurate to model the separate sub-channel types with individual associated grid loss coefficients, the results of Method 3 were used to modify Method 2 results. Method 2 produced a bundle-average loss coefficient, which was multiplied by the Method 3 ratios between sub-channel specific loss coefficients and the bundle average coefficient. In this manner, sub-channel specific loss coefficients were generated for the Method 2 grid loss correlation based on the results of Method 3. These correlations are also shown in Table 4.3.
4.2 Turbulent Viscosity Optimization

Eighth-bundle symmetry was utilized in the SSG tests, as was shown in Figure 1.5. Additionally, for the SSG test, it was determined that velocity measurements that were taken in different grid spans, but at the same downstream distance from the grid, should be averaged together due to axial symmetry. Thus, the 10 axial measurement levels were reduced to 6 (note that the first measurements in the two grid spans were 25 mm downstream and 35 mm downstream and weren’t averaged together). LDV measurement locations relative to the upstream grids of spans 1a and 1b are shown in Table 4.4.

| Grid span 2a | 25 | - | 50 | 90 | 160 | 235 |
| Grid span 2b | -  | 35| 50 | 90 | 160 | 235 |

In Section 0, the VIPRE-I mixing model was described and two user-input parameters, $\beta$ and $C_T$, were described. These two values were the focus of the mixing model calibration. For the unheated MANIVEL tests, the parameters only appear in the momentum equation since $\Delta h = 0$ in the energy equation, so the $C_T\beta$ product was optimized using the MANIVEL Phase 3 results velocity measurement results. This was done by incrementally varying the mixing coefficient, $\beta$, while keeping $C_T$ at a constant value of 1.0 in order to create the best match.
between calculated and experimental velocities. The optimization process could have also been performed by varying $C_T$ while keeping $\beta$ constant, since it was the product of the two user input variables that affected the mixing model for unheated tests. The closeness of the measured and computed velocity results was calculated using a quadratic function (shown in Equation (4.11)), which compared associated measured and computed sub-channel velocities. The comparison was actually made between dimensionless measured and computed velocities, which are calculated as shown in Equation (4.12).

$$D_c = \sum [X_c(i, l) - X_m(i, l)]^2$$  \hspace{1cm} (4.11)

$$X_m = \frac{v_m - v_{ref}}{v_{ref}}$$ and $$X_c = \frac{v_c - v_{ref}}{v_{ref}}$$  \hspace{1cm} (4.12)

In Equation (4.11), $i$ represents the sub-channel number and $l$ represents the axial measurement level. The optimization was performed for the inner sub-channel types 4, 5 and 6, as shown in Figure 1.5. Thus, Equation (4.11) took the following form during the optimization,

$$D_c = [X_c(6, l) - X_m(6, l)]^2 + 2[X_c(5, l) - X_m(5, l)]^2 + [X_c(4, l) - X_m(4, l)]^2$$ \hspace{1cm} (4.13)

Note that average measured velocities of symmetric sub-channels were used for calculating the dimensionless experimental velocities of Equation (4.12). In Equation (4.13), the factor of 2 appears in the middle term to account for the fact that the flow area of sub-channel type 5 is twice that of 4 and 6 due to using $1/8^{th}$ symmetry. Because sub-channel average velocities were calculated by three different averaging methods (Arithmentic, Voronoi and Triangular (see Section 2.1)), the optimization process was performed using the data resulting from each averaging technique. Additionally, the optimization was performed using the three $f-K$ sets to model the tests shown in Table 4.3. It was found that for $f-K$ set 1 and $f-K$ set 2 the optimum $C_T \beta$ values were zero. The optimum $C_T \beta$ parameter came to 0.0048 for $f-K$ set 3.
Graphical results of the optimization are shown in Appendix A with an example of the optimization for $f$-$K$ set 3 shown in Figure 4.1 and for $f$-$K$ set 1 shown in Figure 4.2. In the figures, the quadratic optimization coefficient is plotted with respect to the turbulent viscosity coefficient for each symmetric axial measurement level.

Figure 4.1: $C_T\beta$ Optimization for Arithmetic-Averaged Data and Correlation $f$-$K$ Set 3

Figure 4.2: $C_T\beta$ Optimization for Arithmetic-Averaged Data and Correlation $f$-$K$ Set 1
It can be seen that there was a marked difference in the behavior of the quadratic coefficient trends between the two optimizations. Actually, the $f$-$K$ set 2 optimization trend looked similar to the behavior seen in Figure 4.2. No absolute minimum in the quadratic coefficient was found for $f$-$K$ sets 1 and 2 for any of the averaging methods, so the local minimum of zero was chosen as the optimum *turbulent viscosity* parameter. Optimum $C_T\beta$ values did appear for the $f$-$K$ set 3 optimization, but different measurement levels were found to have different optimum values. It was also clear that there was some anomalous behavior associated with the 35 $mm$ velocity measurement. Because of this fact, an average of all optimum values (excluding the 35 $mm$ value) was taken to arrive at a single optimum value that covered all measurement levels. This was done for each averaging technique used to consolidate velocity measurement data; however, only the result of the arithmetic averaging technique was used to define the optimum mixing value. Results of optimum values for each correlation set and averaging technique are summarized in Table 4.5 along with the associated standard deviation of the optimum values found at different axial levels.

**Table 4.5: Optimum $C_T\beta$ Values Found for all Correlation Sets and Averaging Techniques**

<table>
<thead>
<tr>
<th></th>
<th>Correlation Set 1</th>
<th>Correlation Set 2</th>
<th>Correlation Set 3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Triangular Mean</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0066±0.0015</td>
</tr>
<tr>
<td>Arithmetic Mean</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0048±0.0019</td>
</tr>
<tr>
<td>Voronoi Mean</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0048±0.0019</td>
</tr>
</tbody>
</table>

### 4.3 Turbulent Conductivity Optimization

With the optimum $C_T\beta$ value determined, it was possible to optimize the $\beta$ parameter using OMEGA single-phase test data. Since the tests were heated, the $\beta$ term appeared in the energy equation (see Equation (3.5)) and was able to be optimized by keeping the $C_T\beta$ term constant at the optimum value found in Section 4.2. Unlike the previous optimization, the
optimization using OMEGA data accounts for mixing caused by energy differences between sub-channels. The optimization was performed by varying the mixing coefficient, \( \beta \), and analyzing the effect on an optimization parameter, \( D_c \). However, in this optimization, instead of keeping \( C_T \) constant, the product \( C_T \beta \) was kept constant at the optimum value found in the turbulent viscosity optimization. Thus, optimum values of \( C_T \) and \( \beta \) were simultaneously determined. Additionally, the \( D_c \) parameter was defined to measure differences in dimensionless temperatures between sub-channels so that energy mixing was quantified as opposed to the momentum mixing of the turbulent viscosity optimization. The \( D_c \) formulation for this optimization is shown in Equation (4.14) with associated dimensionless temperature formulations shown in Equation (4.15).

\[
D_c = \sum [X_c(i, l) - X_m(i, l)]^2 \tag{4.14}
\]

\[
X_m = \frac{\tau_m - \tau_{\text{out}}}{\tau_{\text{out}} - \tau_{\text{in}}} \quad \text{and} \quad X_c = \frac{\tau_c - \tau_{\text{out}}}{\tau_{\text{out}} - \tau_{\text{in}}} \tag{4.15}
\]

Once again, \( 1/8^{th} \) bundle symmetry was considered during the optimization and only the inner 9 sub-channels were considered. The previous three correlation sets given in Table 4.3 were used for the OMEGA optimization. The parameter \( \beta \) was varied in increments of 0.001 down to increments of 0.0005 near the \( D_c \) minimum location while keeping \( C_T \beta \) set at the optimum value found in the turbulent viscosity optimization. Additionally, an additional, non-optimized value of \( C_T \beta \) was tested for each \( f-K \) set in order to determine the sensitivity to the choice of \( C_T \beta \). In summary, \( \beta \) was varied using the correlation sets depicted in Table 4.6.
Table 4.6: Correlation Sets for OMEGA Mixing Parameters Optimization

<table>
<thead>
<tr>
<th>Correlation Set</th>
<th>1.1</th>
<th>1.2</th>
<th>2.1</th>
<th>2.2</th>
<th>3.1</th>
<th>3.2</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Bundle Friction</strong></td>
<td>0.184*Re^-0.161</td>
<td>0.184*Re^-0.161</td>
<td>0.280*Re^-0.208</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>Grid Loss Correlation</strong></td>
<td>2.813*Re^-0.161</td>
<td>2.813*Re^-0.161</td>
<td>0.768*Re^-0.104</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{corner}}$</td>
<td>---</td>
<td>2.658Re^-0.161</td>
<td>0.732*Re^-0.103</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{side}}$</td>
<td>---</td>
<td>2.893Re^-0.161</td>
<td>0.813*Re^-0.105</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{inner}}$</td>
<td>---</td>
<td>2.799Re^-0.161</td>
<td>0.812*Re^-0.108</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$C_t\beta$</td>
<td>0.0000</td>
<td>0.0048</td>
<td>0.0000</td>
<td>0.0048</td>
<td>0.0000</td>
<td>0.0048</td>
</tr>
</tbody>
</table>

Optimum $\beta$ values were found for each of the 15 single phase OMEGA SSG tests. These values are plotted against test Re for the 3 correlation sets in Figure 4.3 through Figure 4.5.

Figure 4.3: $\beta$ Optimization for $f$-$K$ Set 1
Figure 4.4: $\beta$ Optimization for $f$-$K$ Set 2

Figure 4.5: $\beta$ Optimization for $f$-$K$ Set 3

It is apparent from these figures that there was an increasing trend of $\beta$ with increasing bundle average Re number, which should be expected. However, it was a loosely correlated trend and so modeling of the 15 single-phase tests was done with the individual optimum $\beta$ values found for each respective test. However, the optimum $\beta$ correlations found in the above figures
were used to determine the optimum diffusivity coefficient for the ONB tests. These figures also show that there was about a 0.005 sensitivity of the optimum $\beta$ value to the ranged of $C_f \beta$ tested. This sensitivity was more or less constant over the entire range of Re tested.

Figure 4.6 provides a summary of the minimum $D_c$ values found for each of the single-phase tests with respect to test ID. Figure 4.7 shows the minimum $D_c$ values with respect to test Re. It’s evident from these charts that all correlation sets had similar minimum $D_c$ values for similar tests and that no trend appeared between Re and modeling accuracy.

![Figure 4.6: Minimum $D_c$ Value for all Single-Phase Tests](image)
Table 4.7 shows the optimum β values for all 15 single-phase testing conditions for both optimized and non-optimized correlation sets.
Table 4.7: Optimum $\beta$ Value for OMEGA Single Phase Tests

<table>
<thead>
<tr>
<th>Correlation Set</th>
<th>1.1</th>
<th>1.2</th>
<th>2.1</th>
<th>2.2</th>
<th>3.1</th>
<th>3.2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Run</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>0.008</td>
<td>0.0085</td>
<td>0.008</td>
<td>0.0085</td>
<td>0.007</td>
<td>0.0075</td>
</tr>
<tr>
<td>2</td>
<td>0.0095</td>
<td>0.01</td>
<td>0.009</td>
<td>0.0095</td>
<td>0.0085</td>
<td>0.009</td>
</tr>
<tr>
<td>3.1</td>
<td>0.0085</td>
<td>0.009</td>
<td>0.008</td>
<td>0.0085</td>
<td>0.0075</td>
<td>0.008</td>
</tr>
<tr>
<td>3.2</td>
<td>0.0095</td>
<td>0.01</td>
<td>0.0095</td>
<td>0.01</td>
<td>0.0085</td>
<td>0.009</td>
</tr>
<tr>
<td>4.1</td>
<td>0.0095</td>
<td>0.01</td>
<td>0.0095</td>
<td>0.01</td>
<td>0.0085</td>
<td>0.009</td>
</tr>
<tr>
<td>4.2</td>
<td>0.01</td>
<td>0.0105</td>
<td>0.01</td>
<td>0.0105</td>
<td>0.009</td>
<td>0.0095</td>
</tr>
<tr>
<td>4.3</td>
<td>0.0115</td>
<td>0.012</td>
<td>0.011</td>
<td>0.012</td>
<td>0.01</td>
<td>0.011</td>
</tr>
<tr>
<td>4.4</td>
<td>0.012</td>
<td>0.012</td>
<td>0.0115</td>
<td>0.0125</td>
<td>0.0105</td>
<td>0.0115</td>
</tr>
<tr>
<td>5</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0095</td>
<td>0.01</td>
</tr>
<tr>
<td>6</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0095</td>
<td>0.01</td>
</tr>
<tr>
<td>7</td>
<td>0.011</td>
<td>0.0115</td>
<td>0.011</td>
<td>0.0115</td>
<td>0.01</td>
<td>0.0105</td>
</tr>
<tr>
<td>8</td>
<td>0.012</td>
<td>0.0125</td>
<td>0.0115</td>
<td>0.0105</td>
<td>0.0105</td>
<td>0.011</td>
</tr>
<tr>
<td>9</td>
<td>0.011</td>
<td>0.0115</td>
<td>0.011</td>
<td>0.0115</td>
<td>0.01</td>
<td>0.0105</td>
</tr>
<tr>
<td>10</td>
<td>0.011</td>
<td>0.012</td>
<td>0.011</td>
<td>0.0115</td>
<td>0.01</td>
<td>0.0105</td>
</tr>
<tr>
<td>11</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0105</td>
<td>0.011</td>
<td>0.0095</td>
<td>0.01</td>
</tr>
</tbody>
</table>

### 4.4 Velocity, Temperature and Pressure Sensitivity to Calibration Parameters

After optimizing the three $f$-$K$ sets for VIPRE-I, several sensitivity analyses were performed to determine the impact of the calibrated values on computed velocity, temperature and pressure drop. Analyses were performed for both heated and unheated test configurations. The optimized $f$-$K$ sets that were determined in the previous three sections are summarized in Table 4.8. The sensitivity analysis of computed pressure, velocity and temperature follows.
Table 4.8: Optimized $f$-$K$ Sets used in Sensitivity Analysis

<table>
<thead>
<tr>
<th>Correlation Set</th>
<th>1.1</th>
<th>2.1</th>
<th>3.2</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Bundle Friction</strong></td>
<td>$0.184\text{Re}^{0.161}$</td>
<td>$0.184\text{Re}^{0.161}$</td>
<td>$0.280\text{Re}^{0.208}$</td>
</tr>
<tr>
<td><strong>Grid Loss Correlation</strong></td>
<td>$2.813\text{Re}^{0.161}$</td>
<td>$2.813\text{Re}^{0.161}$</td>
<td>$0.768\text{Re}^{0.104}$</td>
</tr>
<tr>
<td>$k_{\text{corner}}$</td>
<td>---</td>
<td>$2.658\text{Re}^{0.161}$</td>
<td>$0.732\text{Re}^{0.103}$</td>
</tr>
<tr>
<td>$k_{\text{side}}$</td>
<td>---</td>
<td>$2.893\text{Re}^{0.161}$</td>
<td>$0.813\text{Re}^{0.105}$</td>
</tr>
<tr>
<td>$k_{\text{inner}}$</td>
<td>---</td>
<td>$2.799\text{Re}^{0.161}$</td>
<td>$0.812\text{Re}^{0.108}$</td>
</tr>
<tr>
<td>$C_T$</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0048</td>
</tr>
<tr>
<td>$\beta$</td>
<td>0.0100</td>
<td>0.0100</td>
<td>0.0095</td>
</tr>
</tbody>
</table>

### 4.4.1 Computed Pressure Drop Sensitivity

Pressure drop measurements from the MANIVEL SSG Phase 6 data were used for comparison to calculated pressure drop values. Because VIPRE-I considers the pressure drop caused by gravity and the differential pressure transducers did not, the gravitational pressure drop for a 279 mm span (0.3981 psi) were subtracted off of the VIPRE-I results before comparing results. Comparisons were made using the three $f$-$K$ sets of Table 4.8. It wasn’t anticipated that the mixing coefficient values would impact pressure drop results. Comparisons were made for the average pressure drop of each test (pressure drop of all four pressure transducer measurements were averaged together). Figure 4.8 shows the ratio of the average measured and predicted pressure drops for each test. The ratios are presented as a function of bundle-average Reynolds number for the associated tests of Phase 6. Additionally, the deviation and relative deviation of the predicted results from measured results is presented in Table 4.9.
Figure 4.8: Comparison of Measured and Predicted Pressure Drops for MANIVEL SSG Phase 6 Results

Table 4.9: Measured and Predicted Pressure Drop Statistics for MANIVEL SSG Phase 6 Data

<table>
<thead>
<tr>
<th>Run</th>
<th>Re (bundle)</th>
<th>Mean Measured dP (mbar)</th>
<th>Mean Computed dP (mbar)</th>
<th>Correlation Set 1</th>
<th>Correlation Set 2</th>
<th>Correlation Set 3</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>20895</td>
<td>29.25</td>
<td>29.32</td>
<td>29.20</td>
<td>28.86</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>30986</td>
<td>60.73</td>
<td>60.80</td>
<td>60.80</td>
<td>60.00</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>40358</td>
<td>98.58</td>
<td>98.84</td>
<td>99.07</td>
<td>97.58</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>51469</td>
<td>153.98</td>
<td>154.69</td>
<td>154.92</td>
<td>152.50</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>61485</td>
<td>212.70</td>
<td>213.98</td>
<td>214.44</td>
<td>210.88</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>71786</td>
<td>282.60</td>
<td>283.85</td>
<td>284.54</td>
<td>279.82</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>84056</td>
<td>201.45</td>
<td>203.87</td>
<td>204.21</td>
<td>200.77</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>90753</td>
<td>234.23</td>
<td>233.74</td>
<td>234.43</td>
<td>230.53</td>
<td></td>
</tr>
</tbody>
</table>

Statistics between Measured and Predicted Pressure Losses

<table>
<thead>
<tr>
<th>Relative Deviation</th>
<th>RMS Value</th>
<th>Mean Value</th>
<th>RMS Value (mbar)</th>
<th>Mean Value (mbar)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.005</td>
<td>0.004</td>
<td>1.110</td>
<td>0.698</td>
</tr>
<tr>
<td></td>
<td>0.007</td>
<td>0.005</td>
<td>1.396</td>
<td>1.014</td>
</tr>
<tr>
<td></td>
<td>0.011</td>
<td>-0.010</td>
<td>1.904</td>
<td>-1.571</td>
</tr>
</tbody>
</table>

It’s clear from the figure and table that $f-K$ set 1 produced the most accurate pressure drop results while $f-K$ set 3 produced the least accurate results. Overall, pressure drop computations
were insensitive to the optimized $f$-$K$ set choice since all results were within 1.6% of the measured results.

### 4.4.2 Computed Axial Velocity Sensitivity for Unheated Test Conditions

It was anticipated that axial velocity profile prediction would be affected by both $f$-$K$ set and mixing model choices. Table 4.10 parameters were used as input including correlation sets 3.1 and 3.3 to test the sensitivity to the turbulent viscosity parameter. By comparing results using correlation sets 1.1, 2.1 and 3.2, the velocity sensitivity to friction- and grid-loss was tested. Note that the $C_f\beta$ parameter in correlation sets 3.1 and 3.3 was varied by $\pm 1\sigma$ from the mean values found in Section 4.2.

<table>
<thead>
<tr>
<th>Correlation Set</th>
<th>1.1</th>
<th>2.1</th>
<th>3.1</th>
<th>3.2</th>
<th>3.3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bundle Friction</td>
<td>0.184$Re^{-0.161}$</td>
<td>0.184$Re^{-0.161}$</td>
<td>0.280$Re^{-0.208}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Grid Loss Correlation</td>
<td>2.813$Re^{-0.161}$</td>
<td>2.813$Re^{-0.161}$</td>
<td>0.768$Re^{-0.104}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{corner}}$</td>
<td>---</td>
<td>2.658$Re^{-0.161}$</td>
<td>0.732$Re^{-0.103}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{side}}$</td>
<td>---</td>
<td>2.893$Re^{-0.161}$</td>
<td>0.813$Re^{-0.105}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{inner}}$</td>
<td>---</td>
<td>2.799$Re^{-0.161}$</td>
<td>0.812$Re^{-0.108}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$C_f\beta$</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0029</td>
<td>0.0048</td>
<td>0.0067</td>
</tr>
</tbody>
</table>

Appendix B shows results of the sensitivity analysis using the correlation sets shown in Table 4.10 as VIPRE-I input. The maximum variation in velocity caused by optimized $f$-$K$ set choice and $C_f\beta$ variation of $\pm 1\sigma$ are shown in Table 4.11.
Table 4.11: Maximum Sensitivities to Optimized $f-K$ Set and $\pm 1 \sigma$ Variation of $C_{f\beta}$ (Range Shown in Parentheses)

<table>
<thead>
<tr>
<th></th>
<th>Ch. 4</th>
<th>Ch. 5</th>
<th>Ch. 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f-K$ Sensitivity (1.1 vs. 2.1 vs. 3.2)</td>
<td>-0.13% to 0.03% (0.16%)</td>
<td>-0.05% to 0.04% (0.09%)</td>
<td>-0.02% to 0.09% (0.11%)</td>
</tr>
<tr>
<td>$C_{f\beta}$ Sensitivity (3.1 vs. 3.2 vs. 3.3)</td>
<td>-0.14% to 0.08% (0.22%)</td>
<td>-0.11% to 0.06% (0.17%)</td>
<td>-0.09% to 0.05% (0.14%)</td>
</tr>
</tbody>
</table>

The results of the velocity sensitivity analysis showed that the variation in $C_{f\beta}$ had, by a slight margin, a greater impact in the computed velocity results than did the choice of the $f-K$ parameters. Varying $C_{f\beta}$ by a greater amount than $1 \sigma$ should be expected to have an even greater impact on computed velocity profile results. Overall, the maximum range of variation in the calculated velocity profile occurred in channel 4 when $C_{f\beta}$ was varied by $\pm 1 \sigma$ and totaled less than one quarter of one percent. It is evident from this analysis that velocity results were not particularly sensitive to the loss coefficients and mixing model parameters developed for modeling the SSG tests when considering the unheated configuration of the MANIVEL tests.

The effect of input parameter selection was also tested by comparing MANIVEL Phase 3 measured sub-channel velocities and simulation results. Results were obtained for all 6 sub-channel types, which are presented in Appendix C. Maximum deviations between computed and measured velocities are summarized in Table 4.12. It can be seen from this table that, overall, $f-K$ set 1.1 of Table 4.10 performed best by a slight margin. For inner-type sub-channels, the maximum deviation between predicted and measured axial velocity observed was less than 2.5%.
Table 4.12: Maximum Deviations between Predicted and Measured Velocities using Optimized $f$-$K$ Correlation Sets

<table>
<thead>
<tr>
<th>Correlation</th>
<th>Ch 1</th>
<th>Ch 2</th>
<th>Ch 3</th>
<th>Ch 4</th>
<th>Ch 5</th>
<th>Ch 6</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>3.40%</td>
<td>0.77%</td>
<td>2.17%</td>
<td>1.12%</td>
<td>1.32%</td>
<td>0.86%</td>
</tr>
<tr>
<td>2.1</td>
<td>4.86%</td>
<td>1.24%</td>
<td>2.77%</td>
<td>1.17%</td>
<td>1.07%</td>
<td>1.12%</td>
</tr>
<tr>
<td>3.2</td>
<td>1.85%</td>
<td>2.08%</td>
<td>3.15%</td>
<td>1.35%</td>
<td>0.83%</td>
<td>2.39%</td>
</tr>
</tbody>
</table>

4.4.3 Computed Temperature and Velocity Sensitivity Analysis for Heated Tests

The VIPRE-calculated velocity and temperature sensitivity was tested with respect to optimized $f$-$K$ set selection and a ± 2 $\sigma$ variation in the turbulent diffusivity coefficient, $\beta$. The correlation sets used in the sensitivity analysis are summarized in Table 4.13. Reference single-phase test 4.2 operating conditions were used in VIPRE-I to perform the sensitivity analysis.

Table 4.13: Correlation Sets used for VIPRE-I Input in Temperature and Velocity Sensitivity Analysis for Heated Tests

<table>
<thead>
<tr>
<th>Correlation Set</th>
<th>1.1</th>
<th>2.1</th>
<th>3.2</th>
<th>3.1</th>
<th>3.2</th>
<th>3.3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bundle Friction</td>
<td>$0.184\times\text{Re}^{0.161}$</td>
<td>$0.184\times\text{Re}^{0.161}$</td>
<td>$0.280\times\text{Re}^{0.208}$</td>
<td>$0.280\times\text{Re}^{0.208}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Grid Loss Correlation</td>
<td>$2.813\times\text{Re}^{0.161}$</td>
<td>$2.813\times\text{Re}^{0.161}$</td>
<td>$0.768\times\text{Re}^{0.104}$</td>
<td>$0.768\times\text{Re}^{0.104}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{corner}}$</td>
<td>---</td>
<td>$2.658\times\text{Re}^{0.161}$</td>
<td>$0.732\times\text{Re}^{0.103}$</td>
<td>$0.732\times\text{Re}^{0.103}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{side}}$</td>
<td>---</td>
<td>$2.893\times\text{Re}^{0.161}$</td>
<td>$0.813\times\text{Re}^{0.105}$</td>
<td>$0.813\times\text{Re}^{0.105}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$k_{\text{inner}}$</td>
<td>---</td>
<td>$2.799\times\text{Re}^{0.161}$</td>
<td>$0.812\times\text{Re}^{0.108}$</td>
<td>$0.812\times\text{Re}^{0.108}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$C_T$</td>
<td>0.0000</td>
<td>0.0000</td>
<td>0.0048</td>
<td>0.0048</td>
<td>0.0048</td>
<td>0.0048</td>
</tr>
<tr>
<td>$\beta$</td>
<td>0.0100</td>
<td>0.0100</td>
<td>0.0095</td>
<td>0.0079</td>
<td>0.0095</td>
<td>0.0111</td>
</tr>
</tbody>
</table>

Results of the sensitivity analysis are given in Appendix B. The maximum variations in sub-channel average velocity and temperature for inner-type sub-channels are shown in Table 4.14. It is evident when comparing results to Table 4.11 that velocity was more sensitive to $f$-$K$ choice for the heated configuration of OMEGA SSG Test 4.2. The maximum sensitivity in velocity was actually experienced in channel 6 when changing $f$-$K$ correlation sets. As for the temperature profile variation, the greatest sensitivity was experienced in channel 6 when $\beta$ was varied by 2 $\sigma$.  


Table 4.14: Maximum Velocity and Temperature Sensitivities to Optimized $f$-$K$
Correlations and $\pm 2\sigma$ Variation of $\beta$ (Range Provided in Parentheses)

<table>
<thead>
<tr>
<th></th>
<th>Ch. 4</th>
<th>Ch. 5</th>
<th>Ch. 6</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>f-k Sensitivity (1.1 vs. 2.1 vs. 3.2)</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Temperature (°C)</td>
<td>-0.08 to 0.13 (0.21)</td>
<td>-0.02 to 0.06 (0.08)</td>
<td>0.02 to 0.07 (0.09)</td>
</tr>
<tr>
<td>Velocity</td>
<td>-0.18% to 0.52% (0.70%)</td>
<td>-0.32% to 0.83% (1.15%)</td>
<td>-0.77% to 0.49% (1.26%)</td>
</tr>
<tr>
<td><strong>β Sensitivity (3.1 vs. 3.2 vs. 3.3)</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Temperature (°C)</td>
<td>-0.09 to 0.13 (0.22)</td>
<td>-0.26 to 0.3 (0.56)</td>
<td>-0.54 to 0.62 (1.16)</td>
</tr>
<tr>
<td>Velocity</td>
<td>-0.02% to 0.03% (0.05%)</td>
<td>-0.07% to 0.06% (0.13%)</td>
<td>-0.13% to 0.12% (0.25%)</td>
</tr>
</tbody>
</table>
Chapter 5: OMEGA SSG Single-Phase Heat Transfer Analysis

Fifteen SSG single-phase tests were run, in all – the operating conditions were summarized in Table 3.3. All tests were run while maintaining an outlet pressure of 155 bars. Mass flux ranged from $3010 \text{ kg/m}^2\text{-sec}$ to $4560 \text{ kg/m}^2\text{-sec}$ and inlet temperatures ranged from $185.8 \degree C$ to $282.3 \degree C$. Consequently, bundle-average Reynolds number for the tests ranged from about 250,000 to 550,000. It is important to make note of the test chronology and of important events that had occurred during OMEGA SSG testing. The test chronology is depicted in Figure 5.1. It can be seen that two different thermocouple calibrations were used for obtaining single-phase rod surface temperature measurements. Details on the thermocouple calibration process could be found in Cubizolles (Cubizolles, 2007). It can also be seen that thermocouples had failed during testing and that the probe for heater rod 6 had become immobilized during single-phase testing due to bowing of rod 6.
Bundle geometry was actually checked before testing by measuring gap-gap distances between all rods for the selected axial elevations of -977 mm, -698 mm, -419 mm, -140 mm, and -5 mm. The first four axial locations were the grid span centers and the last one measured gap.

Figure 5.1: OMEGA SSG Test Chronology

<table>
<thead>
<tr>
<th>Run ID</th>
<th>Date</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Calibration 1</td>
<td>9</td>
<td>2006-27-28</td>
</tr>
<tr>
<td>Single Phase/Run 04-1</td>
<td>3</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 04-2</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 01</td>
<td>5</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 09</td>
<td>6</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 02</td>
<td>9</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 10</td>
<td>10</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 11</td>
<td>11</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 03-1</td>
<td>13</td>
<td></td>
</tr>
<tr>
<td>Calibration 2</td>
<td>27</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 03-2</td>
<td>2006-23-25-26</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 05</td>
<td>30</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 06</td>
<td>31</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 07</td>
<td>13</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 04-3</td>
<td>14</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 08</td>
<td>16</td>
<td></td>
</tr>
<tr>
<td>Calibration 3</td>
<td>7</td>
<td></td>
</tr>
<tr>
<td>Boiling tests/Test 1/</td>
<td>2</td>
<td>Probes 8 &amp; 9 were repaired</td>
</tr>
<tr>
<td>Cali{ation 4</td>
<td>3</td>
<td>Tests performed without probe 6</td>
</tr>
<tr>
<td>Boiling tests/Test 2/</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>Boiling tests/Test 3/</td>
<td>24</td>
<td></td>
</tr>
<tr>
<td>Boiling tests/Test 4/</td>
<td>27</td>
<td></td>
</tr>
<tr>
<td>Single Phase/Run 04-4</td>
<td>16</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-01</td>
<td>3</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-02</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-05</td>
<td>5</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-06-1</td>
<td>25</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-06-2</td>
<td>29</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-04</td>
<td>30</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 01-03</td>
<td>31</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-01</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-05-1</td>
<td>7</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-05-2</td>
<td>15</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-05-3</td>
<td>18</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-02</td>
<td>25</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-03</td>
<td>26</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 02-04</td>
<td>27</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 03-01</td>
<td>19</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 03-02</td>
<td>19</td>
<td></td>
</tr>
<tr>
<td>ONB/Run 03-03</td>
<td>29</td>
<td></td>
</tr>
</tbody>
</table>
spacing at the EOHL. After rod deformation caused the rod 6 thermocouple to jam, another set of measurements were made on the bundle geometry. Additional axial measurement levels were added at locations of -499 mm, -459 mm, -379 mm and -339 mm in grid span 2a. The bundle geometry was re-checked at the end of experimental testing. The post-rod 6 failure measurements revealed bundle-wide deformation, which is depicted in Figure 5.2 through Figure 5.5 for the four grid spans in the measurement section. The figures show gap measurement deviation from the initial measurements before the start of testing. The largest measured deformation occurred in span 2a and was almost 1.75 mm between rods 5 and 6 (Decossin, 2007). The effect of the bundle deformation will be seen in the following section on local HTC analysis.

![Figure 5.2](image_url)

Figure 5.2: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 2b
Figure 5.3: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 2a

Figure 5.4: Change in Gap Size (mm) just after Bundle Deformation Detection and at End of Testing for Grid Span 1b
There were two phases of single-phase heat transfer analysis – local heat transfer analysis and quadrant-averaged heat transfer analysis. The first phase involved directly calculating experimental HTCs for each rod surface temperature measurement that was made and then analyzing circumferential and axial behavior of local HTC for all instrumented rods. This analysis provided insights into the reliability of measured data and its applicability for further analysis. The second phase of heat transfer analysis involved averaging circumferential experimental HTCs at the same axial measurement level to yield a single experimental HTC per level. Insights of the first phase of analysis were used and single-phase heat transfer models were developed for the SSG geometry. These two phases of single-phase heat transfer analysis are described in the following sub-sections.
5.1 Local Single-Phase Heat Transfer Analysis

Of the fifteen single-phase tests, only 12 tests were used due to lack of measurement data in tests 3.1, 4.1 and 10. Local single-phase HTCs were determined using the measured outer-wall rod surface temperatures, associated local rod heat flux and code-predicted sub-channel temperature using Equation (5.1).

\[ h = \frac{q''(i, \theta)}{T_{wo} - T_F} \]  

(5.1)

It was necessary to calculate the local heat flux because of the eccentricity in the rod wall thickness, which caused the heat flux to vary around the circumference. The heat flux was calculated based on the local wall thickness, which was calculated as described in Section 2.3. The equation for calculating local wall heat flux is shown in Equation (5.2).

\[ q''(i, \theta) = q'''' \left( \frac{R_0^2 - (R_0 - e(i, \theta))^2}{2R_o} \right) \]  

(5.2)

Local heat transfer was analyzed as a ratio between experimental values and code-calculated values using the Dittus-Boelter correlation. There were four main insights of the local heat transfer analysis: 1) bundle transverse gradients were significant, causing considerable differences in experimental heat transfer behavior between the central rod and non-central rods, 2) the impact of the SSGs on the thermal hydraulic behavior of the flow was more significant than expected and prevented thermal hydraulic behavior representative of a bare bundle configuration from being realized in any section of the bundle, 3) the repeatability in heat transfer behavior between different grid spans was poor, most likely due to the previously discussed bundle deformation that occurred during testing, and 4) the circumferential evolution of the experimental HTC was chaotic and did not lend itself to correlation.
The experimental heat transfer behavior change between the central and non-central rods is shown using Figure 5.6 and Figure 5.7, which show the experimental-to-computed local heat transfer coefficient ratio plotted against axial location in the bundle. SSG locations are denoted with vertical dashed lines and all of the circumferential measurements taken around the rod are plotted, which accounts for the high level of scatter in the plots. Results are shown here for reference test 4.2. Results for all rods of test 4.2 are shown in Appendix D.

![Figure 5.6: Rod 5 Axial HTC Evolution for Test 4.2](image1)

![Figure 5.7: Rod 7 Axial HTC Evolution for Test 4.2](image2)

It can be seen that there was a greater amount of scatter for non-central corner rod 7 compared to central-rod 5. This was due to transverse gradients in the bundle which were likely caused by a combination of the unheated test section casing and the large peaking factor (1.3) between inner and outer heater rods. The experimental rod surface temperature measurements saw the effects of the gradient, but the code couldn’t since bulk properties were calculated for the entire sub-channel. This shortcoming caused by the transverse gradients is further exemplified with Figure 5.8, which shows the difference between experimental and predicted HTC for three different rods all facing the same sub-channel. The Dittus-Boelter prediction in VIPRE-I calculated a single HTC for sub-channel type 6, but due to gradients in the bundle, the
experimental HTC changed between the rods that were adjacent to the sub-channel. For this reason, it was determined that only central rod 5 should be used in further analysis.

Figure 5.8: Sub-Channel Type 6 HTC Ratio predicted using Adjacent Rods 1, 2 and 5

It can further be seen from Figure 5.6 that the impact of the SSG on heat transfer behavior was observed throughout the entire grid span. The test 4.2, rod 5 HTC ratio axial evolution is shown for only grid spans 1a and 1b to better show this behavior in Figure 5.9. The two grid spans are plotted against distance to upstream grid. Specifically, it appeared that there was a larger-than-expected upstream effect of the SSG on flow. Yao observed that the upstream effect of spacer grids was 2 X/D (Yao, 1982), whereas the impact of the NESTOR SSG downstream of grid span 1a was observed to be 6.5 X/D when all single-phase test data was analyzed. Figure 5.9 shows that the upstream impact seen for grid span 1b was even larger.
The SSG upstream impact may have been caused by the bundle deformation. It should be noted that the bundle deformation was less severe in grid span 1a when compared to the other three grid spans. Interestingly, gaps 10, 11 and 28, which represented gaps between the central rod and surrounding rods, experienced the greatest deformation in grid span 1b as evidenced by Figure 5.4. From Figure 5.9 it can also be seen that there was a distortion between the mean repeatability between results obtained for successive grid spans. The distortion was even more severe when comparing grid span 1a to 2a and 2b. Figure 5.10 shows a comparison of grid spans 1a and 2b results.
Figure 5.10: Test 4.2 Local HTC Ratio Axial Evolution for Grid Spans 1a and 2b

The unusual behavior of Figure 5.10 was also seen in Figure 5.6, which shows the axial evolution of HTC for the entire measurement length of rod 5. The repeatability issues that were seen between grid spans were also likely caused by the bundle deformation. Due to these distortions, it was deemed appropriate to only perform further analysis on span 1a data. Grid span 1a data measurements were considered acceptable, but it should be mentioned that the upstream behavior of the SSG was not physical. The repeatability distortion was witnessed for all single-phase tests – results are shown in Appendix D.

It was initially envisioned that it may be possible to develop a correlation for rod circumferential HTC behavior; however, local HTC analysis around the rod circumference revealed an erratic behavior which did not lend itself to correlation. Figure 5.11 and Figure 5.12 show the HTC ratio evolution around the rod circumference for Rods 2 and 5 at selected axial elevations in grid spans 1a and 1b. All experimental results were combined into a single 0-45° sector of the rod based on 1/8th symmetry. This insight revealed that further single-phase heat
transfer analysis should be performed using average experimental HTC values, which combines measurements from the entire circumference of the rod.

Figure 5.11: Rod 2 Circumferential HTC Ratio Evolution for Test 4.2

Figure 5.12: Rod 5 Circumferential HTC Ratio Evolution for Test 4.2
A final insight of the local heat transfer analysis pertained to the single-phase heat transfer model development. To test the validity of the Dittus-Boelter equation, the experimental-to-calculated HTC ratios were plotted against associated local Reynolds number. The local Reynolds number was calculated by VIPRE-I and was actually only available on a per-sub-channel basis. Results for all 12 single-phase tests are shown in Figure 5.13.

![Figure 5.13: Local HTC Ratio with respect to Code-Calculated Local Re for All Single-Phase Tests](image)

Using a linear trend-line, it was seen that there was only a slight decreasing dependency of the HTC ratio on local Reynolds number. The mean value of the HTC ratio was $0.9946 \pm 0.0390$ with data scatter ranging from -8 % to 15 %. To further investigate this behavior, a separate plot was made where experimental results were separated with respect to their axial measurement location. These results are shown in Figure 5.14.
Figure 5.14: Local HTC Ratio Segregated by Axial Measurement Location with respect to Local Reynolds number for all Single-Phase Tests

It is clear from this figure that measurements taken close to the grid (35 mm and 70 mm) were subject to grid effects that Dittus-Boelter does not model. As should be expected, code-calculations were under-predicted compared to measurements. On the other hand, for measurements further downstream, Dittus-Boelter slightly over-predicted the single-phase heat transfer by 2-3%. Despite this small over-prediction, there was practically no trend in the HTC ratio with Re for measurement locations from 70 mm to 244 mm. The trend seen for the most upstream measurements (35 mm) should be expected since flow blockages have a decreasing impact on flow as Re increases.

The exponent of the trend lines for the other data sets averaged out to -0.0006. Considering that the standard deviation of the data was larger than this value (σ=0.00678), it appeared that no correction to the Dittus-Boelter equation was necessary for the SSG test setup.
5.2 Circumferentially-Averaged Heat Transfer Analysis

The second phase of the single-phase heat transfer analysis incorporated previous insights and focused on the single-phase heat transfer model development, which would then be applied for future ONB analysis. Average experimental HTCs were obtained by using rod quadrant-averaged temperatures to calculate the rod-quadrant averaged HTCs. The quadrant-averaged temperatures were obtained using a trapezoidal averaging process shown in Equation (5.3). The quadrant-averaged temperatures were then used in Equation (5.1) to determine the associated quadrant averaged HTCs – as a note, the quadrant heat flux was used based on the average wall thickness in the quadrant. To obtain a single experimental HTC per axial level, the four resultant quadrant-averaged HTC values were arithmetically averaged together.

\[
\bar{T} = \frac{1}{\theta_N - \theta_1} \left( \frac{1}{N} \sum_{i=1}^{N-1} \frac{T_i + T_{i+1}}{2} (\theta_{i+1} - \theta_i) \right)
\]  

(5.3)

The axial evolution of the averaged HTC ratio is shown for all tests in Figure 5.15.

Figure 5.15: Axial Evolution of Averaged HTC Ratios for All Single-Phase Tests
Compared to the local HTC ratios, the scatter in the data has been reduced through the averaging process. An exponential curve fit was used to quantify the differences between predicted HTCs and enhanced experimental HTCs caused by the SSG. The non-physical rise in experimental HTC upstream of the grid was modeled with a linear trend-line. While the rise in the HTC ratio was considered to be non-physical, it was necessary to model the upstream effects seen during testing to improve code-calculated heat transfer results for ONB analysis. Because of the repeatability concerns and lack of a bare-bundle region, the grid-enhancement correlation developed would be dedicated to the SSG configuration and not applicable to other bundle configurations. Figure 5.16 shows the averaged HTC evolution downstream and upstream of the SSG. The ratios were obtained from span 1a, but end-of-span results that were affected by the downstream grid were moved to the left side of the figure. For comparison, Figure 5.17 shows grid enhancement data obtained from Yao (Yao, 1982).

![Graph showing HTC ratio evolution downstream and upstream of the SSG for all single-phase tests.](image)

**Figure 5.16:** Averaged HTC Ratio Evolution Downstream and Upstream of the SSG for All Single-Phase Tests
In order to generate an exponential curve fit, the data was normalized to the lowest measured-to-predicted ratio of all tests, which was 0.9512. It can be seen that the exponential curve fit of the experimental data was significantly different than the correlation provided by Yao (Yao, 1982). As previously mentioned, Yao only observed an upstream grid effect of 2 X/D and also, the discontinuity between HTC behavior upstream and downstream of the SSG that was seen in Figure 5.16 was not seen in Figure 5.17. The functions that would be applied to NESTOR data to correct for grid enhancement effects are shown in Equation (5.4). Application of the grid enhancement functions lead to the corrected experimental-to-predicted HTC ratio evolution shown in Figure 5.18.

\[
G \left( \frac{Z}{D} \right) = \begin{cases} 
0.9512 \left( 1 + 0.1419 e^{-\frac{0.165 Z}{D}} \right) & \text{if } \frac{Z}{D} \leq 16.01 \\
0.9512 \left( 1 + 0.0027 \times \frac{Z}{D} - 0.0331 \right) & \text{if } \frac{Z}{D} > 16.0 
\end{cases}
\] 

(5.4)
In addition to the exponential curve fit, a polynomial curve fit was also utilized to stay consistent with NESTOR partners. The polynomial curve fit is shown in Figure 5.19 for all single-phase heat transfer results. The resulting corrections to the heat transfer ratio are shown in Figure 5.20.
Prior to use of the grid enhancement factors, average HTC ratios ranged from 0.95 to 1.1. This scatter was reduced to 0.98 to 1.06 using the exponential fit and to 0.97 to 1.04 by applying the second order fit. Previously, the mean of the HTC ratios was 0.99 with a standard deviation
of 0.0362. Application of the second order fit caused the mean to rise to 0.9971 and the standard deviation to reduce to 0.0158. Application of the exponential fit caused the mean to rise to 1.003 and the standard deviation to reduce to 0.1647. To better show the effect of applying the grid heat transfer enhancement correction factor (polynomial), the difference between calculated and measured rod surface temperatures for all single-phase tests are shown in Figure 5.21 and Figure 5.22. It can be seen from the figures that the variation from measured outer wall rod temperatures was reduced from (+ 2 \, K, -1.3 \, K) to ± 0.8 \, K. The results of Figure 5.21 and Figure 5.22 are more relevant because the calculated wall superheat will be used in ONB analysis for assessing the ONB criterion.

Figure 5.21: Difference between Predicted and Measured Rod Surface Temperatures for All Single-Phase Tests without Grid Heat Transfer Enhancement Correction Factor
Figure 5.22: Difference between Predicted and Measured Rod Surface Temperatures for All Single-Phase Tests with Grid Heat Transfer Enhancement Correction Factor
Chapter 6: SSG ONB Analysis

A total of 17 ONB tests were performed using the NESTOR SSG setup. ONB was achieved using the higher rod powers and inlet temperatures presented in Table 3.4. Important insights of the single-phase heat transfer tests showed that bundle deformation affected results in the upper three grid spans and that strong traverse gradients caused large circumferential variation in fluid properties on non-central heater rods. Additionally, grid enhancement factors were developed to account for SSG effects on the flow and HTC. These insights and developments were considered in the ONB analysis – tests were used for analysis only if ONB occurred in grid span 1a and only Rod 5 was analyzed.

The analysis was performed by first determining experimental ONB location for appropriate tests along with the experimental local ONB wall superheat. Assessment of the ONB criterion was performed by comparing the experimental and computed wall superheat at the experimental ONB location with and without the grid enhancement functions developed in the previous chapter.

6.1 Experimental ONB Location and Wall Superheat Determination

Experimental ONB location was determined based on visual inspection of the axial surface temperature profile of rod 5. Due to the scatter in the rod outside temperature, the averaged values were used as described in Section 5.2 to make the determination more straightforward. The axial temperature profile obtained from rod 5 is shown for all tests in Appendix E. It was observed that ONB occurred in span 1a for only 5 of the 17 (tests 1.6-1, 1.6-2, 2.5-1, 2.5-2, and 2.5-3) tests and so ONB analysis was only performed on those 5 tests based
on the insights of Chapter 5. Upon determination of the ONB location, the local ONB experimental wall superheat was calculated by subtracting the local saturation temperature at ONB from the local rod-outside wall temperature.

The local saturation temperature was calculated using local pressure in the vicinity of ONB along with the NIST steam tables (National Institute of Standards and Technology, Chemical Science and Technology Laboratory, 2008). The local pressure was determined using the outlet pressure measured during the experiments plus the EOHL pressure drop for the given test (see Section 2.5) and the pressure drop in the bundle from the ONB point to EOHL. VIPRE-I was used to calculate the pressure drop from the ONB location to the EOHL. Since gauge pressure was given in experimental results, atmospheric pressure was also added to obtain the absolute local pressure. The calculation of local pressure for use in determining saturation pressure is shown in Equation (6.1).

\[
P_{\text{loc}} = P_{\text{outlet}} + P_{\text{atm}} + \Delta P_{\text{EOHL}} + \Delta P_{\text{bundle}}
\]  

(6.1)

Experimental ONB locations, as well as corresponding wall superheats are summarized for all SSG ONB tests in Table 6.1. The three NESTOR partners each determined ONB location and wall superheat independently. After comparing results, it was found that there was a \(\pm 40 \text{ mm}\) uncertainty in the experimental ONB location and a \(\pm 0.3 \text{ K}\) uncertainty in the experimental ONB location wall superheat due to the subjectivity of the visual inspection process for determination of ONB.
Table 6.1: Summary of Experimental ONB Locations and Corresponding Wall Superheat
for SSG ONB Tests

<table>
<thead>
<tr>
<th>ONB Test Number</th>
<th>( Z_{\text{ONB}}-Z_{\text{EOHL}} ) [m]</th>
<th>( P_{\text{loc}} ) [bar]</th>
<th>( T_{\text{sat}} ) [°C]</th>
<th>( T_w ) [°C]</th>
<th>( T_w-T_{\text{ONB,sat}} ) [°C]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>-0.183</td>
<td>156.24</td>
<td>345.43</td>
<td>345.95</td>
<td>0.52</td>
</tr>
<tr>
<td>1.2</td>
<td>-0.382</td>
<td>156.41</td>
<td>345.51</td>
<td>345.96</td>
<td>0.45</td>
</tr>
<tr>
<td>1.3</td>
<td>-0.482</td>
<td>156.43</td>
<td>345.52</td>
<td>346.33</td>
<td>0.80</td>
</tr>
<tr>
<td>1.4</td>
<td>-0.694</td>
<td>156.5</td>
<td>345.57</td>
<td>345.77</td>
<td>0.20</td>
</tr>
<tr>
<td>1.5</td>
<td>-0.644</td>
<td>156.5</td>
<td>345.56</td>
<td>345.76</td>
<td>0.20</td>
</tr>
<tr>
<td>1.6-1</td>
<td>-0.840</td>
<td>156.55</td>
<td>345.60</td>
<td>346.38</td>
<td>0.78</td>
</tr>
<tr>
<td>1.6-2</td>
<td>-0.868</td>
<td>156.56</td>
<td>345.60</td>
<td>346.44</td>
<td>0.84</td>
</tr>
<tr>
<td>2.1</td>
<td>-0.084</td>
<td>156.37</td>
<td>345.49</td>
<td>345.79</td>
<td>0.30</td>
</tr>
<tr>
<td>2.2</td>
<td>-0.333</td>
<td>156.48</td>
<td>345.56</td>
<td>345.53</td>
<td>-0.03</td>
</tr>
<tr>
<td>2.3</td>
<td>-0.432</td>
<td>156.52</td>
<td>345.58</td>
<td>345.96</td>
<td>0.38</td>
</tr>
<tr>
<td>2.4</td>
<td>-0.694</td>
<td>156.64</td>
<td>345.64</td>
<td>346.14</td>
<td>0.50</td>
</tr>
<tr>
<td>2.5-1</td>
<td>-1.042</td>
<td>156.88</td>
<td>345.76</td>
<td>346.49</td>
<td>0.73</td>
</tr>
<tr>
<td>2.5-2</td>
<td>-0.968</td>
<td>156.87</td>
<td>345.75</td>
<td>346.52</td>
<td>0.77</td>
</tr>
<tr>
<td>2.5-3</td>
<td>-0.968</td>
<td>156.87</td>
<td>345.75</td>
<td>346.46</td>
<td>0.71</td>
</tr>
<tr>
<td>3.1</td>
<td>-0.457</td>
<td>156.42</td>
<td>345.52</td>
<td>345.89</td>
<td>0.37</td>
</tr>
<tr>
<td>3.2</td>
<td>-0.669</td>
<td>156.49</td>
<td>345.56</td>
<td>345.76</td>
<td>0.2</td>
</tr>
<tr>
<td>3.3</td>
<td>-0.693</td>
<td>156.5</td>
<td>345.57</td>
<td>346.08</td>
<td>0.51</td>
</tr>
</tbody>
</table>

6.2 Assessment of VIPRE-Computed Wall Superheat at ONB

The VIPRE-I simulations were performed such that the code was forced to assume
single-phase heat transfer throughout the entire bundle. The turbulent diffusivity coefficient of
the turbulent mixing model was calculated using the correlation developed during the turbulent
conductivity optimization (see 0). The grid heat transfer enhancement functions developed in
Section 5.2 were applied to the VIPRE-I results to correct computed rod-outside wall temperature
for the SSG effects. Results were also generated without the use of the grid enhancement
functions for comparison’s sake. Table 6.2 shows the results of code-calculated wall superheat
both with and without the grid enhancement correction function applied. Figure 6.1 shows a
graphical representation of the computed and experimental ONB locations for the 5 relevant ONB
tests.
Table 6.2: Comparison of Experimental and Computed ONB Wall Superheat with and without Grid Enhancement Corrections

<table>
<thead>
<tr>
<th>Run</th>
<th>ONB$_{exp}$ (m)</th>
<th>$T_{sat}$ (°C)</th>
<th>$T_{w,exp} - T_{sat}$ (°C) [Exp]</th>
<th>$T_{w,calc} - T_{ONB,sat}$ [°C]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>No G(z)</td>
<td>Polynomial G(z)</td>
</tr>
<tr>
<td>1.6-1</td>
<td>-0.843</td>
<td>345.6</td>
<td>0.78</td>
<td>2.79</td>
</tr>
<tr>
<td>1.6-2</td>
<td>-0.868</td>
<td>345.6</td>
<td>0.84</td>
<td>2.66</td>
</tr>
<tr>
<td>2.5-1</td>
<td>-1.042</td>
<td>345.76</td>
<td>0.73</td>
<td>2.92</td>
</tr>
<tr>
<td>2.5-2</td>
<td>-0.968</td>
<td>345.75</td>
<td>0.77</td>
<td>3.12</td>
</tr>
<tr>
<td>2.5-3</td>
<td>-0.968</td>
<td>345.75</td>
<td>0.71</td>
<td>3.34</td>
</tr>
</tbody>
</table>

Figure 6.1: Experimental and Computed Wall Superheats at Experimental ONB

From Figure 6.1, it is clear that the grid enhancement functions made little difference in the wall superheat prediction when compared with the magnitude of the VIPRE-I over-prediction of ONB wall superheat. The best results were actually obtained without the application of the grid enhancement functions for tests where ONB occurred further than 0.1 m from the upstream grid. The reason for this behavior can be seen by comparing the HTC ratio behavior for the 5 ONB tests analyzed both with and without the grid enhancement functions. The behavior change is shown in Figure 6.2 and Figure 6.3 for the polynomial fit and the exponential fit, respectively.
From 0.0 to 0.1 m, the grid enhancement functions acted to correct the under-prediction of the HTC, but after 0.1 m, the functions actually made predictions worse. This was due to the
fact that the grid enhancement functions followed the trend of single-phase HTC data, which
showed an overprediction from 0.1 m to the end-of-span. Basically, inapplicability of the grid
enhancement functions was caused by a change in heat transfer behavior between the single-
phase and ONB tests. Figure 6.4 was generated by EDF to demonstrate the differences between
experimental and calculated rod surface temperatures in grid span 1a. Calculations were
performed with THYC and the EDF grid enhancement correction function was applied. While
single-phase test data (shown with hollow triangles) was corrected to within 0.8 K, the ONB
results still showed a large amount of error. It’s interesting to note that the largest error between
predicted and measured results were in the center of the grid where the grid enhancement
function had very little effect. This fact demonstrates that there was an overall change in the
single-phase heat transfer behavior changed between single-phase and ONB tests. The cause for
this change remains unexplained.

Figure 6.4: Difference between Experimental and Predicted Rod Surface Temperature
with EDF-Produced Grid Enhancement Function Applied for Single-Phase and ONB Test
Runs
The ONB wall superheat prediction results are shown for the three NESTOR partners in Figure 6.5. Additionally, VIPRE-I results are shown using both the PSU-determined ONB locations and the EDF-determined ONB locations to test the ONB determination uncertainty effect on ONB wall superheat prediction.

![Graph showing predicted and experimental wall superheat at experimental ONB location calculated by EDF, CEA and PSU.]

Figure 6.5: Predicted and Experimental Wall Superheat at Experimental ONB Location as Calculated by EDF, CEA and PSU

It can be seen that calculations performed with each of the three codes resulted in over-predictions in wall superheat which was in the range of 1–3.5 K. THYC appeared to be the most accurate code in terms of predicting ONB wall superheat; however, an investigation revealed that it was actually the EDF-developed single-phase heat transfer model that was responsible for the improved results. Figure 6.6 shows comparisons between THYC and VIPRE-I predictions of span 1a wall superheat for ONB test 1.6-1 both with and without the EDF and PSU single-phase heat transfer models. Hollow squares show results obtained from the codes with the unmodified Dittus-Boelter equation. Solid squares show results using the PSU and EDF optimized models. It can be seen that results are fairly consistent for the unmodified code results, but become quite
different with the application of the single-phase heat transfer models. Results were similar for test 2.5-2, as shown in Figure 6.7. More detailed comparisons were made between THYC and VIPRE-I results to ensure proper data handling and to analyze causes for differences in code predictions. Results of the analysis can be found in Appendix F.

![Figure 6.6: Computed and Experimental Wall Superheat using VIPRE-I and THYC and PSU and EDF Single-Phase Heat Transfer Models for Test 1.6-1](image)
The current VIPRE-I ONB criterion is 1 °F wall superheat, which is 0.56 K. When rod surface temperature grows beyond that temperature, the code chooses between the single-phase or sub-cooled nucleate boiling heat transfer correlations, whichever produces the larger HTC. From experimental results, it would appear that the 1 °F criterion is too low, but because of the unexplained change in heat transfer behavior between single-phase and ONB tests, it’s not possible to recommend a reliable modification to the current criterion. To put the wall superheat uncertainty into perspective, for a PWR with an average core temperature rise of 35 K, a 1-3.5 K uncertainty in wall superheat will lead to uncertainty in ONB location ranging from 10-37 cm. VIPRE-I’s uncertainty was on the order of 2-2.5 K, which would result in uncertainty in ONB location ranging from 21-26 cm.
Chapter 7: Summary and Conclusions

The purpose of the NESTOR program was to aid in the development of an Axial Offset Anomaly for PWR cores by assessing/developing single-phase heat transfer models and wall-superheat based ONB criterion. Experimental tests were performed on bare-bundle and Mixing Vane Grid 5x5 rod bundle configurations. Pressure drop and velocity measurements were made in unheated tests (MANIVEL loop) while rod surface temperature and End of Heated Length temperature measurements were made in heated tests (OMEGA loop). Simple Support Grids were implemented in both bundle configurations to prevent rod bow caused by the electrical-induced magnetic forces in the bundles. The two different bundle configurations were dubbed SSG and MVG.

All experimental testing has been completed and data analysis has been performed by three partners: EDF, CEA and PSU, each using a separate sub-channel code for simulating experiments. This thesis documents the data analysis performed by PSU using VIPRE-I for the SSG configuration results. The three sub-channel codes – THYC-COUER (EDF), FLICA-IV (CEA) and VIPRE-I (PSU) – were calibrated to the SSG configuration bundles by developing optimized friction and grid loss coefficient correlations. Friction and grid loss coefficients were developed from MANIVEL SSG pressure loss data. Optimized turbulent mixing coefficients were optimized for each of the three friction and grid loss correlation sets using MANIVEL SSG velocity measurements and OMEGA SSG End of Heated Length measurements. Uncertainty in the code-calculated velocity resulting from use of the three different optimized friction and grid loss coefficient sets was $\pm \ 2.5 \%$ and pressure prediction uncertainty was $\pm \ 1.2 \ mbar$. A $10 \%$ variation in the optimized turbulent diffusivity coefficient resulted in a $0.35 \ K$ variation in sub-channel temperature. It was determined that the use of different optimized grid and friction loss correlation sets in the three codes resulted in sub-channel temperature prediction differences of
about 0.5 $K$. An additional difference of 0.5 $K$ in sub-channel temperature prediction resulted from steam table differences in the codes.

The calibrated sub-channel codes were used to perform analysis on single-phase heated tests performed in the OMEGA loop. Experimental Heat Transfer Coefficients were obtained by using the rod heat flux, measured outside-wall temperature and code-predicted sub-channel temperature. A preliminary analysis of local Heat Transfer Coefficients revealed that transverse gradients in the bundle precluded the use of non-central heater rods for further data analysis. Furthermore, there was a distortion in mean repeatability between successive grid span heat transfer behavior, which was attributed to a measured bundle deformation that occurred during testing. Span 1a results were deemed acceptable for further analysis, but a non-physical impact on experimental Heat Transfer Coefficient was observed upstream of the SSG located at the end of the grid span. A second stage of heat transfer analysis used averaged Heat Transfer Coefficients for development of grid heat transfer enhancement functions. An exponential curve-fit was applied to the experimental-to-Dittus-Boelter predicted Heat Transfer Coefficient ratio to capture grid impact on single-phase heat transfer, but a linear trend-line was also added to account for upstream SSG impact. A second order polynomial fit was also used to capture the effects. Both enhancement functions improved outer-wall temperature calculations by reducing differences between predicted and measure temperatures from (2.5 $K$, -1.3 $K$) to (0.8 $K$, -0.8 $K$), but due to the non-physical upstream SSG effects and mean repeatability distortion, the grid enhancement functions were only applicable to the SSG configuration and cannot reasonably be extended to actual PWR fuel bundles.

Analysis was performed on the ONB tests using insights of the single-phase test analysis. Five tests, in which ONB occurred in grid span 1a, were analyzed by determining experimental ONB location and associated wall superheat. Experimental ONB was determined by visual
inspection of the axial temperature profile and finding where a slope change occurred due to
initiation of sub-cooled boiling. The three NESTOR partners determined ONB independently
and it was found that there was an uncertainty of $\pm 40\ mm$ in location determination and $\pm 0.3\ K$
in wall superheat determination. Experimental wall superheats exhibited a significant amount of
scatter ranging from 0.1 to 1 $K$. Sub-channel codes were used to predicted wall superheat at
experimental ONB location by forcing the codes to assume single-phase heat transfer in the entire
bundle. Results showed that all codes tested over-predicted experimental wall superheat values
by 1-3.5 $K$. This over-prediction resulted from significantly under-predicted Heat Transfer
Coefficients despite the use of the dedicated single-phase heat transfer models. By comparing
differences between predicted and experimental rod temperatures for single-phase and ONB tests,
it was found that the heat transfer behavior had changed from single-phase to ONB tests. The
cause for the change remains unexplained and so it is impossible to reliably modify the current
ONB criterion used in the sub-channel analysis codes. The VIPRE-I over-prediction of wall
superheat was within the range of 2-2.5 $K$, which corresponds to an uncertainty in ONB location
of 21-26 $cm$ for typical PWR operating conditions.

This thesis documented the results of analysis performed on SSG configuration test
results, but further analysis is presently being performed on data from the MVG configuration.
The analysis will proceed in a similar fashion as it had for the SSG configuration. As was
expected, the MVG impact on the enhancement of heat transfer was several magnitudes larger
than that observed for the SSG-type grids. The bundle-deformation effects were much less
significant in the MVG configuration – having little effect on the mean repeatability between grid
spans; however, the upstream impact of the mixing vane grids was still significant and no regions
free of grid effects were observed. Despite these minor issues, analysis of the MVG
configuration should provide insight into the accuracy of the current wall-superheat ONB
criterion for bundle configurations representative of industrial PWR bundles. MVG analysis is anticipated to be completed mid-2010, at which time a final report on all analysis results will be submitted to the Electric Power Research Institute.
References


Miller, P. B. (1956). Heat Transfer to Water Flowing Parallel to a Rod Bundle. 2.


Appendix A: Turbulent Viscosity Optimization

The turbulent viscosity optimization (\(C_f\beta\) parameter) was performed using MANIVEL Phase 3 velocity measurement data. An optimization factor (\(D_c\)) was calculated for inner sub-channels at each axial measurement level using the three \(f-K\) correlation sets described in Section 4.2. Sub-channel velocities calculated by triangular, arithmetic, and Voronoï averaging techniques were analyzed. Graphs showing evolution of the \(D_c\) parameter with respect to the value of \(C_f\beta\) are presented in Figure A.1 through Figure A.9.
Figure A.1: $C_f\beta$ Optimization for Triangular-Averaged Data using $f-K$ set 1

Figure A.2: $C_f\beta$ Optimization for Arithmetic-Averaged Data using $f-K$ set 1

Figure A.3: $D_C C_f\beta$ Optimization for Voronoï-Averaged Data using $f-K$ set 1

Figure A.4: $D_C C_f\beta$ Optimization for Triangular-Averaged Data using $f-K$ set 2
Figure A.5: $C_\beta$ Optimization for Arithmetic-Averaged Data using $f-K$ set 2

Figure A.6: $C_\beta$ Optimization for Voronoï-Averaged Data using $f-K$ set 2

Figure A.7: $C_\beta$ Optimization for Triangular-Averaged Data using $f-K$ set 3

Figure A.8: $D_c C_\beta$ Optimization for Arithmetic-Averaged Data using $f-K$ set 3
Figure A.9: $C_f\beta$ Optimization for Voronoï-Averaged Data using $f$-$K$ set 3
Appendix B: Velocity and Temperature Sensitivity Analysis

Results from the velocity and temperature profile sensitivity analysis are shown in this appendix. Table 4.10 provided the input parameters used for the velocity sensitivity analysis in the unheated tests. Velocity sensitivity was tested with respect to choice of optimized $f$-$K$ set and also with respect to the turbulent viscosity parameter by varying $C_T\beta$ by ± 1 σ. Figure B.1 through Figure B.3 show VIPRE-I-calculated velocity sensitivity to optimized $f$-$K$ sets and Figure B.4 through Figure B.6 show VIPRE-I-calculated velocity sensitivity to the turbulent viscosity parameter. The velocity sensitivity analysis was performed for inner sub-channels using the MANIVEL SSG Phase 3 operating conditions.

A velocity sensitivity was also performed for the heated test configuration using the operating conditions of the reference test, single-phase test 4.2. Figure B.7 through Figure B.9 show the velocity sensitivity to the optimized $f$-$K$ sets for inner sub-channel types and Figure B.10 through Figure B.12 shows the velocity sensitivity to a ± 2 σ variation in $\beta$.

The bulk sub-channel temperature sensitivity to optimized $f$-$K$ set and a ± 2 σ variation in $\beta$ was finally tested. Results are shown for inner sub-channel types in Figure B.13 through Figure B.18.
Figure B.1: Channel 4 Velocity Sensitivity to Optimized $f$-$k$ Set for Unheated MANIVEL Phase 3 Test

Figure B.2: Channel 5 Velocity Sensitivity to Optimized $f$-$k$ Set for Unheated MANIVEL Phase 3 Test

Figure B.3: Channel 6 Velocity Sensitivity to Optimized $f$-$k$ Set for Unheated MANIVEL Phase 3 Test

Figure B.4: Channel 4 Velocity Sensitivity to $\pm 1\sigma$ in $C_T\beta$ for Unheated MANIVEL Phase 3 Test
Figure B.5: Channel 5 Velocity Sensitivity to $\pm 1 \sigma$ in $C_T\beta$ for Unheated MANIVEL Phase 3 Test

Figure B.6: Channel 6 Velocity Sensitivity to $\pm 1 \sigma$ in $C_T\beta$ for Unheated MANIVEL Phase 3 Test
Figure B.7: Channel 4 Velocity Sensitivity to Optimized f-K Set for Heated Single-Phase Test 4.2 Conditions

Figure B.8: Channel 5 Velocity Sensitivity to Optimized f-K Set for Heated Single-Phase Test 4.2 Conditions

Figure B.9: Channel 6 Velocity Sensitivity to Optimized f-K Set for Heated Single-Phase Test 4.2 Conditions

Figure B.10: Channel 4 Velocity Sensitivity to $\pm 2\sigma$ in $\beta$ for Heated Single-Phase Test 4.2 Conditions
Figure B.11: Channel 5 Velocity Sensitivity to ± 2 σ in β for Heated Single-Phase Test 4.2 Conditions

Figure B.12: Channel 6 Velocity Sensitivity to ± 2 σ in β for Heated Single-Phase Test 4.2 Conditions
Figure B.13: Channel 4 Sub-Channel Temperature Sensitivity to Optimized $f$-$K$ Set for Heated Single-Phase Test 4.2 Conditions

Figure B.14: Channel 5 Sub-Channel Temperature Sensitivity to Optimized $f$-$K$ Set for Heated Single-Phase Test 4.2 Conditions

Figure B.15: Channel 6 Sub-Channel Temperature Sensitivity to Optimized $f$-$K$ Set for Heated Single-Phase Test 4.2 Conditions

Figure B.16: Channel 4 Sub-Channel Temperature Sensitivity to $\pm 2 \sigma$ in $\beta$ for Heated Single-Phase Test 4.2 Conditions
Figure B.17: Channel 5 Sub-Channel Temperature Sensitivity to $\pm 2 \sigma$ in $\beta$ for Heated Single-Phase Test 4.2 Conditions

Figure B.18: Channel 6 Sub-Channel Temperature Sensitivity to $\pm 2 \sigma$ in $\beta$ for Heated Single-Phase Test 4.2 Conditions
Appendix C: VIPRE-I Computed Velocity for MANIVEL Phase 3

The VIPRE-I-predicted axial velocity profile for the MANIVEL SSG Phase 3 simulation is shown in this appendix. Predicted and measured velocity profiles are shown on the same plot for each of the six sub-channel types. The axial velocity, as calculated with the three optimized $f$-$K$ sets shown in Table 4.3, is presented for the six sub-channel types in Figure C.1 through Figure C.6. The axial velocity calculated with $f$-$K$ set 3 and a $\pm 1 \sigma$ variation in the $C_T \beta$ parameter is shown for all six sub-channels types in Figure C.7 through Figure C.12.
Figure C.1: Channel 1 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets

Figure C.2: Channel 2 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets

Figure C.3: Channel 3 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets

Figure C.4: Channel 4 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets
Figure C.5: Channel 5 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets

Figure C.6: Channel 6 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test using Optimized $f$-$K$ Sets

Figure C.7: Channel 1 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with $\pm 1 \sigma$ Variation of $C_T\beta$

Figure C.8: Channel 2 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with $\pm 1 \sigma$ Variation of $C_T\beta$
Figure C.9: Channel 3 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with ± 1 σ Variation of $C_T \beta$

Figure C.10: Channel 4 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with ± 1 σ Variation of $C_T \beta$

Figure C.11: Channel 5 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with ± 1 σ Variation of $C_T \beta$

Figure C.12: Channel 6 Predicted and Measured Velocity Profiles for MANIVEL SSG Phase 3 Test with ± 1 σ Variation of $C_T \beta$
Appendix D: Local Single-Phase Heat Transfer Results

Local single-phase heat transfer analysis results are shown in this appendix. Figure D.1 to Figure D.8 shows the axial evolution of the HTC ratio obtained from all instrumented rods during test run 4.2. Figure D.9 to Figure D.23 shows the axial evolution of local HTC ratio obtained from the central instrumented rod for all single-phase tests.
Figure D.1: Rod 1 Axial HTC Evolution for Test 4.2

Figure D.2: Rod 2 Axial HTC Evolution for Test 4.2

Figure D.3: Rod 3 Axial HTC Evolution for Test 4.2

Figure D.4: Rod 4 Axial HTC Evolution for Test 4.2
Figure D.5: Rod 5 Axial HTC Evolution for Test 4.2

Figure D.6: Rod 7 Axial HTC Evolution for Test 4.2

Figure D.7: Rod 8 Axial HTC Evolution for Test 4.2

Figure D.8: Rod 9 Axial HTC Evolution for Test 4.2
Figure D.9: Axial Evolution of Local HTC for Test 1

Figure D.10: Axial Evolution of Local HTC for Test 2

Figure D.11: Axial Evolution of Local HTC for Test 3.1

Figure D.12: Axial Evolution of Local HTC for Test 3.2
Figure D.17: Axial Evolution of Local HTC for Test 5

Figure D.18: Axial Evolution of Local HTC for Test 6

Figure D.19: Axial Evolution of Local HTC for Test 7

Figure D.20: Axial Evolution of Local HTC for Test 8
Figure D.21: Axial Evolution of Local HTC for Test 9

Figure D.22: Axial Evolution of Local HTC for Test 10

Figure D.23: Axial Evolution of Local HTC for Test 11
Appendix E: ONB Test Experimental Axial Temperature Profiles

The average temperature axial profile is shown for rod 5 of all 17 ONB tests performed in the OMEGA SSG facility. The saturation temperature based on the EOHL outlet pressure of each test is also shown as a horizontal dashed line. Figure E.1 through Figure E.17 show the temperature profiles.
Figure E.1: Rod 5 Axial Temperature Profile for ONB Run 1.1

Figure E.2: Rod 5 Axial Temperature Profile for ONB Run 1.2

Figure E.3: Rod 5 Axial Temperature Profile for ONB Run 1.3

Figure E.4: Rod 5 Axial Temperature Profile for ONB Run 1.4
Figure E.5: Rod 5 Axial Temperature Profile for ONB Run 1.5

Figure E.6: Rod 5 Axial Temperature Profile for ONB Run 1.6-1

Figure E.7: Rod 5 Axial Temperature Profile for ONB Run 1.6-2

Figure E.8: Rod 5 Axial Temperature Profile for ONB Run 2.1
Figure E.9: Rod 5 Axial Temperature Profile for ONB Run 2.2

Figure E.10: Rod 5 Axial Temperature Profile for ONB Run 2.3

Figure E.11: Rod 5 Axial Temperature Profile for ONB Run 2.4

Figure E.12: Rod 5 Axial Temperature Profile for ONB Run 2.5-1
Figure E.13: Rod 5 Axial Temperature Profile for ONB Run 2.5-2

Figure E.14: Rod 5 Axial Temperature Profile for ONB Run 2.5-3

Figure E.15: Rod 5 Axial Temperature Profile for ONB Run 3.1

Figure E.16: Rod 5 Axial Temperature Profile for ONB Run 3.2
Figure E.17: Rod 5 Axial Temperature Profile for ONB Run 3.3
Appendix F: THYC and VIPRE-I Consistency Analysis

In order to assure that correction factors were applied to NESTOR data appropriately and to investigate code modeling differences, an analysis was performed between THYC and VIPRE-I. This appendix summarizes the results of the THYC and VIPRE-I analysis. The majority of correction factors were applied to OMEGA data (wall thickness, flow variation, and thermal expansion). After applying said corrections and calculating local heat transfer coefficients from NESTOR data, differences were found between THYC and VIPRE-I calculated values. Such differences can be observed in Figure F.1 which depicts the ratio of experimental and computed HTCs for grid spans 1a and 1b of Run 4.2, Rod 5.

![Graph showing the ratio of experimental to calculated heat transfer coefficients for Run 4.2, Rod 5 data.]

Figure F.1: VIPRE-I-Predicted Ratio of $h_{\text{exp}}$ to $h_{\text{calc}}$ for Run 4.2, Rod 5 Data

Since the calculation of experimental HTC depends on the heat flux, outer rod temperature, and sub-channel temperature, these parameters were compared between EDF and PSU. Figure F.2 shows differences between calculated outer rod temperatures and Figure F.3...
shows differences between sub-channel temperatures predicted by THYC and VIPRE-I. A comparison of the heat flux is not presented since differences were insignificant.

Figure F.2: Outer Rod Temperature Difference between PSU and EDF

Figure F.3: Differences between VIPRE-I and THYC-calculated Sub-Channel Temperatures
It can be seen from these figures that there is a noticeable difference in predicted sub-channel temperatures. This is due, in large part, to the choice of code input conditions – EDF used optimized $f$-$K$ set 1 while PSU used optimized $f$-$K$ set 3. It can be seen from Figure F.2 that the difference between EDF and PSU calculated outer rod temperatures is biased towards a value of -0.09 °C. However, this shift was not noticed when comparing CEA and PSU values, which can be seen in Figure F.3. The reason for this inconsistency can be attributed to the fact that CEA and PSU both used the test targeted operating conditions for code input while EDF used the time-averaged operating conditions for code input.

Figure F.4: Difference in Predicted Sub-Channel Temperatures between FLICA-IV and VIPRE-I

The previously mentioned differences accounted for inconsistencies between calculated experimental HTCs; however, computed HTCs also differed, which has been attributed to differences in code steam tables. Since both codes used the Dittus-Boelter equation for predicted HTC, differences between the codes came down to fluid property and flow field inconsistencies.
Figure F.5 and Figure F.6 show the differences between Re and Pr distributions for the measurement locations as predicted by VIPRE-I and THYC.

Figure F.5: Comparison of Re Distribution Predicted by VIPRE-I and THYC

Figure F.6: Comparison of Pr Distribution Predicted by VIPRE-I and THYC
It is evident from these figures that there were considerable differences between fluid properties predicted by THYC and VIPRE-I. Additionally, there were differences in the predicted velocity field around Rod 5.

A more in depth investigation was performed on the specific contribution of the sources of discrepancy using Run 4.4 data. During heat transfer analysis, a discrepancy between the experimental-to-predicted HTC ratio of both codes was about 6.5%. This same behavior was seen for test 4.4. Results of Figure F.7 show that about 2.5% of the HTC ratio discrepancy was from differences in predicted HTC whereas about 4% came from differences in the calculated experimental HTC.

Figure F.7: Comparison of THYC and VIPRE-I Predicted HTC’s for Single-Phase Test 4.4
As mentioned during the analysis of Run 4.2 differences, the discrepancies were determined to stem from modeling and fluid property differences. To determine the contribution of fluid property differences, the fluid properties of THYC and VIPRE-I were compared over the temperature range of interest. Results are shown in Figure F.9.
By analyzing enthalpy difference between inlet and axial locations further downstream, it was determined that fluid property differences resulted in about 0.5 $K$ impact on fluid temperature prediction. The total temperature difference witnessed for sub-channels surrounding rod 5 is shown in Figure F.10.
Figure F.10: Difference in Sub-channel Temperature between THYC and VIPRE-I for Run 4.4

Since the total difference was nearly 1 \( K \), it can be assumed that differences between the codes caused by modeling differences (e.g. friction factor, grid loss coefficient, mixing models, and input parameters) accounted for the other 0.5 \( K \) difference in predicted sub-channel temperature. The fluid temperature difference led to about 2.3\% impact on calculated experimental HTC and -1.3\% impact on Dittus-Boelter calculated HTC. In other words, both sources (modeling differences and steam table fluid property differences) accounted for about half of the error seen between the two codes.

Since the goal of the concurrent analysis wasn’t to produce identical results, but to obtain independent results using individual sub-channel analysis codes, such differences were deemed acceptable. The data corrections performed by the individual parties were considered to be applied correctly, allowing for the continuation of thermal hydraulic analysis.