INVESTIGATION OF TWO-PHASE FLOW THERMAL-HYDRAULIC BEHAVIOR IN ROD BUNDLE DURING REFLOOD TRANSIENTS BASED ON THE RBHT EXPERIMENTAL DATA

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ABSTRACT

Reflood transients within a rod bundle geometry involves complicated thermal-hydraulic phenomena. This scenario could occur during emergency coolant injection for a light water reactor (LWR) under accident conditions. The general features related to the reflood stage, such as the flow and heat transfer regimes, quench front propagation, thermal-hydraulic non-equilibrium, are well known to the mechanical and nuclear engineering communities. However, detailed measurements and/or quantitative predictions of the local two-phase flow structures, interfacial mass and heat transfer processes, spacer grid effects on two-phase flow dynamics, especially for the Dispersed Flow Film Boiling (DFFB) regime, still present a major challenge today. This piece of missing information, once acquired, could contribute significantly to a better understanding of the two-phase flow process as well as the overall rod bundle response to postulated accidents such as the Loss-Of-Coolant-Accident (LOCA).

In order to have a better understanding of the reflood transients and to provide reliable experimental data for the two-phase flow mass and heat transfer model development, a series of constant reflood tests were performed at the NRC/PSU RBHT test facility. The tests cover a wide range of conditions that could be encountered during the accident scenario for a LWR, accounting for various parameters that include the system pressure, inlet liquid flooding rate, inlet liquid subcooling, initial peak cladding temperature, and the rod bundle power input. In the current experiment, transient variations of key parameters related to the two-phase flow thermal-hydraulics, such as the mass flow rate, fluid temperature, droplet behavior, as well as the rod bundle response, including the cladding temperature, and quench front propagation along the bundle, are measured using various measuring techniques. Based on these unique experimental results obtained, further data reductions and analyses have been performed to specifically study the two-phase flow characteristics (parametric effects, thermal-hydraulic non-equilibrium, mass
quality, liquid droplet dynamics, etc.) and the spacer grid effects (droplet breakup on spacer grid, form loss, etc.) in the DFFB regime. In addition, based on the experimental data, theoretical models (thermal-hydraulic non-equilibrium, two-phase flow mass quality, liquid droplet breakup, and spacer grid pressure drop) were developed describing the two-phase flow interactions as well as the rod bundle heat transfer processes.

Another part of this work includes a numerical analysis of the reflood transients using a nuclear reactor thermal-hydraulic sub-channel analysis code, i.e., COBRA-TF. The latter incorporates a two-fluid three-field solution scheme for the two-phase flow by solving a set of nine time-averaged conservation equations. Numerical simulations were performed for the reflood transients covering a wide range of test conditions. The results were compared with the RBHT reflood data set to evaluate the prediction capability of COBRA-TF on reflood transients, especially on the mass and heat transfer process in the DFFB regime. The overall prediction uncertainties of the code for important two-phase flow and heat transfer quantities were determined. Based on the comparison and evaluation to the existing models, further improvements as well as developments of new analysis models could be proposed for the two-phase flow and rod bundle heat transfer analyses.

The major outcomes of the current work are: 1). The system parametric effects (including the system pressure, inlet liquid injection rate, inlet liquid subcooling and the rod bundle power input) on the quench front propagation, collapsed liquid level variation, rod bundle overall pressure drop, rod bundle cladding temperature and heat flux, vapor temperature, spacer grid temperature, spacer grid pressure drop as well as on the liquid droplet size and velocity have been investigated in detail. 2). Various theoretical and empirical models have been developed based on the RBHT data, these include the two-phase flow thermal-hydraulic non-equilibrium model, two-phase flow local mass quality model for the DFFB regime, dry spacer grid induced liquid droplet
breakup model, and the spacer grid pressure drop model in the DFFB regime. 3). Comprehensive and inclusive COBRA-TF numerical simulations were performed and compared with corresponding RBHT reflood tests to verify the code prediction capability and to validate various two-phase flow mass and heat transfer models incorporated in COBRA-TF.

In the experimental part, very comprehensive and detailed two-phase flow measurement has been achieved in the RBHT test facility. It was found that the system pressure, flooding rate and rod bundle power input strongly affect the thermal-hydraulic behavior of the two-phase flow and rod bundle heat transfer. Whereas the inlet liquid subcooling was found to have secondary effect on the mass and heat transport, especially in the DFFB regime. Meanwhile, unique liquid droplet data has been obtained during reflood based on advanced laser imaging system. The entrained liquid droplets were found to have a log-normal distribution in size. As a liquid droplet size becomes larger, its corresponding velocity decreases. While it is relatively difficult to distinguish the most dominating system parametric effects on the liquid droplet size variation other than the quench front location relative to the point of measurement, the droplet velocity was found to be quite different for different flooding rate and system pressure tests. During reflood transients, the two phases were always found to be in significant thermal-hydraulic non-equilibrium but no detailed work were found to quantitatively study this phenomenon due to the limitation of experimental data. In the current study, the non-equilibrium extent, as characterized by the vapor phase superheat and the two-phase slip ratio, were investigated quantitatively. It was observed that the two-phase slip ratio is not only a function of the distance from the quench front location where most droplets get entrained but also a function of the liquid droplet size. Moreover, if there is thermal non-equilibrium within the two-phase flow, then the hydraulic non-equilibrium is likely to exist.
In the theoretical part, the several models developed (two-phase flow mass quality, liquid droplet breakup, and spacer grid pressure drop) in the current study were able to predict the two-phase flow mixture thermal-hydraulic behavior with significantly improved accuracy. This is mainly because the development of these models started from the fundamental principles and mechanisms involved in the two-phase flow mass and heat transfer processes and from the adequate consideration of various factors that are important to the current problem. The proposed two-phase flow mass quality correlation was able to predict the data from the current RBHT test and previous tests well within ±10% error. Model developed for the dry spacer grid liquid droplet breakup had an error within ±17%, whereas the spacer grid pressure drop model developed for the DFFB regime had an error span of ±25% (Pa).

In the numerical part, the COBRA-TF simulations and their detailed comparisons with the RBHT experiments provide abundant and very informative information for the code performance, based on which a verification and validation study was carried out in order to quantify the prediction uncertainties involved in reflood transients. It has been found that over the reflood conditions explored in the current study, the COBRA-TF was able to predict the rod cladding temperature within ±15% (K) error, the spacer grid and vapor phase temperatures within ±20% (K) error, and the droplet velocity within ±30% (m/s) error span. However, it was also found that COBRA-TF tended to predict early quench of the entire bundle, while under-predicting the DFFB regime void fraction variation. Most specifically, large uncertainties existed for two-phase flow pressure drop calculation.

The various results and major findings obtained in this work are significant and informative in the sense that new two-phase flow phenomena and behaviors were either discovered for the first time or investigated extensively due to abundant RBHT reflood data that made this possible. The valuable and unique data obtained from the NRC/PSU RBHT test
facility, either filled the void or greatly increased the current experimental data base for reflood transients (especially for the DFFB regime with liquid droplets present), and may serve as benchmark tests that will significantly improve the performance of the numerical simulation tools. Also, the theoretical works performed in the current study provided a detailed characterization of the two-phase flow behavior by relating the basic and local flow and heat transfer quantities (such as the interfacial heat transfer, vapor superheat, slip ratio, droplet size and fluid properties, etc.) to variables that are of importance in the two-phase flow thermal-hydraulic analysis (such as the void fraction, mass quality, ratio of in- and out-coming liquid droplet size at spacer grid location, spacer grid pressure drop, etc.), thus broadening our way of understanding the two-phase flow and providing more choices when tackling the problem at hand. Last, the extensive code verification and validation performed for the COBRA-TF simulation provided the basis and direction for further model improvement and development.
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Chapter 1

Introduction

The thermal-hydraulic behavior during convective boiling in the post-CHF regime has been an important topic for many scientific researches and engineering applications, such as in cryogenic systems, refrigeration systems, spray cooling, spray combustion, steam generators, as well as in the core emergency cooling period of a nuclear reactor under accident conditions. In the post-CHF regime, the temperature of the heated surface is mainly controlled by the cooling capability of the two-phase flow mixture in which the liquid droplets are dispersed within the continuous vapor flow. The corresponding flow pattern encountered is often referred to as the mist flow or liquid droplet dispersed flow, while the corresponding heat transfer mode is generally called the Dispersed Flow Film Boiling (DFFB). This type of mass and heat transfer regime develops at a very high void fraction region that is generally above 0.8-0.9 depending on the system pressure, mass flow rate and many other flow parameters. Detailed discussions on the DFFB phenomenology as well as on modeling of various mass and heat transfer processes involved can be found in the works performed by Chen (1986) [1], Varone and Rohsenow (1990) [2] and recently by Andreani and Yadigaroglu (1992, 1994) [3, 4].

In previous studies related to the two-phase flow with boiling heat transfer, it was found that limited experimental data has been obtained for the detailed two-phase flow thermal-hydraulic non-equilibrium, especially during reflood transients, for the two-phase behavior in the DFFB regime due to its extreme complexity. Moreover, the thermal and hydraulic non-equilibrium between two phases were often subjected to separate studies such that their coupled effects and interactions cannot be adequately accounted for. Finally, there does not have an obvious connection between the macroscale and microscale analyses. That is, the useful results
obtained from microscale analyses (interfacial heat transfer, interfacial drag, liquid droplet size and velocity distribution, local conditions of the heating surface, etc.) cannot be readily transferred and applied to macroscale analyses (cladding temperature prediction, two-phase mass quality, flow regime transition, pressure drop, etc.). As an attempt to solve these difficulties existing in the study of two-phase flow behaviors and to better understand the fundamental thermal-hydraulic aspects involved in reflood transients, in the present study, data obtained from the RBHT reflood tests were evaluated and analyzed in detail. Particularly, the important flow quantities (cladding temperature, quality, slip ratio, etc.) governing the two-phase flow thermal-hydraulic non-equilibrium were investigated using the proposed analyzing methodology, from which a detailed and comprehensive understanding of the transient thermal-hydraulic non-equilibrium behaviors can be obtained.

Based on the complete experimental data set, various theoretical models and correlations were developed by considering the fundamental physics involved in order to improve the prediction capability of various two-phase flow models for the thermal-hydraulic behaviors in the DFFB regime. The models/correlations developed in the current study include: two-phase flow thermal-hydraulic non-equilibrium, two-phase flow local mass quality, liquid droplet breakup on dry spacer grid, and the spacer grid pressure drop in the DFFB regime. Significant improvement of accuracy in predicting the two-phase flow behaviors has been achieved using these models/correlations, indicting that the models developed captured the underlying physics involved in the DFFB regime.

In the numerical simulation part of the current study, various numerical simulations were performed for the reflood transients using the sub-channel analysis code COBRA-TF, which is a thermal-hydraulic computational code for predicting the thermal-hydraulic behavior of water-cooled nuclear reactors. COBRA-TF uses a two-fluid three-field (liquid, vapor and droplet) solution scheme to solve the conservation equations of mass, momentum and energy for each
field. In the current study, COBRA-TF analysis model has been developed for the RBHT reflood tests covering wide ranges of parameters. Detailed comparisons have been made between the numerical predictions and the experimental data such that the two-phase flow and heat transfer models incorporated in COBRA-TF can be validated.

The current study is useful in many ways since very detailed measurements for key flow and heat transfer parameters have been made possible at the NRC/PSU RBHT test facility and the current analysis has investigated both thermal and hydraulic two-phase behavior during reflood. The models/correlations developed showed significant improvement of their performance in predicting the two-phase flow behavior. Through comparing the numerical calculations with experiments, needs for existing model upgrading and/or replacement have been identified and discussed, which will provide guidance for future experimental research works as well as for theoretical and computational model development efforts.
Chapter 2

Background and Literature Review

In this chapter, a literature survey has been performed to obtain a comprehensive and in-depth understanding of previous studies on the two-phase thermal-hydraulic behavior and liquid droplet entrainment during reflood transients. An overview of the sub-channel analysis code COBRA-TF is also provided.

2.1. Two-Phase Flow Thermal-Hydraulic Non-Equilibrium

As one of the most important topics in the thermal-hydraulic and safety analysis for light water reactors (LWRs), the thermal-hydraulic processes that could exist in the DFFB regime during reflood have been studied extensively based on either theoretical analyses or experimental investigation. In general, the fundamental difficulties involved in characterizing the DFFB regime come from its significant thermal-hydraulic non-equilibrium within the two-phase flow mixture. Reflood is a highly transient and unstable process both for small scale two-phase interactions (turbulence effect, droplet entrainment, droplet breakup, etc.) and for large scale phenomena (mass flow rate and pressure oscillations, quench front (QF) propagation, variation of heat flux profile, etc.). The problem is complicated by the need to consider the interfacial mass and heat transfer behavior that depends not only on the temperature difference but also on the relative velocity and interfacial area between the two separate phases. The interfacial area further depends on the liquid droplet volumetric concentration (or void fraction), the droplet spatial distribution (distribution of heat sinks) within the flow channel, and the droplet size distribution. Therefore, evolution of the thermal-hydraulic processes in the DFFB regime under reflood is often flow-
history dependent. Previous studies reported that very significant thermal non-equilibrium can be developed between two phases in the DFFB regime. Mueller (1967) [5] and Polomik (1967) [6] experimentally measured the superheat of vapor phase in convective film boiling in a tube geometry. This phenomenon was later confirmed by experiments performed in tubes by Nijhawan et al. (1980) [7], Evans et al. (1983) [8] and Gottula et al. (1985) [9]. These studies showed that vapor superheats as high as several hundred degrees can be achieved during reflood. On the other hand, Hochreiter (1977) [10] and Unal et al. (1988a, 1988b and 1991) [11, 12, 13] have observed the thermal non-equilibrium in their experiments for the rod bundle geometry. In Unal’s work, for example, the actual mass quality was compared with the equilibrium quality both for high and low mass flux cases. Results indicated that while vapor temperature is close to the saturation temperature immediately downstream of the quench front, significant vapor superheat exists in the far-field region. However, constrained by the measurement capability, only limited data on vapor superheat was obtained in their studies.

Up to date, various integral heat transfer models predicting the overall heat transfer between the rod surface and the two-phase flow mixture within the DFFB regime were developed (Bennett et al. 1967 [14], Kaminaga 1981 [15], Murao and Iguchi 1982 [16], Kawaji and Banerjee 1987, 1988 [17, 18], Nelson and Unal 1992 [19]). In these models, different heat transfer mechanisms were considered, including convective heat transfer between the wall and the fluid as well as between the two phases, and radiation heat transfer between the three components (wall, vapor and liquid droplets). Other researchers such as Forslund and Rohsenow (1968) [20] and Ganic and Rohsenow (1977) [21], also incorporated the direct wall and liquid contact heat transfer process into their studies.

Despite of these macroscale heat transfer analyses conducted between the wall and the fluid for various flow geometries, investigations were also directed to the detailed analysis of microscale heat transfer process between a single droplet and its surrounding vapor phase. Many
experimental and numerical studies have been carried out in the attempt to quantify this mass and heat transfer process and various empirical correlations have been proposed by previous researchers. Based on their experiments, Lee and Ryley (1968) [22] proposed a correlation for interfacial heat transfer between a hanging liquid droplet and its surrounding vapor flow in terms of dimensionless numbers. In their study, the Nusselt number was reported as a function of the Reynolds and Prandtl numbers of the fluid. However, their experiments were only restricted to low vapor superheat conditions such that the actual vapor phase superheat during reflood cannot be adequately accounted for. As an attempt to improve the prediction of the interfacial heat transfer process for high superheat conditions, Ban and Kim (2000) [23] modified Lee and Ryley’s correlation by adding a correction term expressed by the ratio of actual quality to thermal equilibrium quality, $x_a/x_e$. Their results compared relatively well with the experimental data. In 1983, Renksizbulut and Yuen (1983a, 1983b) [24, 25] reported an empirical correlation for the interfacial heat transfer calculation in high-temperature steam, based on their experiments as well as numerical calculations for a single droplet evaporating in an air/steam flow. Their equation was later modified by Renksizbulut and Haywood (1988) [26] to account for the effects of variable fluid properties and the droplet internal circulation. Haywood et al. (1994) [27] further extended this work by considering the effect of droplet deformation.

During reflood, as mentioned above, the two-phase flow thermal non-equilibrium is always accompanied by significant hydraulic non-equilibrium (or mechanistic non-equilibrium). In DFFB, entrained liquid droplets are expected to be moving at different velocities than the vapor velocity, leading to the relative motions between phases. Moreover, each of these droplets, large or small, will have different velocities. As a result, detailed prediction of the relative motion between phases requires detailed characterization of the droplet size and velocity distributions as a function of space and time as well as the flow channel geometry under investigation. In order to solve this problem, by considering a force balance on droplet motion and the effects of
evaporation, interfacial drag correlations were reported in the works by Renksizbulut and Yuen (1983b) [25], Renksizbulut and Haywood (1988) [26], and Chiang et al. (1992) [28]. However, in deriving these correlations, they only considered the interactions of a single liquid droplet with its surrounding vapor flow, thus ignored the droplet distribution as well as the interactions of adjacent droplets themselves. Ingebo (1956) [29] studied the drag coefficients for droplets in clouds and reported a correlation as a function of the Reynolds number only. In the correlations developed by Ishii and Chawla (1979) [30], drag-similarity criteria and mixture-viscosity model were introduced in order to derive constitutive relations for interfacial drag force for a group of particles. Their results compared well with experiments. Nevertheless, their model only considered the steady state conditions whereas the interfacial phase change process, which are commonly encountered during reflood transients, was completely ignored.

From the above literature survey, it can be seen very limited experimental data has been obtained for the two-phase flow thermal-hydraulic non-equilibrium, especially during reflood transients. Moreover, the thermal and hydraulic non-equilibrium between phases were often subjected to separate studies such that their coupled effects and interactions cannot be accounted for. Finally, there does not have an obvious connection between the macroscale and microscale analyses. For this reason, useful results obtained from microscale analyses cannot be readily transferred and applied to macroscale analyses. As an attempt to solve these difficulties existing in the study of two-phase flow behaviors and to better understand the fundamental thermal-hydraulic aspects involved in reflood transients, in the present study, data obtained from the RBHT reflood tests were evaluated and analyzed in detail. Particularly, the important parameters governing the two-phase flow thermal-hydraulic non-equilibrium were investigated using the proposed analyzing methodology, from which a detailed and comprehensive understanding of the transient thermal-hydraulic non-equilibrium behaviors can be obtained.
2.2. Liquid Droplet Entrainment, Deposition and Breakup

In order to accurately model the mass and heat transfer processes within the DFFB regime, many researchers focused their studies on the behavior of liquid droplets as well as the interactions of droplets with surrounding vapor. Representative experimental and theoretical works include: Ganic and Rohsenow (1977) [21], Adams and Clare (1984) [31], Lee et al. (1984) [32], Sugimoto and Murao (1984) [33], Paik and Hochreiter et al. (1985) [34], Yao et al. (1988) [35], Unal et al. (1991) [36], Ireland et al. (2003) [37], Cheung and Bajorek (2011) [38] and Cho et al. (2011) [39]. Previous studies indicate that the liquid droplet size in DFFB can be adequately represented by a log-normal distribution, despite that Sugimoto and Murao (1984) [33] assumed a $\Gamma$-distribution function to describe the droplet size. The log-normal distribution is a very important distribution in probability theory and statistics due to its extensive applications in scientific researches and engineering fields from human biology to environment radiation measurement. For example, in spray process involving droplet impacts, the size distribution of atomized droplets can be described by a log-normal distribution (Wu 2003 [40]). As a result, many studies of the sample size and confidence interval were based on log-normal distribution, including Finney (1941) [41], Hale (1972) [42], Cochran (1977) [43], Pashchenko (1996) [44], Zhou and Gao (1997) [45] and Olsson (2005) [46].

2.2.1 Liquid Droplet Entrainment

Keeyes et al. (1970) [47] performed an experimental work for adiabatic steam in a vertical pipe 3.7 m long and 12.7 mm in diameter for pressures ranging from 34 to 68 bars. In their experiments, in which a total mass flux in the range of $\sim$1360 to $\sim$2720 kg/m$^2$s was employed, the
liquid entrainment and liquid film flow rate were obtained using a porous wall device to extract the liquid film flow.

Ishii et al. (1975) [48] derived the inception criterion of droplet entrainment in the co-current gas-liquid annular flow regime. Since then, the shearing off of the roll wave crests and the wave undercutting were recognized as the physical mechanisms for droplet entrainment at Reynolds numbers above and below 160, respectively. In their work, changes in the critical gas velocity for the onset of entrainment at a certain film Reynolds number was obtained and was explained based on the entrainment mechanism.

Wurtz (1978) [49] measured the film flow rates, film thickness and pressure drop for two different tubular configurations. Steam-water mixture was selected as the working fluids while the operating pressure was maintained within the range between 30 to 90 bars, and the mass flux was set between 500-3000 kg/m²s. In addition the flow quality was varied between 0.08 and 0.6. A suction device was used to extract the liquid film along the wall. Finally the rate of droplet entrainment was calculated according to the difference between the total liquid flow rate and the film flow rate at the surface.

Dallman (1978) [50] obtained a correlation for the fraction of entrained liquid phase in a gas-liquid system under fully developed annular flow conditions by using an approximate dynamic balance between the entrainment rate from the wall film and the deposition rate of droplets back to the wall film. The experiment was performed covering a wide range of entrainment rates, local film thicknesses and pressure drops. The effect of flow asymmetry in a horizontal two-phase flow was considered in the work as well.

Ishii et al. (1981) [51] conducted a study of liquid entrainment to obtain a correlation for the entrainment fraction that can be expressed as a function of three dimensionless groups, including the dimensionless gas flux, the total liquid Reynolds number, and the dimensionless diameter. In their study, an expression describing the distance required to reach the so-called
quasi-developed entrainment state was derived. At the entrance, however, an exponential relaxation expression was proposed by the researchers to account for the entrainment fraction that would eventually reach the fully developed value along the flow path. A droplet entrainment model was developed by considering the shearing off effects of the roll wave crests. The results reported by the correlation agreed pretty well with the experimental data within the pressure range of 1-4 atm, with a liquid Reynolds number varied between 370-6400 and a superficial gas velocity less than 100 m/sec.

Sugawara (1990) [52] performed an analytical and numerical study on the liquid entrainment. An entrainment model was developed for an upward co-current annular flow based on the dimensionless gas velocity and the force balance between the interfacial shear and the surface tension at the gas-liquid wavy interface. The proposed model also used a pressure correction factor, the polynomial cure fit for the wave height and a logarithmic curve fit for the gas Reynolds number. Validation studies were carried out using the Film Dryout Analysis Code in Sub-channels (FIDAS). However, their work failed to include the effect of gravity when performing the force balance analysis.

Nigmatulin et al. (1996) [53] developed a correlation considering the dynamic droplet entrainment rate that accounts for a critical Weber number below which no entrainment could expected to occur. A separate equation was proposed to determine the critical Weber number that corresponds to the onset of entrainment for both laminar and turbulent flow conditions. Their work, for the first time, considered the influence of gravity upon the flow direction and matched well with the experimental data. The possibility of additional entrainment due to the collisions of depositing droplets was also investigated.

Lopez et al. (2001) [54] used the air-water and Freon-113 as the working fluids in their experiments and developed a correlation based on dimensionless parameters including the film Reynolds number, the Weber number, and the density ratio. A double film extraction technique
was used to determine the entrainment rate. The experiments were scaled in order to produce steam-water flow conditions at high pressures. Moreover, the correlation was also scaled by a factor to account for the average growth rate of ripples due to the Kelvin-Helmholtz instability at the interface. However, the main mechanism for droplet entrainment in an annular flow, i.e., shearing off of roll wave crests as suggested by Ishii and Grolmes (1975) [48] was not considered in the development of the correlation.

Sawant et al. (2009) [55] reported a correlation for the entrainment fraction based on their experimental data using air-water and Freon-113 as the working fluids. Similar to Nigmatulin et al. (1996) [8], they considered the critical gas and liquid flow rates below which no entrainment is expected to take place. A correlation for the minimum liquid film flow rate that is possible under the maximum entrainment condition was also proposed. Though the correlation compared favorably with the air-liquid experimental data reported in the literatures, significant differences were identified in their prediction for the steam-water flow conditions.

2.2.2 Liquid Droplet Deposition

Paleev and Filipovich (1966) [56] performed experiments using air-water flow system in a horizontal tube to measure the liquid deposition mass flux and obtained a correlation for the droplet deposition rate in terms of the gas Reynolds number and the density ratio by fitting a curve to their experimental data. The experiments were carried out at atmospheric pressure and the local gas Reynolds number varied between $3 \times 10^4$ and $8.5 \times 10^4$. This correlation however does not account for the dependence on the droplet size or liquid Reynolds number.

Namie and Ueda (1972, 1973) [57, 58] performed an experimental study to investigate the droplet transfer in a two-phase annular mist flow. In their experiment the droplet transfer to the duct wall, the distribution of droplet velocity, the droplet concentration, and the gas velocity
were measured. An air-water flow system was used for annular mist flow regime through a horizontal duct of rectangular cross section. During the test, the mean diameter of liquid droplets varies between 27 and 40 microns. It was found that the droplet deposition coefficient was significantly affected by both the gas velocity and the droplet concentration. While the droplets velocity distribution was found to be relatively flat within the range of test conditions, the eddy diffusivity of the gas was found to reduce as the droplet concentration was increased, thus resulting in a decreased deposition constant.

In the work of Ganic and Mastanaiah (1981) [59], the droplet deposition from a two phase turbulent flow onto the wall of a smooth tube was studied thoroughly both theoretically and experimentally. A model for the deposition of particles in the Stokes Regime (Rep < 1) was proposed based on the turbulent diffusion in the core region of the two phase flow which is followed by a free flight to the wall. They found that the dimensionless deposition velocity only depends on the particle relaxation time and the Reynolds number. Several tests were performed using air-water as the working fluid in a vertical tube for a Reynolds number ranging from 52,500 to 94,600. The model proposed was found to agree well with their experimental data and other relevant data reported in the literature.

Trela and Zembik (1982) [60] presented a model for droplet deposition from turbulent gas flow to a vertical plate based on the so-called stopping distance that allows presenting of different turbulent diffusivities of the gas and droplets. Experiments were performed utilizing a horizontal duct and the deposition measurement was taken on a separated vertical plate mounted parallel to the duct wall. Air-water flow system was used under the pressure of 2 atm. The mean gas velocity flowing through the duct varied from 1 to 14.1 m/sec while the maximum droplet concentration in the experiment was set to be 0.035 kg/m³.

El-Kassaby and GanicIn (1986) [61] extended the theory of Ganic and Mastanaiah (1981) [59] to taking the Oseen regime (Rep < 5) into consideration. Both theoretical and experimental
studies were performed for droplet deposition onto a smooth vertical tube. However, calculation of the particle to fluid diffusivity ratio was based on the drag coefficient in the Oseen regime, which differs from Ganic and Mastaniah’s approach.

Issapour and Lee (1990) [62] performed an experiment study of the droplet deposition from a turbulent two phase mist flow onto a parallel vertical wall. The air-water flow system was used with the Re number varying between $1.54 \times 10^5$ and $4.2 \times 10^5$. A particle sizing two dimensional reference mode Laser Doppler Anemometry technique was adopted to acquire the data. A correlation for the droplet deposition coefficient was developed using the experimental data along with a postulated physical mechanism based on the apparent turbulent viscosity of the gas with respect to the particles and the most energetic eddy frequency of the flow.

Schadel et. al. (1990) [63] performed an experimental study to measure the rate of deposition and atomization in the co-current two phase annular flow regime within a vertical tube. They found that for low droplet concentrations, the droplet deposition rate increased linearly with an increase in the droplet mass flow rate whereas for high droplet concentrations, the droplet deposition rate was found to be independent of the droplet concentration. They also found that the droplet deposition rate was relatively insensitive to the gas velocity. Their results, however, contradict with the results obtained by previous researchers (Namie and Ueda 1972, 1973 [57, 58]). The cause for the discrepancy was attributed to the possibility that the slip ratio between the droplets and the gas is a variable.

2.2.3 Liquid Droplet Breakup at Spacer Grid

One of the earlier studies of droplet breakup was performed by Yao et al. (1988) [35] who investigated the behavior of various offsets of incoming droplets with respect to a heated strip. The resulting droplet spectrum and dynamics were reported together with the explanation of
the droplet breakup mechanism. The objective of their work was to report the available database in non-dimensional form, to correlate the mean diameter of shattered droplets, and to discuss the results and their application to reactor safety analyses. In their study, data for droplets (with diameter of $d_o$) impacting on hot thin strips (with thickness of $W$) beyond the Leidenfrost temperature have been correlated and compared to two limiting conditions. The mechanisms of droplet disintegration during impaction with $d_o/W$ ranging from 3 to 6 were found to involve shattering and cutting by the edge of the strip. Two groups of small and large droplets were generated respectively by these two mechanisms. The smaller droplets contributed much to the increased liquid-to-vapor interfacial area for heat and mass transfer.

In the work of Ergun (2006) [64], the dispersed flow film boiling heat transfer model and the spacer grid models of the COBRA-TF code were modified by adding a small droplet field to the code as the fifth field. Since wet spacer grids is expected to provide a large interfacial area for the heat transfer between the superheated vapor and the liquid deposited on the spacer grid, the grid rewet has been modeled and the effects of wet grid on heat transfer has been investigated. Reflood experiments during which DFFB exists were selected from the Full Length Emergency Core Heat Transfer-System Effects and Separate Effects Tests (FLECHT-SEASET) and the Rod Bundle Heat Transfer (RBHT) tests for the verification of the upgraded code. The results of the code evaluations were presented by comparing the experimental data with the numerical simulations performed both with original and modified code.

The work of Srinivasan (2010) [65] focused on the development of a wet grid model to describe the wet grid phenomenon observed during the reflood transients of a postulated Loss of Coolant Accident (LOCA) in a nuclear reactor. A mathematical model is formulated for the wet grid breakup process. It can be used to predict the droplet diameter downstream of a wet grid, given the flow and system conditions upstream of the grid. In addition, a numerical-based
correlation is proposed to obtain the downstream to upstream ratio of the Sauter mean diameter of droplets in a wet grid situation.

Cheung and Bajorek (2011) [38] theoretically studied the dynamics of droplet breakup associated with a dispersed two-phase flow mixture through a dry grid spacer in a rod bundle during a reflood transient in a pressurized water reactor. Based on the conservation of liquid mass and the kinetic energy as well as the surface energy of the droplets, a correlation was derived for the ratio of the Sauter mean diameters of the droplets downstream and upstream of the grid. They found that the Sauter mean diameter could decrease appreciably as a consequence of the cutting and shattering of the droplets when passing through a dry grid spacer, thus increasing the interfacial heat transfer surface area. The decrease in the droplet size was found to depend on the Weber number of the incoming droplets, the blockage ratio of the grid spacer, and the fraction of the kinetic energy of the incoming droplets converted to the surface energy of the newly generated droplets during the breakup process. Comparisons of the theoretical results were made with the experimental data obtained at the RBHT test facility as well as with other relevant data reported in the literature and were found to agree quite well with each other.

2.3. Two-Phase Flow Pressure Drop and Spacer Grid Effects

In the past few decades, a number of researchers focused their efforts on the pressure drop measurements under two-phase flow conditions and the corresponding prediction model development not only for the nuclear fuel rod geometry but also for the heated tube with various orientations and many other flow setups. These studies include: Lockhart and Martinelli (1949) [66], Chisholm (1967) [67], Beattie et al. (1973, 1982) [68, 69], Friedel (1979) [70], Chen et al. (2001) [71], Olekhnovitch et al. (2005) [72], Leung et al. (2005) [73], Vijayarangan et al. (2007) [74]. The model developed by Lockhart and Martinelli, Beattie and Friedel focus on the
derivation of two-phase flow pressure drop multiplier. Chisholm’s model is widely used for frictional pressure drop prediction in tubes. Chen’s model introduced the dependence of the Bond and Weber number as a correction of the homogeneous model. In Beattie’s model, a correlation of two-phase flow pressure drop multiplier for spacer grid (SG) and obstacles in post-CHF region was also proposed.

Spacer grids (SGs) of various types and spacings are widely used in fuel bundle designs. From a mechanical point of view, the use of SG will help maintain the rod spacing as well as the structural integrity of the fuel assemble and prevent any flow-induced vibration. From a heat transfer point of view, the presence of a SG is expected to increase the turbulence downstream of the SG for single-phase and two-phase flow heat transfer. The reconstruction of the boundary layer downstream of the SG results in lowered cladding temperature and increased CHF values. However, introducing the SG into flow channels will also increase the pressure drop along the rod bundle at the same time, which in turn requires a larger pressure head for primary pumps to drive the coolant through the system. For this reason, accurate quantification of SG induced pressure drop is crucial for SG designs and for thermal-hydraulic analysis under normal operating and accident conditions of LWR.

A variety of studies have been performed by previous researchers to address the issue of spacer grid effects on the mass and energy transfer within the nuclear fuel rod assemblies. The presence of these grids is known to have a significant impact on the flow condition and the heat/mass transfer process due to their non-negligible thickness that causes acceleration/deceleration of the flow, creation of secondary flows, disruption of the boundary layer, and entrained droplet breakup in dispersed droplet flow regime. The primary research findings are that the implementation of spacer grid structures inside the reactor core fuel assembly will enhance the heat transfer either between the fuel rod surface and fluid or between the liquid interface due to the increase in the fluid Nusselt number and the CHF values in rod bundles, thus
leading to higher safety limit. In addition to the investigations performed for the single-phase flow, for which spacer grid-induced heat transfer enhancement was observed, spacer grids have additional effects on two-phase flow, including in particular the spacer grid induced droplets entrainment and de-entrainment as well as droplet breakup.

Stosic (1999) [75] studied the effects of spacer grids on dryout/rewetting as well as the local thermal hydraulics behaviors within rod bundles. In their study both the spacer grid types, i.e., swirl vane, etc., and the spacing were examined in detail. It is found that reducing the spacing between spacer grids leads to increased CHF values. This phenomenon is mainly due to both the disruption of the liquid film formed on the spacer grid surface and the thermal relaxation length between spacer grids. Moreover, it was also observed that the quench time of the bundle, as affected by the spacer grids, was strongly dependent on the mass flowrate of the fluid. As discussed, the spacer grid induced heat transfer enhancement was a function of the Reynolds number and the blockage ratio. For the same blockage ratio, there was less enhancement for higher Reynolds numbers. While if the Reynolds number is hold the same, a higher blockage ratio will lead to a higher heat transfer enhancement. Such induced heat transfer enhancement effects were found to disappear after only 5-20 diameters downstream of the spacer grid, while the induced CHF enhancement effects lasted until 60-80 diameters downstream of the spacer grid. CHF enhancement disappeared by 120-140 diameters downstream of the spacer grid.

Peng et al. (2003) [76] studied the effects of a flow obstacle on the flow using R-134a refrigerant as the working fluid. In their studies, the flow obstacle with a blockage ratio of 0.12 was a cylinder that was held against the wall of a tube by a magnet in order for it to move easily along the axial directions. Their results indicated that there was a significant increase in the convective heat transfer around the flow obstacle region followed by an exponential decrease.

Cho et al. (2007) [77] compared the effects of various types of spacer grid including the egg-crate spacer grid and the swirl vane spacer grid. Their test channel consisted of a single
heater rod enclosed by a circular housing. For both types of spacer grids the heat transfer enhancement was observed, with the swirl vane spacer grid having a greater enhancement effect, especially under low flooding rate conditions. Owing to the increase in the heat transfer, the quench time was found to be faster than that of the egg crate type spacer grid.

The work performed by Lee and Chang (2010) [78] utilizing R-134a refrigerant as the working fluid demonstrates the effects of spacer grids on heat transfer. Their experimental setup included a circular tube with a single heater rod and two different types of spacer grids: one with swirl vanes and the other one without swirl vanes. The blockage ratios are 0.040 and 0.058, respectively. This study was aimed at investigating spacer grid effects on post-CHF flow regimes. In general, heat transfer enhancement was observed for both types of spacer grid, with, however, a more obvious effect for the swirl-vane spacer grids. At the test section outlet, far downstream of the spacer grid location, the enhancement effects were found to vanish when compared to the case without spacer grids.

To-date, very few studies have been conducted for pressure drop over SGs and/or obstacles, especially in the post-CHF regime. Spengos (1959) [79] investigated the head losses for single-phase liquid flow through a rod bundle with different types of supports. A correlation was developed based on his experimental results. De Stordeur (1961) [80] also studied the pressure drop over different SG types experimentally. But the final results were only presented in graphical form, which is inconvenient for practical applications. Rehme (1973, 1978 and 1980) [81, 82 and 83] proposed a correlation similar to that of Spengos’s but with emphasizing more on the effect of relative plugging (blockage ratio) of SG. Based on Rehme’s data, Cevolani (1995) [84] suggested empirical correlations for the modified loss coefficient C_v of Rehme’s pressure drop correlation. Chun and Oh (1998) [85] developed a model based on a mechanistic approach such that no empirical constant is needed. Beattie (1973) [68], Lottes (1961) [86], Mendler et al. (1961) [87], and Rooney et al. (1974) [88] reported different prediction models for two-phase
expansion pressure losses which can be used to obtain a two-phase SG loss multiplier. However, the idea of using a two-phase multiplier to determine the SG pressure loss, the same way as calculating the two-phase frictional pressure drop, requires additional experimental and/or theoretical work to be done on quantifying the single-phase SG pressure loss coefficient, which is usually highly geometry-dependent and may further complicate the problem. Moreover, additional uncertainties could be introduced in the pressure drop calculation. Unal et al. (1994) [89] experimentally investigated the pressure drop for rod bundle SG in the post-CHF dispersed flow regime, which is similar to the present work. However, no correlation was proposed in their study. Zhang et al. (2016) [90] proposed an empirical correlation by fitting the experiments performed for support plate. Good agreement between experiments and predictions was observed. Recently, Maskal and Aydogan (2017) [91] evaluated various combinations of two-phase SG pressure drop multipliers and single-phase loss coefficients based on the BWR Full Size Fine Mesh Test (BFBT). It was observed that pressure drop values due to SG models can be significantly different and none of these prediction models universally apply outside of the original experimental conditions under which they were validated.

Yao et al. (1982) [92] studied the heat transfer augmentation by straight grid spacers in rod bundles for single-phase and post-critical heat flux dispersed flow. They also examined the heat transfer effect of swirling grid spacers in single-phase flow. In their study, predictive correlations were established to analyze the heat transfer enhancement. However, due to the lack of adequate understanding on the complexity of spacer grid induced cutting and shattering, the droplet breakup were not considered in their study.

Sugimoto et al. (1984) [33] performed an experimental study in order to clarify the effect of the spacer grid on reflood heat transfer in PWR-LOCA. The flow pattern, the thermal responses and the water accumulation near the spacer grid were investigated by shifting the grid spacer at the mid-plane of the simulated core. In their research works, the heat transfer coefficient
before the quenching was about 20-50% higher just downstream of the spacer grid than that of upstream of the spacer grid. The decrease in droplet diameters induced by the spacer grid cutting and shattering was also observed in the droplet dispersed flow regime under reflood transient. They argued that the heat transfer enhancement due to the spacer grid is mainly attributed to the increased interfacial surface area of droplets in the dispersed flow and also to the increased film boiling heat transfer in the slug flow.

Yao et al. (1988) [35] conducted the experiments with droplets impacting on the edge of the thin steel strips that were heated to beyond the Leidenfrost temperature. High-speed movies were taken and analyzed and showed that the shattered droplets were generally bimodal in size distribution. The volume ratio of these two size groups of generated droplets, the mean diameter of droplets, and the ejection angles and velocities of shattered droplets are shown as a function of incoming droplet Weber number, the ratio of incoming droplet diameter to strip thickness, and the offset of the droplet relative to the strip. The data are presented in non-dimensional form and correlations are provided for the mean diameter of the shattered droplets. The theoretical limiting conditions of a droplet impacting normally to a large plate and cutting by a strip of zero thickness are analyzed. The present results are compared with those of the limiting conditions. The application to a nuclear reactor spacer grid behavior during two-phase dispersed flow is discussed.

Ireland et al (2003) [37] quantified the behavior of droplets as they pass through a spacer grid in a rod bundle geometry consisting of 49 rods connected with mixing-vane spacer grids and an overall length of 3.7 m. The droplet diameter distributions upstream and downstream of a spacer grid were determined by a digital imaging system. Their results showed that the spacer grid produces 29% decrease in the mean diameter of the droplets under the given experimental conditions.
In the work of Cho et al. (2007) [77], an experimental study was performed to investigate the effects of a spacer grid in an annular flow channel with a uniform power shaped single rod during a bottom-reflood phase. The ranges of the experimental parameters are 2-8 cm/sec for the flooding velocity, 20-80 °C for the inlet subcooling temperature, and 500-700 °C for the initial wall temperature. In their study two types of spacer grids, i.e., a swirl-vane type grid and a straight egg-crate type spacer grid, were examined to compare the differences in their thermal hydraulics behavior through the spacer grids used. In the case of a low flooding rate and a high wall temperature condition, the cooling capacity of the swirl-vane spacer grid is better than that of the straight egg-crate type grid. Rewetting velocities through the swirl-vane spacer grids are faster than those through the other type of grids. The cladding temperature of the heater rod near a spacer grid shows a different pattern with the types of spacer grids used.

2.4. Overview of the Sub-channel Analysis Code COBRA-TF

The COBRA-TF code referring to COolant Boiling in Rod Arrays-Two Fluid, is a computer program originally developed by the Pacific Northwest Laboratory to provide best-estimate thermal hydraulic analyses of a LWR reactor vessel for design basis accidents and anticipated transients. In cooperation with tests such as the FLECHT-SEASET and the NRC/PSU RBHT programs, COBRA-TF was constantly modified and improved to enhance its predictive capability for reflood transients.

The two-fluid formulation, generally being incorporated in thermal-hydraulic analysis codes, separates the conservation equations (mass, momentum and energy) for each phase, i.e. the vapor and liquid phases. However, COBRA-TF extends this treatment to three fields: vapor, continuous liquid and entrained liquid droplets. By dividing the liquid phase into two separate
fields, more physically realistic and accurate predictions can be made for the two-phase flow behavior.

The COBRA-TF two-fluid three-field representation of the two-phase flow results in the requirement of solving a set of nine time-averaged conservation equations. The Eulerian time average over a time interval is used and the interval is assumed, on one hand, to be long enough such that it smooths out the random fluctuations in the flow, and on the other hand, short enough to preserve any gross unsteadiness in the flow.

The general assumptions made in the COBRA-TF two-fluid conservation equations are listed below [93]:

- Gravity is the only body force in the momentum equation;
- No volumetric heat is generated in the fluid;
- Radiation heat transfer is limited to rod-to-drop and rod-to-steam;
- Pressure is uniform in all phases;
- Irreversible viscous dissipation is neglected in the enthalpy formulation of the energy equation;
- Turbulent stresses and turbulent heat flux of the entrained liquid phase are neglected;
- Viscous stresses are further split into fluid-wall shear and fluid-fluid shear;
- Fluid-fluid shear in the entrained liquid phase is also neglected;
- Conduction heat flux is partitioned into a fluid-wall conduction term and a fluid-fluid conduction term; and
- Heat conduction within one phase is assumed to be negligible in the entrained liquid field.
2.5. Discussion on the Current Status of Reflood Study

From the above comprehensive literature review it can be seen that the current status of study on the two-phase thermal-hydraulic behavior during reflood transients, especially in the DFFB regime, is far from enough in accurately predicting all the possible phenomena involved.

Firstly, thermal-hydraulic non-equilibrium has been discovered long ago back to the work by Mueller (1967) [5] and Polomik (1967) [6]. However, due to the extremely complexed flow behavior and the limitation in two-phase flow measurement techniques, quantitative studies made for this topic is very scarce. Previously, the heated rod bundle or tube were relatively short in length, thus it was difficult to maintain the DFFB regime for a long time. Although hot patch technique has been developed and applied to some of the test facility to freeze the quench front and to create a steady-state DFFB regime, such method tends to distort the real reflood conditions since the actual reflood transients are never in steady-state. In addition, only a few attempts have been made to measure the vapor temperature in the DFFB regime for these experiments. Due to thermal non-equilibrium, the vapor is significantly superheated, leading to the need to characterize the vapor temperature profile in detail. On the other hand, in order to study the hydraulic non-equilibrium, knowledge of detailed liquid droplet size and velocity distribution within the flow channel is required. This portion of data has just been made possible with the development of modern advanced instrumentation technique.

Second, the various models applied for the two-phase flow in the DFFB are highly simplified due to lack of understanding of basic physics involved in reflood transients. For instance, the two-phase flow mass quality is usually obtained through the energy balance calculation, which frequently assumes that the vapor phase is at saturation. Despite of rather different flow pattern and heat transfer mode, the pressure drop prediction is still based on the two-phase multiplier that was developed mainly for continuous liquid flow with vapor bubbles.
dispersed. Also, very few studies were performed on the liquid droplet breakup due to spacer grid effect. Most of the models neglect some important mechanisms during the breakup process.

Thirdly, because of the scarce experimental data and the oversimplified analysis models for the reflood transients, in numerical simulations, selecting two-phase models as well as interpreting the calculation results are thus empirical. Therefore, extensive verification and validation work need to be done based on experiments in order to develop high-fidelity computational tools for nuclear thermal-hydraulic applications.

Aiming at solving these problems, the current work performed a comprehensive and detailed analysis on the two-phase flow behavior during reflood transients, especially for the DFFB regime. It starts from a complete experimental data reduction and analysis based on recent data obtain from the NRC/PSU Rod Bundle Heat Transfer (RBHT) Test Facility. Effects of various system parameters on the reflood thermal-hydraulics and rod bundle temperature response have been investigated. Then, by making use of this abundant, unique data collected, sophisticated two-phase flow models can thus be developed and validated with a significantly improved accuracy. Last, the current experimental data and models developed can be compared with the numerical simulation results such that performance of current numerical simulation tools can be evaluated.
Chapter 3

NRC/PSU RBHT Test Facility

The Rod Bundle Heat Transfer (RBHT) test facility was designed and built by The Pennsylvania State University (PSU) under the sponsorship of the United States Nuclear Regulatory Commission (U.S. NRC). This facility is aimed at conducting systematic separate effects tests to obtain adequate data in support of the NRC thermal-hydraulic transient analysis code development efforts.

3.1 General Test Configuration

Figure 3-1 shows the general configuration of the RBHT test facility. It is a once-through test facility with liquid and/or vapor being the working fluid. Aiming at simulating the reflood transients of the Loss of Coolant Accident (LOCA) for LWRs under low pressure conditions, the RBHT test facility is capable of performing various type of tests including: constant reflood tests, variable reflood tests, oscillatory reflood tests, steady-state level swell tests, steam cooling tests and steam cooling with droplet injection tests. For reflood test specifically, the system is able to cover a wide range of system conditions with system operating pressure up to 413.7 kPa (60 psia), inlet liquid flooding rate between -0.2 to 0.2 m/sec (-8 to 8 in/sec), rod bundle peak power input up to 2.30 kW/m (0.7 kW/ft), initial PCT up to 1144 K (1600 °F) and inlet liquid subcooling temperature as high as 83 K (150 °F). Note that the negative flooding rate is used for oscillatory reflood tests.

Figure 3-2 shows the main components of the RBHT test facility. It includes: the injection water supply tank, the liquid injection pump, a vertical test section with upper and lower
plenum at both ends, small and large carryover tanks, a steam separator, the steam separator drain tank and a pressure oscillation damping tank. The boiler component is mainly used for the steam cooling and steam cooling with liquid droplet injection test series as well as for preheating the various system components before reflood test.

Figure 3-1: Schematic of the RBHT Test Section.

Figure 3-2: Main Components of the RBHT Test Section
A schematic of the RBHT test section is shown in Figure 3-3. In total there are 12 quartz windows located on opposite sides of the Inconel housing for flow and liquid droplet visualization within the rod bundle. There is one pair of quartz windows for each spacer grid (except for the first grid at the bottom of the bundle), which allows for a comparison of two-phase flow and droplet distributions up- and down-stream of the spacer grid. Water flows upward from the inlet at the bottom of the bundle to the outlet at the upper plenum. Along the test section, seven spacer grids made of Inconel 600 are located ~522 mm apart with each having a blockage ratio of 0.362. A skewed axial power profile towards the top of the bundle is applied to the heated rods with the maximum-to-average power ratio being 1.5 at 2.74 m (9 ft).

![Schematic of the RBHT Test Section](image)

Figure 3-3: Schematic of the RBHT Test Section

A photograph of the rod bundle structure at a spacer grid location as well as a cross-section view of the square array and a single rod bundle are shown in Figure 3-4 (a), (b) and (c), respectively. As can been from Figure 3-4 (b), the RBHT test facility has a 7 × 7 rod bundle
assembly with four unheated support rods at the corners and 45 electrically heated rods with a heated length of 3.66 m (12 ft). Each rod has a typical PWR rod diameter of 9.49 mm (0.374 in) and rod pitch of 12.6 mm (0.496 in), as shown in Figure 3-4 (c). The test facility is heavily instrumented such that it is capable of capturing the transient variations of various flow and heat transfer quantities during reflood period (Hochreiter et. al. 2010, 2012 [94, 95]). In Figure 3-4 (b), the heated rods equipped with the instrumentation are marked with letter “I” whereas the ones which have no instrumentations are labeled as “U”. “S” indicates that the rod is a support rod.

Figure 3-5 further presents the actual spacer grid used within the rod bundle. It can be seen that mixing vanes with a $30^\circ$ angle are used to enhance the flow mixing downstream. TC’s
are installed on the spacer grid surface to measure the temperature variation during entire reflood transients.

![Photograph of the Spacer Grid with Mixing Vanes](image)

Figure 3-5: Photograph of the Spacer Grid with Mixing Vanes

### 3.2 Introduction of the RBHT Instrumentations

The RBHT test facility is heavily instrumented such that it is capable of capturing the transient variations of various thermal-hydraulic parameters during reflood period (Hochreiter et al. 2010, 2012 [94, 95]). In total there are 512 data channels in the data acquisition system recording the transient variations of different parameters during a test, these include temperature, pressure drop, flow rate, etc. There are 256 TC’s mounted inside the heater rods to monitor the cladding and spacer grid temperature variations, covering the entire length of the rod bundle. The flow housing has 23 pressure taps connected to sensitive differential pressure (DP) cells providing measurements of pressure drop for the two-phase flow mixture at various locations. Figure 3-6 shows the actual photograph of the DP cells installed along the test section through pressure taps. Besides, there are also thirteen stand-off penetrations along the flow housing for measuring of vapor superheats in the DFFB regime. The steam temperature is measured using miniature thermocouples having a diameter of 0.813 mm (0.032 in) which are attached to the traversing steam probe rakes having a diameter of 0.381 mm (0.015 in), as is shown in Figure
3.7. These are very small diameter thermocouples having a fast response time such that they can follow the vapor temperature accurately in a dispersed, non-equilibrium, two-phase flow environment. As the quench front approaches, the number and sizes of the droplets increase which can lead to wetting of these thermocouples. Experiments performed as part of the FLECHT-SEASET program indicated that very small thermocouples could provide reliable vapor superheat ready for the longest time period until they are quenched as the froth region approaching. As a result, the steam temperatures measured in the DFFB regime are considered to be valid as long as the quench front is still far away.

Figure 3-6: Photograph of the RBHT Test DP Cells

Figure 3-7: Photograph of the RBHT Test Steam Probe Rakes
3.3 Liquid Droplet Laser Imaging System

A high resolution imaging system known as the Oxford Lasers Firefly System is used for droplet measurement and analysis. Figure 3-8 shows the experimental setup of the imaging system. As can be seen, this system consists of a high-resolution (over 1.0 Megapixels) digital camera, an infrared laser instrument that generates laser beams, the associated computer and control equipment and the corresponding data analysis software known as VisiSize, which is capable of analyzing the droplet velocity and size distributions.

![Figure 3-8: Configuration of the Oxford Lasers Firefly Imaging System](image)

The actual picture of the laser imaging system is shown in Figure 3-9. Two sets of the Oxford Lasers Firefly systems are installed in the RBHT test facility to acquire droplet data during reflood tests. Laser system 1 and laser system 2 are located upstream (2.74m or 108in.) and downstream (2.92m or 115in.) of spacer grid #6 in the RBHT test facility, respectively. To better align the Firefly lasers so that better illumination can be achieved, a laser mounting system which allows for adjusting the lasers to be adjusted independently was built and installed as shown in Figure 3-9. In addition, band-pass IR filters were fabricated and installed on both camera lenses to filter out the infrared light emitted by the heated rods.
As described in the operation manual of the Oxford Lasers (2013, 2015) [96, 97], the Firefly diode laser is an illumination system that provides pulses of infrared laser light with variable pulse durations and pulse repetition frequencies. It is designed to be used with high speed and/or high resolution cameras without infrared filters for high speed image capture. The light can be manipulated into either sheet or area illumination by means of internal focusing optics. The sheet technique can be used to illuminate a discrete slice of a flow field for ‘Particle Image Velocimetry’ or other full field techniques. Area illumination can be used for either front or rear lighting to analyze shapes, sizes and positions of objects, particles or droplets.

The diode laser consists of a control unit and a laser head connected by an umbilical cable. The laser head produces the light for illumination of the subject to be photographed. The control unit provides the necessary electronics to regulate the pulse repetition frequency and select the required pulse length (exposure). A user interface panel and software application provide laser control and the user input. The laser may be triggered either with the internal clock or an external pulse from a camera or other trigger source.

The JAI AM-201CL camera is a 2-megapixel industrial grade CCD camera offering a combination of HDTV resolution, high fidelity, and high frame rates. The AM 201CL features
the KAI-02150 quad-tap sensor from Kodak, capable of providing full $1920 \times 1080$ monochrome resolution at 64 fps. A standard Camera Link digital interface supports 8-bit, 10-bit, or 12-bit output. The AM201CL uses the Kodak CCD quad-tap architecture for rapid image acquisition, then combines the four taps into two for monochrome readout over a simple one-cable Camera Link base configuration. Built-in channel balancing capabilities are provided to ensure uniformity across the image.

Analysis of images collected by the Firefly Laser system is performed by the VisiSize 6.508 software. VisiSize 6.508 is a video imaging software package featuring fast image analysis. The system is designed to rapidly analyze a wide variety of particles, droplets or bubbles and report the size and velocity information for up to 1000 particles per frame. It can analyze a single image and can also analyze a sequence of images to build up particle size and velocity distributions. The software output includes histograms and the major averages as well as measurements of the spread, the mean droplet size, and the average droplet velocity. Test data can be stored and recalled for later manipulation or combination with other recorded data. The system is capable of automatic data save and subsequent collection restart (if images are being analyzed online) so quasi steady-state data can be recorded for a transient test. To visualize droplet behavior, a series of frames can be written to the active memory and replayed as a “movie”. Movies can also be saved to disk for later replay. Single frame can be saved for later recall and print in TIFF format as well. The image processing method of the software is discussed later.

The droplet sampling region is selected to be the central sub-channel “D4”, as is shown in Figure 3-8, and the axial location is fixed at the upstream of SG 6 with an elevation of 2.74 m/108 in (peak power location). The detailed measurement configuration within the sub-channel “D4” can be found in Figure 3-10 that enlarges the local sampling area. While the laser detection area setting has a dimension of $12.6 \times 8.985 \times 16.275$ mm (Probe Depth × Width × Height), the
actual droplet sampling area that can be achieved is limited by the viewing width (gap) between two adjacent columns of rods, which is about 3.1 mm.

As is illustrated in Figure 3-8, the digital camera together with an infrared laser generator is adjusted and focused on the focal plane at a desired measurement elevation along the heated length. To eliminate non-infrared light from the environment and incidental infrared light from heater rods, a narrow band-pass light filter is installed in front of the camera. Any additional illumination interference is eliminated by applying an anti-glare attachment. Figure 3-11 is an illustration of the typical images taken with and without droplets in the measuring location. The continuous area in white is the flow channel area between two columns of rods. When there is no droplet present in the channel, the laser beams will pass through the measurement channels, being received by the camera and form a uniform bright background (Figure 3-11 (a)). However, when there are droplets passing by, the laser beams will be refracted by the interface, forming droplet shadow areas on the background as a result (Figure 3-11 (b)). By analyzing these images the droplet size and velocity distributions can be obtained. In order to directly compare droplets at different locations, two sets of camera and laser systems are installed using the same laser pulse frequencies and shutter speeds in both camera and laser systems.

Figure 3-10: 3-D Schematic of the Droplet Measurement Area.
Figure 3-11: Sample Pictures Obtained by the Imaging System

During a reflood test, many images like the ones shown in Figure 3-11 are obtained sequentially. The standard image processing procedure adopted by the VisiSize software is illustrated in Figure 3-12 (Oxford Lasers, 2013, 2015 [96, 97]). First, after loading these images, a pre-processing threshold is applied to distinguish the particle shadows from illuminated background, as is shown in Figure 3-12 (a). Then, a second threshold determines the degree of focus or the sharpness of these droplets Figure 3-12 (b). The VisiSize software is capable of identifying droplets that are out of focus and is specifically calibrated to accurately determine their correct sizes. Droplets that are too far away from the focal plane to be accurately measured are thus rejected in the analysis. Except for the degree of focus criterion, each droplet is also evaluated according to several other criteria such as the degree of droplet sphericity, border contact condition, etc., in order to determine whether a droplet-like shadow should be analyzed or discarded. In other words, if a droplet is too far away from the focal plane, or has an irregular shape far from spherical, or if the droplet is coming into direct contact with the border or overlapping with other droplets, then it is discarded from the analysis. The software is capable of measuring up to 1000 particles per frame. These identified droplets are marked out by the system.
as shown in Figure 3-12 (c) together with the analyzed results. Further data processing uses these droplets as a set of raw data. VisiSize is capable of not only analyzing a single image but also gathering information from a sequence of images to construct the droplet size and velocity distributions.

![Image Processing Procedures for the VisiSize Software]

In order to obtain the correct droplet size distribution, the depth of field (DOF) correction must be used as a correction factor when analyzing a sequence of images (Oxford Lasers, 2013, 2015 [96, 97]). This correction factor accounts for the fact that larger droplets are able to be measured over a greater depth of field than smaller droplets, since smaller droplets are more likely to appear to be out of focus. It should be noted that the DOF correction is only accurate for a camera lens calibrated by Oxford Lasers. As a result of applying the correct DOF factor,
corrected droplet counts are obtained from the original in-focus droplet count actually obtained by the measurement. Since DOF correction factors are always numerically greater than unity, the corrected droplet counts are much larger than the in-focus droplet counts and such correction is more significant when droplet size decreases. Therefore, in order to be statistically valid, calculations of all the statistical parameters (mean, standard deviation, etc.) in the analyses are based on the corrected droplet counts. Statistical parameters determined from the corrected counts will be used to find the droplet in-focus count lower limit.

Besides the droplet size distribution, the VisiSize software is also capable of measuring the droplet velocity distribution. In order to do this, the system is operated in a so-called double pulse mode in which the laser pulses twice with a known time period, creating many paired pictures. The distance traveled by the droplets between each picture pair can thus be determined and be used in the droplet velocity calculation.

In the present study, the sampling frequency is 15 Hz with a pulse separation of 36 μs for velocity measurement in double pulse mode. The field of view is 16275 × 8985 microns with the probe depth being 12600 microns. The micron-pixel ratio is 8.477 in the experimental setup, meaning that the actual dimension of each pixel is 8.477 microns. Two imaging systems are equipped such that droplet behaviors both upstream and downstream of a SG can be measured simultaneously. As is shown in Figure 3-13, the cameras are focused on the central channel “D4” but at different axial elevations. Figure 6 shows the axial measurement locations for the two imaging systems. As can be seen, two digital cameras are located right below and above SG 6 (at an elevation of 2.794 m/110 in). System 1 is fixed upstream of SG 6 with an elevation of 2.743 m/108 in, while system 2 is located downstream of SG 6 with an elevation of 2.921 m/115 in. During reflood tests, the droplets generated at the quench front location are entrained by the steam to move upward. Some of them are expected to impact onto hot SG edges as well as surfaces, leading to the generation of much finer droplets due to the SG cutting and shattering
effects. For a wet grid, a completely different mechanism exists that needs to be studied separately. By quantitatively measuring the droplets simultaneously upstream and downstream of the SG, the effects of SG on the droplet breakup and subsequent heat transfer augmentation can be analyzed.

Figure 3-13: Experimental Configuration of the Lasers and Cameras

3.4 Measurement Uncertainties

The uncertainties in terms of the heater rod and steam probe temperature measurements using TC’s are determined to be ±1.11 K of a total range of 283.15-1644.15 K. Of all the DP cells applied, the largest total probable error involved is about ±10.84 mm of H₂O (0.17%) for a total span of 0-6350 mm H₂O. The inlet flow transmitter has a total probable error of ±0.395 g/sec for the total span of 0-1247 g/sec. One the other hand, the exhaust line steam flow transmitter has a total probable error of ±0.172 m³/min for a total rage of 0-12.7 m³/min. Due to the fact that heater rod TC’s are attached at the inside surface of the cladding, transient variation of the cladding surface temperature is thus deducted from a one-dimensional (1-D) inverse heat
conduction code, DATARH, which is able to compute the local transient surface temperature and heat flux in the radial direction. Since the rod cladding temperature changes slowly in the DFFB regime before quench, the uncertainties involved using the 1-D approach are small. However, it should be noted that the 1-D inverse heat conduction will involve relatively large uncertainties when the quench front is close due to 2-D heat conduction effect.

In order to perform statistically valid analysis on the liquid droplets field, the uncertainty related to the measurement of a group of droplets using imaging system need to be quantified. The individual droplet size measurement uncertainty has been investigated in detail by Todd (1999) [98] using the method for determining 95% confidence level. In his study, a measurement bias was observed in the VisiSize system. That is, the small particles captured by the system appear to be smaller than their actual size. Such bias in individual droplet measurement results from the laser light diffracting around the particles. The effect of laser diffraction is more profound with decreasing size. However, this problem is found to be insignificant. For small droplets (0.1 mm), the uncertainty is determined to be about 3.2% while for large droplets (2 mm), the uncertainty is only about 0.03%. In addition, a correction factor specifically accounting for laser diffraction is used to obtain the final droplet size measured by VisiSize. Therefore, this problem is not a concern in the current study. More detailed discussion can be also found in the work by Ireland et al. (2003) [37]. In Appendix C, various statistical methods are explored to determine the minimum droplet sample size required as a function of standard deviation at a given confidence level. Accordingly, an appropriate droplet count lower limit is selected. Based on this lower limit, the 95% confidence intervals are determined for two typical reflood tests. In addition, the repeatability in droplet measurement of Rod Bundle Heat Transfer (RBHT) reflood tests is carefully examined by comparing data sets from two identical tests. Results show that a lower required level of precision and a smaller scattering in the measured droplet data generally require a smaller sample size. For typical RBHT reflood tests, it is found that the droplet size
measured downstream of a spacer grid near the peak power location always has better accuracy compared with that at the upstream location. For the tests investigated, the maximum relative error in the liquid droplet Sauter mean diameter is found to be generally smaller than 0.15 (15%) when the droplet count lower limit is 30. Comparison of the results from two identical tests indicates that the RBHT reflood test results are highly repeatable.

3.5 Test Procedures and Data Reduction Technique

During a reflood test, the rods are pre-heated to a prescribed initial peak cladding temperature, which is well above the Leidenfrost point, to simulate a postulated LOCA in LWRs. To eliminate possible vapor condensation on the flow housing, the latter is also pre-heated electrically. Once the desired temperature is achieved, water at various subcooling temperatures and mass flow rates is then injected into the lower plenum and all measurement channels are monitored and recorded. During the entire liquid injection period, the system pressure at the exit of the upper plenum and the power input are held constant. As the quench front propagates upward along the heated rods, large amounts of liquid droplets are generated and entrained in the superheated vapor flow, forming the DFFB regime above the quench front location. The entrained liquid droplet mass flow rate is measured in the carryover tanks while the vapor mass flow rate is measured by the volumetric flow meter. The droplet size and velocity distributions are measured by the imaging system.

A series of reflood tests were performed in the RBHT facility in order to investigate the effects of different system parameters on the DFFB thermal-hydraulic transients. The system parameters varied include: the test loop system pressure (137.90 - 413.69 kPa), peak power (0.98 - 2.30 kW/m), initial PCT (1033 - 1144 K), inlet liquid subcooling temperature (11 - 83 K) and inlet flooding rate (0.0191 - 0.2032 m/sec). In the current analysis the results of various reflood
tests have been evaluated. Table 3-1 presents the selected test conditions. These conditions correspond to the low pressure, low flooding rate case such that the flow pattern at the quench front is annular flow. Various comparisons will be made to investigate the thermal-hydraulic behavior during reflood transients based on this data set.

Table 3-1: Test Conditions for Selected RBHT Constant Reflood Tests

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Pressure kPa (psia)</th>
<th>Flooding Rate m/sec (in/sec)</th>
<th>Inlet $\Delta T_{sub}$ K (°F)</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Initial PCT K (°F)</th>
</tr>
</thead>
<tbody>
<tr>
<td>8089</td>
<td>124.11 (18)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8021</td>
<td>137.90 (20)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8041</td>
<td>137.90 (30)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8009</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8023</td>
<td>413.69 (60)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7157</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7173</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8011</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7151</td>
<td>275.79 (40)</td>
<td>0.0191 (0.75)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7174</td>
<td>275.79 (40)</td>
<td>0.0191 (0.75)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7116</td>
<td>275.79 (40)</td>
<td>0.0508 (2)</td>
<td>56 (102)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7112</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>51 (90)</td>
<td>0.98 (0.3)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7095</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>83 (150)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7166</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>83 (150)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7168</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>51 (90)</td>
<td>0.98 (0.3)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8013</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>53 (96)</td>
<td>1.97 (0.6)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8032</td>
<td>275.79 (40)</td>
<td>0.2032 (8)</td>
<td>83 (150)</td>
<td>2.30 (0.7)</td>
<td>1144 (1600)</td>
</tr>
<tr>
<td>8038</td>
<td>275.79 (40)</td>
<td>0.0508 (2)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8040</td>
<td>275.79 (40)</td>
<td>0.0508 (2)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7123</td>
<td>275.79 (40)</td>
<td>0.1016 (4)</td>
<td>23 (42)</td>
<td>1.31 (0.4)</td>
<td>1144 (1600)</td>
</tr>
<tr>
<td>7136</td>
<td>275.79 (40)</td>
<td>0.1524 (6)</td>
<td>83 (150)</td>
<td>2.30 (0.7)</td>
<td>1144 (1600)</td>
</tr>
<tr>
<td>7138</td>
<td>275.79 (40)</td>
<td>0.2032 (8)</td>
<td>73 (132)</td>
<td>2.30 (0.7)</td>
<td>1144 (1600)</td>
</tr>
<tr>
<td>7140</td>
<td>275.79 (40)</td>
<td>0.2032 (8)</td>
<td>5 (9)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
</tbody>
</table>
The data reduction methodology to be described in the following section can be used to evaluate reflood tests as long as the measurement uncertainties are relatively small.

Complete test matrices performed at the RBHT test facility are listed in Appendix A. For the complete data collected for the RBHT reflood tests, please refer to the NUREG report 2019 [99]. The test matrix for original bundle I tests can be found in another NUREG report [95].
Chapter 4

Experimental Data Comparison and Reduction

In this chapter, the RBHT experimental data has been reduced and evaluated in detail with an emphasize on the DFFB regime for various test conditions including: system pressure, inlet liquid velocity, inlet liquid subcooling temperature, rod bundle power input (expressed by the peak power) and rod bundle initial peak cladding temperature (PCT). Key thermal-hydraulic quantities are compared and analyzed in the current study. A complete RBHT data set can be found in the NUREG report [99].

RBHT reflood data is carefully selected and evaluated based on the tests presented in Table 3-1 in Section 3.5 for the two-phase flow thermal-hydraulics within the rod bundle geometry. The various flow and heat transfer quantities investigated include: the quench front probation, collapsed liquid level (CLL), rod bundle overall pressure drop, rod cladding temperature and heat flux, vapor temperature, spacer grid temperature, spacer grid pressure drop, liquid droplet size and velocity variation.

4.1 System Parametric Effects on Quench Front and Collapsed Liquid level (CLL)

During reflood, the propagation of quench front determines how fast the rod bundle can be cooled down. Also, different initial and boundary conditions will result in different two-phase flow mass and heat transfer regimes at the quench front location, which have significant effects on the energy transfer from rod surface to fluid, liquid droplet entrainment and breakup, spacer grid thermal-hydraulic behavior and the subsequent overall energy removal capability of the two-phase flow. As a result, the quench front profile has a very significant effect on the PCT that
could be achieved during the DFFB at the downstream of the quench front and on how much liquid that can be stored within the bundle during reflood. It is necessary to investigate the effects of various system parameters on the quench front propagation. This section provides a detailed study on the quench front propagation for the selected reflood tests.

First, the effect of system pressure on the quench front propagation and CLL is shown in Figure 4-1. Each of those dashed lines represent a spacer grid location. It can be clearly seen that keeping the other test conditions the same, the system pressure has very significant effect on the overall quench front propagation and the CLL variation. As the system pressure increases from 137.90 kPa (20 psia) to 413.69 kPa (60 psia), the total quench time decreases almost by half. This is because higher system pressure corresponds to higher fluid saturation temperature and at the same time alters the fluid thermophysical properties, leading to much faster reflood transients. On the other hand, higher system pressure led to more liquid storage within the bundle, indicating that there was less vapor generation below the quench front. However, it is interesting to point out that the higher system pressure test actually had the highest PCT value in the DFFB regime during reflood, indicating that its heat removal capability in the DFFB regime is quite small. This important observation will be further discussed in the following sections.

In Figure 4-2, the effect of the rod bundle power input is studied. Note that the power generation for each heated rod is maintained constant during the entire reflood process. As expected, higher power input resulted in higher cladding surface temperature and slower quench behavior, since more liquid would be evaporated and leave the rod bundle as vapor. As a result, the quench front propagated slower along the rod bundle surface. In addition to quench front behavior, very significant difference was observed for the CLL during reflood. While the highest peak power test (1.97 kW/m (0.6 kW/ft)) had only less than half of liquid storage within the bundle, the test with 0.98 kW/m (0.3 kW/ft) had significantly more liquid presence within the bundle, especially in the late phase of reflood. Since heat generation in the rods is the driving
force for energy flow and phase change, such profound effect on the two-phase thermal-hydraulic behavior is thus expected.

Figure 4-1: Quench Front Profiles and CLL at Different System Pressures (Separate CLL Curves in Appendix E)

Next, the effect of inlet liquid injection velocity on the quench front and CLL profiles is investigated based on three selected tests having inlet velocity of 0.0191 m/sec (0.75 in/sec), 0.0254 m/sec (1 in/sec) and 0.0508 m/sec (2 in/sec), respectively. From Figure 4-3 it can be observed that the total quench time is shortened with increased inlet liquid injection velocity. The test with high flooding rate is also capable of maintaining significant amount of liquid within the test section, leading to early quench due to increased liquid injection and the increased convective heat transfer between the fluid and rod surface. In addition, for higher flooding rate test the top-down quench can also be observed. This is induced by the backward flow from the upper plate due to liquid droplet deposition.
Figure 4-2: Quench Front Profiles and CLL at Different Peak Power Levels (Separate CLL Curves in Appendix E)

Figure 4-3: Quench Front Profiles and CLL at Different Inlet Velocities (Separate CLL Curves in Appendix E)
Last, the effect of the inlet liquid subcooling temperature can be found in Figure 4-4. Generally speaking, the inlet liquid subcooling temperature is expected to affect the boiling process and the heat transfer regime transition during reflood. The fluid flow with higher subcooling will tend to suppress the boiling process and vapor generation at the same time increase the vapor condensation after departure from the heat surface, thus leading to more liquid storage and less vapor flow above the quench front, as is shown clearly by the CLL variations. However, it can be observed that the quench front propagation behaviors for these tests were quite similar with each other despite of different inlet subcooling. This is mainly because the CHF and/or dryout phenomena are induced by instant heat release from the heating surface and thus are dominated by the local effects such as the liquid properties near the surface, heating surface condition and so forth. Therefore, the inlet liquid subcooling would have minor effect on the quench front propagation.

Figure 4-4: Quench Front Profiles and CLL at Different Inlet Liquid Subcooling Temperatures (Separate CLL Curves in Appendix E)
4.2 System Parametric Effects on Rod Bundle Overall Pressure Drop

Reflood is a highly transient and complicated process that involves various mass and heat transfer behaviors. The propagation of a quench front along the rod bundle surface is a unique characteristic in a two-phase flow system encountered by reflood. As a result, the rod bundle pressure drop is not only a function of axial location but also a function of the reflood time. During the reflood transients of LOCA, the rod bundle total pressure drop determines the rate of coolant mass that can be injected into the core region and thus is a very important quantity that needs to be investigated in nuclear reactor thermal-hydraulics and safety analysis. As introduced in Section 3.2, there are in total 23 DP cells for the pressure drop measurement at RBHT test facility with one DP cell (Chan. 362) spans over the entire rod length and the rest 22 DP cells (Chan.’s 363-384) spread sequentially along the rod bundle. In this section, the rod bundle overall pressure drop measured by Chan. 362 DP cell is processed and investigated in detail under various test conditions. Note that all the DP cells have been calibrated at standard conditions and report the measurement in inches of H₂O. The pressure drop (static head) is then obtained as the product of the liquid head, the density at standard condition (ρ_{STP}) and g, as is shown in Eq. (4.1):

\[ \Delta P = \rho_{STP} h g \] (4.1)

The overall pressure drop includes the gravitational pressure drop for the two-phase mixture within the test section, the frictional pressure drop along the entire rod bundle, the acceleration pressure drop due to flow channel area change and phase change, and the form loss due to blockage structure such as spacer grids. The different components are given in Eq. (4.2) as:

\[ \Delta P_{\text{exp,SG}} = \Delta P_{\text{fric}} + \Delta P_{\text{acc}} + \Delta P_{\text{grav}} + \Delta P_{\text{SG}} \] (4.2)

In Figure 4-5, the rod bundle overall pressure drop variations are compared at different system pressures, namely 137.90 (20), 206.84 (30), 275.79 (40), 413.69 (60) kPa (psia). It can be
seen that the overall pressure drops for the rod bundle were quite similar with each other. During reflood transients in a vertically heated flow channel, even though the overall pressure drop consists various parts, it is considered that the gravitational pressure drop of the two-phase mixture is the dominating factor for the pressure drop variation. And the gravitational pressure drop is further dominated by the liquid phase due to significantly larger density compared with vapor phase. As a result, the overall pressure drop is expected to be closely related to the collapsed liquid level within the flow channel.

Figure 4-5: Rod Bundle Overall Pressure Drop at Different System Pressures

A detailed comparison between the CLL in Figure 4-1 and Figure 4-5 further confirms this behavior. Quite similar in the trend of CLL and the overall pressure drop variations were observed. Initially, the close liquid storage of the liquid within the bundle led to close pressure drop despite of different system operating pressure. Later when the CLL profiles started to deviate from each other due to the quench front propagation and bulk liquid boiling below the quench front, differences in the overall pressure drop among the four tests also became larger and
larger. In particular, the reflood test with the highest system pressure stored the most liquid in the bundle after quench and had the highest overall pressure drop.

The effect of rod bundle power input on the bundle overall pressure drop is shown in Figure 4-6, which can be compared with the CLL variation in Figure 4-2. It can be observed that due to more liquid mass stored in the bundle for the lower power input test Exp. 7168, the overall pressure drop was also higher for this test and, vice versa. Despite of the initial resemblance, the rod bundle power input really had significant effect on the overall pressure drop at later times of reflood due to liquid accumulation.

![Figure 4-6: Rod Bundle Overall Pressure Drop at Different Peak Power Levels](image)

Next, the effect of reflood flooding rate on the overall pressure drop is presented in Figure 4-7. As can be seen, Exp. 7116 reached to very high overall pressure drop due to high flooding rate 0.0508 m/sec (2 in/sec). The flooding rate not only affects the later time pressure drop behavior but also the early time pressure drop. As can be observed from Figure 4-3, the rod
bundle was filled up quickly after initial injection for Exp. 7116. The entire quenching process finished well before 400 sec.

The effect of inlet liquid subcooling temperature is further studied and the results are presented in Figure 4-8. As having been discussed in previous section, the liquid subcooling mainly affects the bulk liquid boiling process below the quench front, which in turn will affect the liquid storage within the test section. Therefore, with higher liquid subcooling, higher overall pressure drop was observed. This is in agreement with the CLL variation in Figure 4-4. In addition, one thing to notice from Figure 4-8 is that relatively large pressure drop oscillations were observed for Exp. 7095.

![Figure 4-7: Rod Bundle Overall Pressure Drop at Different Inlet Velocities](image-url)
4.3 System Parametric Effects on the Heat Transfer in Rod Bundle Geometry

Reflood stage is a highly transient and complex process involving various mass and heat transfer regimes. The thermal-hydraulic process within the rod bundle is significantly affected by various system parameters. The important thermal-hydraulic quantities includes: the rod cladding temperature, rod surface heat flux, vapor temperature above the quench front and the spacer grid temperature.

Figure 4-9 presents the effect of system pressure on these important thermal-hydraulic quantities. Four system pressure conditions were investigated include: 137.90 (20), 206.84 (30), 275.79 (41) and 413.69 kPa (psia) The rod cladding temperature at axial elevation of 2.74 m (108 in), which is at the peak power location, is shown in Figure 4-5 (a). Note that each curve represents an averaged value from several TC’s at the same axial location but different radial locations. As can be clearly seen from Figure 4-5 (a), the test with highest system pressure had
the highest PCT in the DFFB regime, even though it quenched first. On the contrary, the test with lowest system pressure quench the latest but had moderate cladding temperature variation during entire reflood transients. This interesting phenomenon can be further confirmed by the heat flux profile for these tests, as is shown in Figure 4-5 (b) for central channel at 2.69 m (106.1 in).

While Exp. 8023 initially had the lowest heat flux among these reflood tests, the heat transfer rate increased significantly later, resulting in early quench. Next, the vapor temperature variations at 2.54 m (100 in) location are plotted in Figure 4-5 (c). As can be seen, initially the vapor phase had almost the same temperature as the rod cladding. However, liquid injection and the resulted convective heat transfer with liquid droplet entrainment significantly lowered vapor temperature, indicating that higher cooling capability has been achieved between the rod surface and the vapor phase. Despite of the temperature difference, the vapor phase temperature evolution history was quite similar with the rod cladding temperature with the highest system pressure test quenched first. Last, the spacer grid temperature variations for SG 6 at 2.80 m (110.27 in) (Chan. 274) are shown in Figure 4-5 (d). Since spacer grids are unheated structures, their temperature variations depend on the heat balance during the reflood. Energy transported from the rod surface through conduction and radiation is removed by convective heat transfer between the spacer grid surface and the vapor phase and heat conduction to the liquid droplets deposited on to its surface, or to the liquid film subsequently formed, and the radiation as well. Due to increased liquid deposition and reduced vapor superheat as the quench front approaches, spacer grids were found to quench at an earlier time than the rod cladding surface at the same elevation. Therefore, they also serve as heat sinks during reflood transients.
The effect of different rod bundle power inputs on the heat transfer process is shown in Figure 4-10. The different power generation expressed by the peak power are: 0.98 (0.3), 1.31 (0.4), and 1.97 (0.6) kW/m (kW/ft), respectively. As expected, the power generation within the rod bundle dominates the rod bundle heat transfer process. Higher power input led to higher rod cladding temperature and higher heat flux before quench. This resulted in the later quench for the bundle at a specific axial location. Correspondingly, the vapor temperature variations were also
found to be quite different with lower temperature and earlier quench for low power test. Similar trend was observed for the spacer grid as is shown in Figure 4-10 (d). It should also be noted that after quench, the heat flux profiles for the three tests stabilized at different levels, indicating that the heat transfer capability for nucleate boiling is sufficient to removal all the heat being generated within the bundle even for the highest power test.

(a) Rod Cladding Temperature at 2.74 m (108 in)

(b) Rod Cladding Heat Flux at 2.69 m (106.1 in)

(c) Vapor Temperature at 2.54 m (100 in)

(d) Spacer Grid Temperature at 2.80 m (110.27 in)

Figure 4-10: Effect of Rod Bundle Power Input on the Rod Bundle Heat Transfer Process
The inlet liquid injection rate is also a very important system parameter during reflood transients since it directly determines the amount of liquid that can be injected into the bundle. Figure 4-11 shows the experimental results collected for three different inlet flooding rate at 0.0191 (0.75), 0.0254 (1) and 0.0508 (2) m/sec (in/sec), respectively. Increasing the flooding rate will changes the rod bundle behavior significantly. As can be seen from Figure 4-11 (a), for lower flooding rate test Exp. 7151, after initial injection, the rod cladding temperature kept increasing to a maximum value (or turn around value) before starting to decrease. This phenomenon indicates that under current mass flow rate, the two-phase flow mixture doesn’t have sufficient heat removal capability after initial liquid injection until a later time when more and more liquid droplets can be entrained to enhance the heat transfer process. Therefore, for this kind of reflood test, the PCT is the turn around temperature. On the other hand for Exp. 7116 with higher flooding rate, after very short temperature increase that corresponds primarily to single phase vapor heat transfer upon initial injection, the rod cladding temperature was found to decrease continuously. No turn around was observed and thus the PCT is the initial maximum temperature, which usually took place between 0-20 sec. The heat flux variations near peak power location are shown in Figure 4-11 (b). High flooding rate induced high heat flux before quench. In addition, the temperature variations for the vapor phase and spacer grids showed a similar trend as the rod cladding temperature. Due to liquid droplet deposition and liquid film formation, it can be clearly observed that the spacer grid quenched much earlier than the vapor temperature.

Last, the effect of inlet liquid subcooling temperature is presented in Figure 4-12. In the current RBHT reflood test, the degree of liquid subcooling has a wide range between 11(20) - 83(150) K (°F). The four tests selected for comparison spread over the entire range. It can be seen that the initial values for rod cladding temperature, heat flux, vapor phase temperature and spacer grid temperature were quite similar. As a result, it can be concluded that the inlet liquid
subcooling has minor effect on the rod bundle heat transfer behavior above quench front where DFFB regime is expected. Nevertheless, it should be noted that higher inlet subcooling increased the quench front rate and had faster quench. Also, relatively larger oscillations were observed in rod surface heat flux and vapor temperature for higher inlet subcooling tests.

Figure 4-11: Effect of Inlet Liquid Flooding Rate on the Rod Bundle Heat Transfer Process

As the quench front propagated upward, the effect of subcooling was eventually observed due to the fact that the inlet liquid temperature would affect the nucleate boiling and vapor
generation/departure processes below the quench front, which in turn would affect the vapor mass flow rate and liquid entrainment rate above the quench front.

(a) Rod Cladding Temperature at 2.74 m (108 in)

(b) Rod Cladding Heat Flux at 2.69 m (106.1 in)

(c) Vapor Temperature at 2.54 m (100 in)

(d) Spacer Grid Temperature at 2.80 m (110.27 in)

Figure 4-12: Effect of Inlet Liquid Subcooling Temperature on the Rod Bundle Heat Transfer Process
4.4 Spacer Grid Pressure Drop in the DFFB Regime

4.4.1 Determination of Spacer Grid Pressure Drop from RBHT Data

In a two-phase flow system the pressure loss caused by flow blockages such as spacer grid accounts for a large portion of the overall pressure drop, especially when the two-phase flow is in the DFFB regime in which the vapor is the continuous phase, as will be shown in the following sections. Therefore, accurate prediction of the two-phase flow mass and heat transfer as well as the rod bundle thermal-hydraulic behavior during reflood requires accurate characterization of the spacer grid thermal-hydraulic behavior. It is of crucial importance to have a comprehensive and in-depth understanding of the pressure loss induced by spacer grid under various reflood conditions in the DFFB regime. In this section, the experimentally measured pressure drop induced by spacer grid is investigated in detail while a new spacer grid pressure drop correlation will be developed in Section 5.5.

As discussed in Section 3.2, there are 23 DP transducers installed in the experimental facility. They cover the different axial spans over the entire axial length of the test section. In the current study, the pressure drop at the SG 5 location will be examined because SG 5 is located at the upper location (2.175 m) of the test section, closer to the peak power location (2.743 m). Thus, the period for dispersed flow film boiling regime to exist during reflood is expected to be longer at this location such that measurement and analysis could be performed. Also, the arrangement of DP cells near the SG 5 location gives us the privilege to obtain the pressure drop due to SG by subtracting the bare bundle pressure drop values from the pressure drop measured across SG 5. Figure 4-13 presents the sketch of the arrangement of DP cells near SG 5. As can be seen, there is one DP cell covering the length of 0.203 m across the SG 5, measuring the pressure drop at spacer grid location. Eq. (4.3) shows the different components measured by this DP cell.
It consists of pressure loss due to friction, acceleration, and gravity in addition to the form loss of SG. On the other hand, two DP cells that cover the length of 0.102 m are located right above and below SG 5 and measures the bare bundle pressure drop. Components of these two DP cell measurements are shown in Eq. (4.4) below as well. For a bare bundle, only friction, acceleration and gravity pressure loss contribute to the experimentally measured pressure drop. Therefore, the SG pressure drop can be obtained by subtracting the bare bundle pressure drop from the total pressure drop at the SG location, as given by Eq. (4.5).

\[
\Delta P_{exp,SG} = \Delta P_{fric} + \Delta P_{acc} + \Delta P_{grav} + \Delta P_{SG} \tag{4.3}
\]

\[
\Delta P_{exp,bare} = \Delta P_{fric} + \Delta P_{acc} + \Delta P_{grav} \tag{4.4}
\]

\[
\Delta P_{SG} = \Delta P_{exp,SG} - \Delta P_{exp,bare,above} - \Delta P_{exp,bare,below} \tag{4.5}
\]

where,

\(\Delta P_{exp,SG}\) = measured pressure drop of a certain length including SG 5;

\(\Delta P_{exp,bare}\) = pressure drop at bare bundle location (either above or below SG 5);

\(\Delta P_{SG}\) = pressure drop due to SG 5.

**Figure 4-14** shows the pressure drop measured during the reflood for selected DP cells.

For all DP cells, the measured pressure drop is initially relatively small and nearly constant in the DFFB regime. When the quench front passes by a DP cell pressure tap, a significant rise in the pressure drop can be observed, indicating that liquid phase becomes the dominant phase since the static head for liquid phase is larger. Due to the longer span of DP cell and the existence of SG structure, pressure drop measured across SG is found to be larger than the other two DP cells. Note that DP cell readings above and below SG 5 are quite similar since both of them have a 0.102 m span. The difference between these two locations is mainly due to the mass quality and fluid property variation reflecting the linearly increased power distribution with the peak value at
2.742 m. Therefore, the pressure drop induced by SG 5 can be obtained by applying Eq. (4.5) on DP cell measurements.

Having determined the SG pressure drop for the entire reflood period, further analysis is needed in order to identify the DFFB regime. This can be achieved by examining the rod bundle quench profile at SG 5 location, as shown in Figure 4-15. The upper subplot presents the rod surface temperature variation near SG 5 location together with the quench front propagation during reflood. It can be seen that initially the rod temperature is well above the Leidenfrost point ($T_{min}$) such that no direct liquid-wall contact is expected, indicating DFFB occurs during this time period (about 20~330 sec). Later on when the quench front approaches to SG 5 location, rod bundle cladding temperature decreases below the Leidenfrost point gradually and eventually quench (indicated by significant rod surface temperature drop). The $T_{min}$ point is determined by the lowest heat flux in post-CHF regime on the boiling curve. By plotting the pressure drop measurements as a function of time in a similar way, the portion in the DFFB regime can thus be identified. The different heat transfer regimes are marked out in the lower subplot of Figure 4-15. Beside the approach discussed above, the DFFB regime can also be visually identified through pictures taken by video cameras installed at the upper portion of the test section.
In literature, the commonly used approach for the pressure drop across an obstacle such as SG, support plate or orifice is to correlate the pressure drop with a coefficient as shown below:

$$\Delta P_{SG} = C_{SG} \frac{\rho v^2}{2}$$ \hspace{1cm} (4.6)

where $C_{SG}$ is the spacer grid pressure loss coefficient. Its value is dependent on the SG geometry, fluid Reynolds number, and is often determined by experiments. The quantity $v$ represents the flow mixture velocity in the rod bundle. Rewriting and rearranging Eq. (4.6) for two-phase flow mixture yields Eq. (4.7) below. In this study, SG pressure drop comparisons are made by comparing the SG loss coefficient, $C_{SG}$, for different test conditions.

$$C_{SG} = \frac{2\Delta P_{SG}}{\rho_{mix} v^2} = \frac{2\Delta P_{SG} \rho_{mix}}{G^2}$$ \hspace{1cm} (4.7)

In Eq. (4.7), $G$ is the mass flux; $\rho_{mix}$ is the mixture density in the DFFB regime and is calculated as,

$$\rho_{mix} = (1 - \alpha) \rho_{liq} + \alpha \rho_{vap}$$ \hspace{1cm} (4.8)
where, $\alpha$ is the two-phase flow void fraction and is calculated using the exact quality-void fraction correlation given in Eq. (4.9) below:

$$\alpha = \frac{1}{1 + \left(\frac{1-x}{x}\right) \left(\frac{\rho_{\text{vap}}}{\rho_{\text{liq}}}ight) S}$$

where $x$ is the mass flow quality and $S$ is the slip ratio. The two-phase mixture density strongly dependent on the void fraction. In the current analysis, the homogeneous flow model ($S = 1$) is selected for the void fraction calculation since it yields better results for low pressure conditions (Beattie 1973 [68], Andeen and Griffith 1968 [26], Unal et al. 1994 [89]). In the DFFB regime, however, the two phases are in highly thermal-hydraulic non-equilibrium state. Previous studies have shown that the slip ratio between two phases is generally around 1.5 to 3 [100, 101]. For this reason, $S = 2$ is also assumed in the void fraction calculation for comparison. In Eq. (4.9), $\rho_{\text{liq}}$ and $\rho_{\text{vap}}$ are the liquid phase and vapor phase density, respectively.

Figure 4-14: DP Cell Measurements near SG 5 Location
Figure 4-15: Identification of DFFB Regime for DP Cell Measurements near SG 5 Location

The flow quality, $x$, for the dispersed flow mixture is obtained using a one-dimensional energy balance along the test section based on the rod bundle instant heat flux distribution data, inlet liquid mass flow rate as well as the measured vapor temperature. The liquid phase is assumed to be at saturation in the DFFB regime. The mixture properties are also used for the value of Reynolds number:

$$ Re = \frac{\rho_{mix}vD}{\mu_{mix}} = \frac{GD}{\mu_{mix}} \quad (4.10) $$

where $\mu_{mix}$ is the two-phase flow mixture viscosity and is determined by McAdam (1942) [102] equation,

$$ \mu_{mix} = \left( \frac{1-x}{\mu_{liq}} + \frac{x}{\mu_{vap}} \right)^{-1} \quad (4.11) $$

where $\mu_{liq}$ and $\mu_{vap}$ are the liquid and vapor viscosities, respectively. In order to obtain the the Reynolds number in the DFFB regime, the two-phase mass flow rate above the quench front was
determined by subtracting the liquid storage rate below the quench front from the total inlet mass flow rate. The liquid storage rate was obtained from the DP cell measurements.

4.4.2 Results and Discussion

This section presents the effects of various parameters on the two-phase SG pressure drop in the DFFB regime. The effects of four different parameters are compared and analyzed in detail. These are: the operating system pressure, inlet liquid subcooling, inlet liquid flooding rate and the rod bundle peak power. The results are presented in terms of the SG pressure drop loss coefficient as a function of the flow mixture Reynolds number defined in Eq. (4.10). The void fraction is determined using both the homogeneous equilibrium model (HEM) and assuming the slip ratio \( S \) of 2 for comparison. Therefore, for each test condition, the SG loss coefficients calculated for the HEM model and the case when \( S = 2 \) are plotted.

The effect of system pressure on the SG loss coefficient is shown in Figure 8. As can be seen from Table I, three reflood tests have been selected for the pressure study, with the pressure ranging from 137.90 kPa to 413.69 kPa. It is observed that the SG loss coefficient predicted from the method based on different phase velocities (Figure 4-16 (b)) is larger than the value predicted by the HEM model (Figure 4-16 (a)). This is mainly due to the homogeneous assumption made in HEM, which has the slip ratio of one. In reality, due to the highly thermal-hydraulic non-equilibrium in DFFB, the vapor and liquid phase are not flowing at the same velocity. Also, the vapor phase is generally in the superheated condition, indicating that the actual mass flow quality is smaller than the equilibrium quality. This difference will also lead to a smaller void fraction. From Eq. (4.9) it is known that with a slip ratio larger than one, a relatively smaller vapor void fraction and larger mixture density are predicted, resulting in a larger SG pressure drop.
Nevertheless, despite of the difference in the loss coefficient predictions in these two cases, a clear trend of system pressure effect on the grid loss coefficient can be identified from both Figure 4-16 (a) and (b). Pressure also has a significant effect on the two-phase void fraction calculation. For a higher system pressure, a lower void fraction is predicted when the quality is the same. In the current study, all of the reflood tests are conducted at relatively low pressure conditions, it is observed that increase in the system pressure value will shift the data to the upper right location, which corresponds to larger Reynolds numbers. For the same value of Reynolds number, on the other hand, a high system pressure tends to have a larger grid loss coefficient. It should be noted that even though the RBHT test facility is equipped with a pressure oscillation damping tank, non-negligible oscillations in pressure have been observed during the test for Exp. 8023, which has the highest system pressure (413.69 kPa). For this reason, more scatter in data is observed, as shown in Figure 4-16.

*Figure 4-17* shows the effect of inlet liquid subcooling on the SG loss coefficient in DFFB. Four tests are used to compare the liquid subcooling over a wide range from 11 to 83 K. In the present study, no direct relation between the inlet subcooling and grid pressure loss is observed. This is mainly due to the fact that the current work is focused on the SG 5 (2.176 m), which is located at the upper portion of the test section. Due to constant heat input throughout the test, the bulk liquid has already reached saturation well below SG 5 even for the test with the highest subcooling of 83 K (around 1.5 m). As a result, no significant effect of inlet liquid subcooling on the grid loss can be identified.
The three tests selected for inlet liquid flooding rate study are Exp. 7151, 8011 and 7116 with inlet liquid velocity being 0.0191, 0.0254 and 0.0508 m/sec, respectively. Figure 4-18 gives the results of flooding rate effect for the two cases. As expected, significant differences are observed as the Reynolds number increases, indicating the grid loss coefficient strongly depends on the mixture mass flow rate. For this reason, many previous empirical correlations tried to express the loss coefficient as a function of the Reynolds number.

Figure 4-19 presents the effect of different rod bundle power inputs on the SG pressure losses in DFFB. Comparison of selected three tests shows that, even though the largest peak power difference in the current study is about 1 kW/m, similar results as well as scatter are obtained for the SG loss coefficient. In a heated flow channel such as the rod bundle, power input mainly dictates the thermal behaviors of the two phases by affecting the mass flow quality, extent of vapor superheat, void fraction and slip ratio. However, in the DFFB regime in which the vapor phase is the dominant phase and especially under low pressure condition, very large values of void fraction are expected (typically above 0.99). Thus, any further change in the void fraction and thus in subsequent two-phase mixture density is relatively insignificant when compared with
a slight change in hydraulic effect such as the flooding rate. This is the reason why in Figure 4-19 the results for these three tests overlap with each other.

![Image of Figure 4-17: Effects of Inlet Liquid Subcooling on SG 5 Pressure Loss](image1)

![Image of Figure 4-18: Effects of Inlet Liquid Flooding Rate on SG 5 Pressure Loss](image2)

It should be noted that, despite of the four effects discussed above, it has been reported in several studies that the SG blockage ratio also has a significant effect on the SG pressure drop behavior within the rod bundle ([79], [80] and [81]). In the current analysis, however, the blockage ratio of the RBHT test section is fixed at 0.362, thus this factor cannot be evaluated.
The current RBHT SG pressure drop data is also compared with experimental data and correlation reported in the literature. Figure 4-20 presents the RBHT data based on the slip ratio of 2 for different flooding rates together with Unal et al.’s data [82]. As can be seen, Unal’s results for the post-CHF heat transfer regime are slightly higher than the current RBHT data. Major differences between these two experiments may be accountable for the discrepancy. a). a $3 \times 3$ rod bundle geometry with the O.D. of rods being 9.7 mm was used in Unal’s experiments, whereas the current RBHT tests are performed in a $7 \times 7$ rod bundle with an O.D. of rods being 9.5 mm; b). the SGs used for these two tests are very different; c). the inlet mass flux and the flow quality reported in Unal’s data are also different from those in the current RBHT data. Therefore, it is difficult to make a direct comparison between the two sets of data. Nevertheless, the general trends of grid loss coefficient as a function of the Reynolds number in the DFFB for these two tests are very similar.

Figure 4-20 also presents the RBHT single-phase liquid SG loss coefficient [24] and the single-phase grid loss predicted by Cevolani correlation [77] with $\epsilon = 0.362$, which is based on Rehme’s data and method [74, 75 and 76]. It is seen that Rehme’s method under-predicts the single-phase grid loss coefficient significantly. This is similar to what was obtained in the
FLECHT-SEASET experiments [28]. Yao et al. 1982 [85] also reported that Rehme’s method tends to under-predict the experimental data and they found that a 50 percent higher value of the modified grid loss coefficient appears to be more appropriate. Moreover, it can be observed from the figure that the current grid loss coefficient in the DFFB regime is always smaller than the corresponding single-phase grid loss coefficient. This is because under the current test conditions at a given mixture mass flux, instead of liquid phase, the superheated vapor phase is the continuous phase, which has a viscosity of one order of magnitude smaller, but a much larger velocity. Therefore, the SG pressure loss coefficient induced by the two-phase flow mixture is smaller.

![Figure 4-20: Comparisons of Spacer Grid Pressure Drop Coefficients](image)

4.5 Experimental Investigation on the Liquid Droplet Behavior

Under internal flow conditions such as flow within a tube or a rectangular duct, the concentration of the dispersed droplets over the cross-section of channel usually has a distribution. In the study performed by Ganic and Rohsenow 1976 [21], the droplet trajectory in
dispersed flow regime was predicted through boundary layer analysis. Their results indicated that only those droplets having obtained sufficient transverse momentum in the turbulence core could penetrate the viscous layer. Later, Hagiwara et al. 1980 [38] experimentally showed the existence of concentration profiles in adiabatic tubes.

4.5.1 Droplet Distribution within a Sub-Channel

The radial distribution of droplet number concentration across the rod bundle geometry measured by the imaging system at 2.743 m location during reflood is shown in Figure 4-21. The radial distance has been normalized according to the distance between the center of the channel and channel boundary. As a result, droplet at the center of the flow channel has radial distance of 0, while droplet at boundary has radial distance of ±1. The droplet sampling was taken between 20 to 40 sec period after the initial water injection. During this period, the two-phase flow heat transfer regime was identified as the DFFB. In total there were 446 liquid droplets being captured. Their number distribution is plotted against the normalized radial distance away from the center of the channel. It can be clearly seen that the dispersed phase tends to stay in the center of the channel while its concentration gradually vanishes as one moving away from the center. In Figure 4-21, the early decrease of the droplet concentration on both sides is due to the measurement limitation and the rod bundle geometry. Due to the border rejection and shape rejection setup for the image processing, any droplet that is in direct contact with the border or simply be hidden by the rod bundle is rejected.

The droplet velocity distribution in the radial direction at 2.743 m during 20-40 sec period is presented in Figure 4-22. Even though the droplet number concentration has a distribution over the cross-section of flow channel, the droplet velocities, on the other hand, were found to be rather independent of their distances from the wall. This uniformity was also
discussed by Andreani and Yadigaroglu 1994 [3] in their review for the DFFB regime. As a result, the average of measured droplet velocity is considered as a satisfactory representation for the cross-sectional liquid phase velocity at the corresponding axial location. Nevertheless, at a specific radial location, large scatter in data was observed in Figure 4-22. This is partially due to the effect of various droplet sizes. As pointed out by Jin et. al. (2018) [33], these droplets generally have a log-normal distribution in size. At the same radial location, smaller droplets are expected to move faster than larger droplets due to smaller interfacial drag. Another reason that also may contributes to the scatter is that, instead of a steadily propagating quench front, under current low flooding rate experimental conditions, oscillations in the system pressure and quench front propagation have been observed. This periodic behavior leads to a periodic increase and decrease in both the vapor and droplet velocities downstream of the quench font. Such oscillating effects were also reported in other system effects tests, such as FLECHT [39], FLECHT-SEASET [40, 41, 42], LOFT [43], and JAERI Series 5B tests [44]. As a result, even for droplets of the same size at the same radial location, their velocities will change with time, manifesting a temporal effect. Last, the present droplet measurement approach based on the imaging processing technique might be affected by local turbulence flow since only a small area is sampled. Therefore, the measured droplet velocity has a scatter as shown in Figure 4-22.

Figure 4-23 shows the droplet velocity profile as a function of droplet size for the same sampling period. As expected, larger droplets generally travel at a much slower velocity than those of smaller droplets. The velocity difference could be as large as ~10 m/sec between the largest and smallest droplet. This decreasing droplet velocity as a function of droplet size is different from the results obtained by previous studies such as the work performed in tubes for annular-mist flow by Wilkes et al. (1983) [45], the work for dispersed flow above quench front by Ardron and Hall (1981) [46], and the works performed in rod bundles (Lee et al. 1982 [47]; McMinn et al. 1988 [48]), in which no significant velocity difference was observed. Part of the
reason is attributed to the more advanced and accurate droplet size and velocity measurement method using the imaging processing technique adopted in the RBHT test facility. In addition, the RBHT test facility is able to produce the reflood phase of very long duration, which also facilitates the droplet measurement. It should also be noted that the scatter in the droplet velocity for droplets having similar size is not due to the measurement uncertainty but due to the intrinsic oscillating nature of the reflood process. For a low pressure, low flooding rate test, the quench front propagation is actually oscillating instead of a steady movement upward. The quench front moves upward in a step by step manner. As a result, the droplet velocity so measured also exhibits an oscillating nature, leading to the scatter observed.

Figure 4-21: Measured Radial Distribution of Droplet Concentration between 20-40 sec, Exp. 7151
Figure 4-22: Measured Radial Distribution of Droplet Velocity between 20-40 sec, Exp. 7151

Figure 4-23: Measured Droplet Velocity vs. Droplet Size between 20-40 sec, Exp. 7151
4.5.2 Spacer Grid Effects on the Droplet Field

Figure 4-24 and Figure 4-25 present the normalized droplet number frequencies against droplet size for dry SG 6 at 20-40 sec time span and for wet SG 6 at 580-600 sec time span, respectively. The droplet distributions, averaged droplet sizes, and Sauter mean diameters are shown. It can be seen that the droplet size decreased from upstream (402 μm) location to downstream (259 μm) location for a dry SG 6. The opposite trend was observed for a wet SG 6, droplet size increased from the upstream (300 μm) location to downstream (688 μm) location. This clearly indicates that for a dry spacer grid, upon impingement of liquid droplets, cutting and shattering occur which create large amount of smaller droplets that are more thermally active due to the increased interfacial heat transfer area. However, once SG 6 is quenched, a liquid film would form on the grid surface which provides a mass source for liquid ligament generation at the trailing edge of spacer grid. These ligaments would be further sheared off by the vapor flow to form large droplets downstream of the spacer grid.

Note that upon quenching of SG 6, the upstream droplet size remained larger than that of downstream until, at a later time, the downstream droplet size finally became larger. Two reasons that might be responsible for this delayed response in droplet size are stated: a) It takes time for the liquid film on SG 6 to accumulate and grow to a so-called “equilibrium thickness” beyond which any additional liquid mass deposited onto the spacer grid will be responsible for the liquid ligaments shearing and subsequent breakup; b) due to the highly transient and localized effects, it is possible that SG 6 could still be partially wet though all six of the TC’s indicated that full quenching was achieved. After a certain time period, the wet grid effect started to take place and finally dominated the droplet generation process, as can be seen from Figure 4-25 below.
A further investigation of the experiment results shows that for a dry SG, the downstream droplet size is closely related to that of the incoming droplet size. In other words, the size of
incoming droplets at the upstream location will dictate the size of outgoing droplets at the
downstream location. Since wet grid droplet generation results from liquid ligament shearing off
at the SG trailing edge and subsequent aerodynamic breakup, the size of the droplets generated do
not depend strongly on the incoming droplet size. Based on this argument, a wet grid droplet
generation model can be developed that may not be sensitive to the incoming droplet size.

Nevertheless, one should keep in mind that when calculating the Sauter mean diameter
downstream of a wet spacer grid it is necessary to add both the newly generated droplets and the
incoming droplets that escape through the SG without impaction. However, since the presence of
a liquid film will decrease the flow area through a wet grid, it is more difficult for droplets to pass
through the spacer grid without deposition.

4.5.3 Effect of Quench Front Location on the Droplet Field

In addition to the spacer grid temperature, the quench front location relative to the point
of droplet measurement, also has a significant effect on droplet size. Under reflood conditions, a
majority of the liquid droplets are generated at the liquid-vapor interface due to entrainment as
well as at the quench front location due to vigorous evaporation and liquid sputtering. Hence, as
the quench front moves toward the droplet measurement location, droplet sizes are expected to be
comparatively larger.

The droplet in-focus counts as a function of time measured by the VisiSize system every
20 sec are presented in Figure 4-26. The vertical dashed lines in Figure 4-26 indicate the quench
times of each SG, which are labeled in square boxes. The starting and ending quench times are
indicated by TC’s attached to SG 6. The horizontal dashed line shows the axial elevation of SG 6
in the rod bundle.
As shown in Figure 4-26, before 421 sec, SG 6 was dry until it became wet after 472 sec. However, the quench front did not reach SG 6 location until at a much later time around 650 sec. It can be observed from the figure that both the up- and down-stream droplet counts decreased as the quench front propagated upward. Once the quench front approached to the measurement location, the in-focus counts dropped to significantly low numbers to the extent that bias of the data might occur. The main reason for such a low droplet count is that when the quench front approaches the measurement locations, droplets become much larger in size and are most likely to be in non-spherical shape. It is possible that many larger droplets might be either in direct contact with the borders of measurement area and/or be partially hidden by the rods. Moreover, liquid slugs may also occur in the flow path, blocking the viewing area. These situations make it difficult for the imaging system to recognize droplets and to obtain adequate droplet counts with high accuracy for meaningful statistical analysis. As a result, relatively high uncertainties exist in the droplet size and distribution measurements during the late phase of reflood due to the low droplet counts.

In Figure 4-27, the droplet Sauter mean diameter as a function of the quench front location during the entire reflood process is shown for Exp. 7157. Again the spacer grids quench times are particularly marked out. It can be seen that as long as SG 5 was dry, both the up- and down-stream droplet Sauter mean diameters of SG 6 increased accordingly as the quench front propagated upward. Once SG 5 was quenched, the upstream droplet Sauter mean diameter experienced a sudden increase with large fluctuations while the downstream Sauter mean diameter of SG 6 had not yet felt the effect. Later after the quench of SG 6, the downstream Sauter mean diameter experienced the same variation as that of the upstream location.
In Figure 4-27, the droplet Sauter mean diameter as a function of the quench front location during the entire reflood process is shown for Exp. 7157. Again the spacer grids quench...
times are particularly marked out. It can be seen that as long as SG 5 was dry, both the up- and
down-stream droplet Sauter mean diameters of SG 6 increased accordingly as the quench front
propagated upward. Once SG 5 was quenched, the upstream droplet Sauter mean diameter
experienced a sudden increase with large fluctuations while the downstream Sauter mean
diameter of SG 6 had not yet felt the effect. Later after the quench of SG 6, the downstream
Sauter mean diameter experienced the same variation as that of the upstream location.

As the quench front propagates upward, droplets generated at the quench front would
need less time to travel to the droplet measurement location and thus less liquid mass would be
evaporated during the flight, resulting in larger incoming droplets and outgoing droplets at SG 6.
The sudden increase in the droplet Sauter mean diameter is attributed to be due to the quench of
SGs. After a SG rewets, a different droplet generation mechanism takes over as discussed
previously. The results obtained in this study clearly show the important effects of quench front
propagation and SG conditions on the droplet field.

![Figure 4-28: Droplet Sauter Mean Diameter vs Quench Front Location during Reflood, Exp. 7151](image-url)
Figure 4-28 and Figure 4-29 present the droplet Sauter mean diameters as functions of the quench front during reflood for Exp. 7151 and Exp. 7095, respectively. Similar droplet behaviors were observed for these two tests, indicating that the trends observed in Exp. 7157 were repeatable. However, due to the different flooding rates and inlet subcooling temperatures, the time spans of quench front propagation along the rods as well as at the local SG locations were slightly different. In Exp. 7095, due to a much higher inlet water subcooling, the test section was found to be fully quenched much earlier than in the other two experiments, as shown in Figure 4-29.

Figure 4-29: Droplet Sauter Mean Diameter vs Quench Front Location during Reflood, Exp. 7095

In all of the experiments explored in this study, the dramatic drop in up- and down-stream Sauter mean diameter after the quench of SG 6 near 600-700 sec after reflood is attributed to the uncertainty of droplet measurement, since the quench front was very close to the camera locations. For the reasons discussed previously, the number of droplets being captured by the imaging system decreased substantially at late stage of reflood, leading to large uncertainties of
droplet measurement. As a consequence, any results obtained within this time period are statistically meaningless and have been discarded in the current analysis.

4.5.4 System Parametric Effects on the Droplet Field

The effects of reflood test initial conditions on the incoming droplet size are analyzed and discussed in this section for selected tests. The controlling parameters include the system pressure, degree of inlet subcooling, flooding rate as well as the rod bundle peak power. Droplet results obtained using the Oxford Lasers Firefly Imaging system at the upstream of SG 6 are used for comparison. In this section, the droplet size is expressed by the droplet Sauter mean diameter as a function of the normalized time after reflood. The reason for using the normalized time instead of the actual time is that the reflood ending time varies due to different test conditions. For example, Exp. 7116 (0.0508 m/sec (2 in/sec)) ended at around 384.7 sec, while Exp. 7174 (0.0191 m/sec (0.75 in/sec)) ended at around 877 sec after start of reflood. It is inappropriate to compare directly the incoming droplet size of SG 6 at an actual time, say, 100 sec for these two tests, since their quench front axial locations within the rod bundle at 100 sec are quite different.

As discussed in section 4.1, the quench front propagation has a significant effect on the droplet size and velocity variations. For this reason, the actual reflood time has been normalized according to the total quench time of each test to minimize the quench front propagation effect such that droplet sizes and velocities from different tests can be compared at about the same quench front location.

Table 4-1 presents the tests conditions for the selected tests. The effect of system pressure on the incoming droplet size is shown in Figure 4-30 using Exps. 8021 138 kPa (20 psia), 8009 276 kPa (40 psia) and 8023 414 kPa (60 psia). In the early stage of reflood, no obvious trend has been observed and the droplet sizes from these three tests are comparable.
However, for Exp. 8023 a sudden increase in the droplet size is observed at the normalized time around 1.8. This is possibly due to the change of SG condition upstream of the measurement location.

Table 4-1: Test Conditions for Selected Reflood Tests for Liquid Droplet Comparison

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Pressure kPa (psia)</th>
<th>Flooding Rate m/sec (in/sec)</th>
<th>Inlet $\Delta T_{sub}$ K ($^\circ$F)</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Initial PCT K ($^\circ$F)</th>
</tr>
</thead>
<tbody>
<tr>
<td>8021</td>
<td>137.90 (20)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8009</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8023</td>
<td>413.69 (60)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7157</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7173</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8011</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7095</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>83 (150)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7166</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>83 (150)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7151</td>
<td>275.79 (40)</td>
<td>0.0191 (0.75)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7174</td>
<td>275.79 (40)</td>
<td>0.0191 (0.75)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7116</td>
<td>275.79 (40)</td>
<td>0.0508 (2)</td>
<td>56 (102)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7112</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>51 (90)</td>
<td>0.98 (0.3)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7168</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>51 (90)</td>
<td>0.98 (0.3)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8013</td>
<td>275.79 (40)</td>
<td>0.0254 (1)</td>
<td>53 (96)</td>
<td>1.97 (0.6)</td>
<td>1033 (1400)</td>
</tr>
</tbody>
</table>

Table 4-2: SG 5 Partially Wet Period of Selected Tests for Pressure Effect

<table>
<thead>
<tr>
<th>Test No.</th>
<th>System Pressure kPa (psia)</th>
<th>Quench Time (sec)</th>
<th>SG 5 Partially Wet Period Actual time (sec)/Normalized time (-)</th>
</tr>
</thead>
<tbody>
<tr>
<td>8021</td>
<td>138 (20)</td>
<td>1334</td>
<td>282-521/0.21-0.39</td>
</tr>
<tr>
<td>8009</td>
<td>276 (40)</td>
<td>736</td>
<td>177-244/0.24-0.33</td>
</tr>
<tr>
<td>8023</td>
<td>414 (60)</td>
<td>603</td>
<td>108-183/0.18-0.3</td>
</tr>
</tbody>
</table>

Table 4-2 gives the total quench times and the SG 5 partially wet periods for the selected tests. As can be seen, SG 5 for Exp. 8023 started to quench at the normalized time around 0.18,
which corresponds to the sudden change of droplet size. On the other hand, similar droplet sizes were observed for Exp. 8021 and 8009. In the FLECHT-SEASET reflood tests, the system pressure effect was compared within a smaller range of 20 to 40 psia [2] The effects of pressure on the droplet mean diameter was insignificant in the FLECHT-SEASET test series as well.

The system pressure effect on the incoming droplet average velocity is shown in Figure 4-31. In general, the liquid droplet velocity decreases as the quench front approaches the measurement location. As can be seen, a higher system pressure will lead to a lower incoming droplet average velocity upstream of SG 6. This is because the average droplet size for higher pressure test is larger. In addition, the rate of liquid mass initially stored within the rod bundle during reflood is larger for higher pressure test, resulting in a smaller vapor mass flow upward. As a result, the average velocity for entrained liquid droplets is also smaller and less efficient cooling at upper portion of rod bundle can be expected for Exp. 8023. The rod surface temperature variation for these three tests further justified this argument. However, due to the rapid liquid accumulation below quench front, Exp. 8023 is found to be fully quenched at a much earlier time (577.5 sec) than that of Exp. 8021 (1332.6 sec).

The effect of inlet liquid subcooling on the droplet size and average velocity are presented in Figure 4-32 and Figure 4-33, respectively. The tests selected cover the range of subcooling from 11 K (20 °F) to 83 K (150 °F). Table 4-3 shows the SG 5 conditions for these tests. The SG 5 partially wet periods for different tests are similar. It can be seen from Figure 4-32 that there is no clear trend showing for the inlet subcooling effect. There is a significant variation in the droplet diameter among all the tests compared in Figure 4-32. This is probably because that the bulk liquid temperature has already reached saturation far below the droplet measurement location even for the highest inlet subcooling test Exp. 7095 and Exp. 7166 (83 K/150 °F) (around 1.5 m/59 in), according to a one-dimensional energy balance calculation. As a
consequence, droplets arriving at the upstream of SG 6 are saturated and the effect of inlet subcooling is insignificant.

Figure 4-30: Effect of System Pressure on the Incoming Droplet Sauter Mean Diameter

Figure 4-31: Effect of System Pressure on the Incoming Droplet Average Velocity
**Figure 4-33** shows the droplet average velocity variation for the same set of tests. Again, a general decreasing trend is observed during reflood despite of different initial test conditions. However, due to the reason discussed above, the effect of subcooling is not clear. Nevertheless, comparison made between Exps. 7157, 7173 (11 K/20 °F) and Exp. 7095, 7166 (83 K/150 °F) at later stage of reflood indicates that the droplet average velocity appears to be larger when the inlet subcooling is smaller. This phenomenon can be explained by considering the vapor generation in the rod bundle. Since the inlet liquid with lower subcooling is easier to be evaporated, more vapor generation below the quench front location and increased vapor velocity above the quench front are expected. However, such difference is relatively small in the early stage of reflood when the quench front is relatively lower; indicating that more steam is able to flow upward without condensation. Thus, the resulting steam velocity and droplet velocity are comparable.

In FLECHT-SEASET reflood tests, an increasing trend of droplet mean diameter was observed with increasing flooding rate. In RBHT reflood tests, however, the flooding rate effect is not clear. As can be seen in **Figure 4-34**, Exps. 7151, 7174, 8011 and 7116 are compared. The corresponding test conditions can be found in **Table 4-1** and the SG 5 condition can be found in **Table 4-4**. It is seen that the droplet Sauter mean diameters for different tests with different flooding rates are similar, indicating that no direct trend of flooding rate on the averaged droplet size are observed.

**Table 4-3:** SG 5 Partially Wet Period of Selected Tests for Subcooling Effect

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Subcooling K (°F)</th>
<th>Quench Time (sec)</th>
<th>SG 5 Partially Wet Period Actual time (sec)/Normalized time (-)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7157</td>
<td>11 (20)</td>
<td>828</td>
<td>267-314/0.32-0.38</td>
</tr>
<tr>
<td>7173</td>
<td>11 (20)</td>
<td>782</td>
<td>121-256/0.15-0.33</td>
</tr>
<tr>
<td>8009</td>
<td>22 (40)</td>
<td>717</td>
<td>177-244/0.25-0.34</td>
</tr>
<tr>
<td>8011</td>
<td>53 (96)</td>
<td>703</td>
<td>166-237/0.24-0.34</td>
</tr>
<tr>
<td>7095</td>
<td>83 (150)</td>
<td>691</td>
<td>209-255/0.30-0.37</td>
</tr>
</tbody>
</table>
### Table 4-4: SG 5 Partially Wet Period of Selected Tests for Flooding Rate Effect

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Flooding Rate m/sec (in/sec)</th>
<th>Quench Time (sec)</th>
<th>SG 5 Partially Wet Period Actual time (sec)/Normalized time (-)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7151</td>
<td>0.0191 (0.75)</td>
<td>959</td>
<td>334-363/0.34-0.38</td>
</tr>
<tr>
<td>7174</td>
<td>0.0191 (0.75)</td>
<td>877</td>
<td>230-276/0.26-0.31</td>
</tr>
<tr>
<td>8011</td>
<td>0.0254 (1)</td>
<td>703</td>
<td>166-237/0.24-0.34</td>
</tr>
<tr>
<td>7116</td>
<td>0.0508 (2)</td>
<td>385</td>
<td>61-117/0.16-0.30</td>
</tr>
</tbody>
</table>

![Figure 4-32: Effect of Inlet Liquid Subcooling on the Incoming Droplet Sauter Mean Diameter](image-url)

Figure 4-32: Effect of Inlet Liquid Subcooling on the Incoming Droplet Sauter Mean Diameter
Figure 4-33: Effect of Inlet Liquid Subcooling on the Incoming Droplet Average Velocity

![Droplet Average Velocity Graph]

Figure 4-34: Effect of Flooding Rate on the Incoming Droplet Sauter Mean Diameter

![Droplet Sauter Mean Diameter Graph]

**Figure 4-35** presents the effect of flooding rate on the incoming droplet average velocity for the three tests selected. It is found that the droplet average velocity appears to increase as the
flooding rate increases. The droplet average velocity for Exps. 7151 and 7174 (0.019 m/sec (0.75 in/sec)) is found to be significantly smaller than Exp. 7116 (0.051 m/sec (2 in/sec)) despite of the differences between the two tests. Since droplets are entrained in the steam flow, the above observation can be further justified by examining the steam mass flow rate at the measurement location, as is shown in Figure 4-36. As can be seen, the steam mass flow rate for the two tests with flooding rate of 0.019 m/sec (0.75 in/sec) are repeatable except that their quench times are different. Exp. 8011 with the flooding rate of 0.0254 m/sec (1 in/sec) has a mass flow rate larger than Exps. 7151 and 7174 with flooding rate of 0.019 m/sec (0.75 in/sec) but it is smaller than that of Exp. 7116 with flooding rate of 0.051 m/sec (2 in/sec). However, due to a higher inlet mass flow rate, Exp. 7116 quenched first.

Figure 4-35: Effect of Flooding Rate on the Incoming Droplet Average Velocity
Figure 4-36: Effect of Flooding Rate on the Steam Mass Flow Rate

The effect of rod bundle power input on the incoming droplet Sauter mean diameter and the average droplet velocity are presented in Figure 4-37 and Figure 4-38, respectively. Table 4-5 presents the SG 5 conditions after reflood for the selected tests. As can be seen, the effect of rod bundle power input shows no clear trend for the three tests selected. As for the incoming droplet average velocity, it seems that a higher power input leads to a higher droplet velocity due to a higher vapor generation rate and the resulting vapor velocity.

Table 4-5: SG 5 Partially Wet Period of Selected Tests for Peak Power Effect

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Quench Time (sec)</th>
<th>SG 5 Partially Wet Period Actual time (sec)/Normalized time (-)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7112</td>
<td>0.98 (0.3)</td>
<td>511</td>
<td>153-196/0.30-0.38</td>
</tr>
<tr>
<td>8011</td>
<td>1.31 (0.4)</td>
<td>703</td>
<td>166-237/0.24-0.34</td>
</tr>
<tr>
<td>8013</td>
<td>1.97 (0.6)</td>
<td>1200</td>
<td>333-427/0.28-0.36</td>
</tr>
</tbody>
</table>
Figure 4-37: Effect of Rod Bundle Peak Power on the Incoming Droplet Sauter Mean Diameter

Figure 4-38: Effect of Rod Bundle Peak Power on the Incoming Droplet Average Velocity
Chapter 5

Two-Phase Flow Model Development for the DFFB Regime

From the literature survey presented in Chapter 2, it can be seen that limited experimental data has been obtained for the two-phase flow thermal-hydraulic non-equilibrium, especially for the DFFB regime during reflood transients. Moreover, the thermal and hydraulic non-equilibrium between phases were often subjected to separate studies such that their coupled effects and interactions cannot be adequately accounted for. Finally, there does not have an obvious connection between the macroscale and microscale analyses for the two-phase mass and heat transfer process. For this reason, useful results obtained from microscale analyses cannot be readily transferred and applied to macroscale analyses. As an attempt to solve these difficulties existed in the study of two-phase flow behaviors and to better understand the fundamental thermal-hydraulic aspects involved in reflood transients, various two-phase flow models were developed in this Chapter.

5.1 Model Development for the Two-Phase Flow Thermal Non-Equilibrium

Reflood transient is a highly dynamic thermal-hydraulic phenomenon involving many coupled and interactive processes. During the entire reflood transient within a vertical rod bundle geometry, transition of the flow and heat transfer takes place from the single-phase liquid at the bottom all the way to the single-phase vapor or dispersed droplets in vapor flow at the top. Such flow regime transitions significantly alter both the heat transfer behavior and the relative motion between phases. Figure 5-1 shows a sketch of the different flow regimes encountered during a reflood transient. Above the quench front (post-CHF region or DFFB regime), typically there will
be three different kinds of heat transfer mechanisms: 1) radiation heat transfer among the rod surfaces, vapor and liquid droplets; 2) convective heat transfer between rod surfaces and vapor, and; 3) interfacial heat transfer between liquid droplets and vapor. Under certain circumstances, such as in the transition boiling regime, direct contact heat transfer between the rod surfaces and droplets may also contribute a significant portion to the total heat removal process. On the other hand, below the quench front (pre-CHF or bulk liquid boiling regime), mainly two-phase nucleate boiling or single-phase liquid convection will dominate the heat transfer process. The CHF is expected to occur at the dry-out point, which is accompanied by significant stored energy release from the rod bundle to the liquid.

The problem is very complicated in the DFFB regime due to its highly thermal-hydraulic non-equilibrium characteristics. In an upward flowing channel such as in rod bundle, the vapor phase is generally moving at a much faster velocity than the liquid droplets and, it is usually superheated due to heat addition from the wall. Therefore, accurate description of the flow and
heat transfer behaviors requires detailed characterization of the thermal-hydraulic non-equilibrium states.

As illustrated in Figure 5-1, flow regimes and/or heat transfer regimes have been divided into four regions in order to facilitate the analysis and to determine all the key parameters (e.g. mass quality, vapor superficial velocity, etc.) based on a 1-D mass and energy balance calculation. Region I corresponds to the single-phase liquid heat transfer regime whereas Region II corresponds to the nucleate boiling regime. These two regions are separated by the saturation point. Note that the subcooled boiling regime is neglected in the current analysis. The post-CHF regime is represented by Region IV, as shown in Figure 5-1(b). This region corresponds to the dispersed droplet flow regime. Last, Region III, which represents the region across the QF location, connects the pre-CHF and post CHF region. The mass quality along the bundle is calculated step-by-step in the axial direction using Eq. (5.1).

\[
\int_{z_1}^{z_2} n q'(z) dz = \dot{m}_{tot} \left[ (1 - x(z_2)) h_{liq}(z_2) + x(z_2) h_{vap}(z_2) \right] - \dot{m}_{tot} \left[ (1 - x(z_1)) h_{liq}(z_1) + x(z_1) h_{vap}(z_1) \right]
\]

(5.1)

where, \( q'(z) \) is the linear heat flux of each heater rod and \( n \) is the total number of heater rod (\( n = 45 \) for RBHT test). \( h_{liq}(z) \) and \( h_{vap}(z) \) are the liquid and vapor enthalpy at \( z \) location, respectively. The quantity \( x(z) \) is the mass quality, which will be determined at different axial locations. The quantity \( \dot{m}_{tot} \) is the corresponding two-phase mass flow rate in the control volume. For the pre-CHF region, \( x(z) \) is calculated bottom-up based on the inlet boundary conditions. For the post-CHF region, on the contrary, \( x(z) \) is calculated in a top-down manner in order to make use of the outlet boundary conditions, thus avoiding calculation across the CHF region, which usually involves large uncertainty. As such, the mass quality in Region III can be determined through a first-order linear interpolation between Region II and Region IV once the mass qualities are determined for the two regions.
Following this methodology, the actual mass quality, \( x \), along the entire test section can be adequately determined from experimental data. The equilibrium quality, \( x_e \), can be further determined by the requirement of energy balance and the equation is given as:

\[
X_r = \frac{x_e}{x_a} = \frac{h_{vap} - h_{liq}}{h_{fg}}
\] (5.2)

where, the ratio between \( x_e \) and \( x_a \) is expressed as \( X_r \), which is a useful indicator of the two-phase flow thermal non-equilibrium. The higher is the thermal non-equilibrium within a two-phase flow mixture, the larger the \( X_r \) would be. If the two phases are in thermal equilibrium, then \( X_r \) will reduce to unity.

Having determined the boundary conditions, the actual mass flow quality as well as the equilibrium quality at different axial locations of the test section can be calculated as a function of the reflood time. Applying the methodology described above, the calculated results for Exp. 7151 are shown in Figure 5-2 and Figure 5-3 for different quench front locations. The rod bundle heat flux and surface temperature are also plotted on the same figure for comparison. Since there are only thirteen steam probe rakes along the axial location, the vapor temperature was interpolated linearly according to the TC locations on the heater rods to have a direct comparison.

Figure 5-2 presents the results when the quench front was at 0.6656 m location, which corresponds to the early stage of reflood during which a large portion of the rod surface temperature was still well above the Leidenfrost point. Therefore, direct liquid-wall contact was precluded in the region far away from the quench front. From the heat flux subplot it can be seen that, different from the specified power input profile, the actual cladding heat flux had the largest value at the quench front location. Immediately above the quench front there was a region where the rod bundle heat flux was significantly higher than the specified value, indicating that very efficient heat transfer can be achieved between the rod surface and fluid mixture in this region due to strong turbulent mixing and liquid breakup. Possible liquid-wall direct contact heat transfer
is another contributing factor for this effective heat exchange. In addition, such highly transient flow behavior also leads to significant liquid evaporation, thus de-superheating the vapor. Consequently, in the region near the quench front location, the rod surface temperature is below the Leidenfrost point while the vapor temperature is close to saturation. Therefore, the actual mass quality was found to be close to the equilibrium quality, indicating that immediately downstream of the quench front the two-phase flow thermal-equilibrium exists. This delay of vapor superheat build-up downstream of the quench front was also observed by Chen (1986) [1], Unal (1988 and 1991) [11, 13] and the corresponding region is sometimes referred to as the froth regime or the near field. However, the situation deteriorates quickly as one moves away from the quench front, where it is referred to as the far field. As can be seen, in the upper portion of the rod bundle, heat flux actually went below the specified power generation, indicating that poor heat transfer exists and the rod surface temperature was still increasing. The vapor temperature was found to be very close to the wall temperature. In this region, the vapor phase was significantly superheated due to limited interfacial heat transfer (i.e., limited heat sinks). As a result, the actual mass quality deviated from the equilibrium quality at the upper locations above the quench front. The higher is the elevation, the higher is the vapor superheat.

As the quench front propagates upward, the temperature profiles for cladding and fluid as well as the two-phase flow mass quality will change accordingly. Figure 5-3 shows the mass quality axial variation together with the rod bundle heat flux and temperature profiles when the quench front was at 2.023 m. It can be observed that below the quench front the rod surface heat flux followed exactly the specified power profile and the rod surface temperature remained near the liquid saturation, indicating that once quenched, heat generated within the rod bundle can be readily removed by the two-phase flow mixture through single-phase liquid heat transfer and/or nucleate boiling. Since the liquid was initially subcooled, the equilibrium quality was found to be negative while the actual quality was zero (subcooled boiling regime was neglected). At the
quench front location, a rapid increase in both the actual mass quality and equilibrium quality can be observed due to significant vapor generation induced by instant heat release. Similar to Figure 5-2, the two phases were found to be close to thermal equilibrium near the quench front while significant vapor superheat built-up in the far field region. Accordingly, the actual mass quality deviated from the equilibrium quality rapidly and maintained the thermal non-equilibrium state.

At this time, the heat flux was larger than the specified power input value, indicating that the rod surface temperature started to decrease. It can also be observed that the axial temperature difference of the rod bundle surface was very significant at the quench front. Therefore, the effect of axial heat conduction within the rod has to be considered in order to accurately predict the temperature and heat flux transients in the quench front regimes.

Figure 5-2: Axial Variation of the Quality at the QF Location of 0.6656 m, Exp. 7151

It has been found that the thermal non-equilibrium is commonly encountered in the DFFB regime under various test conditions. The quality results for three other reflood tests are presented in Figure 5-4 for comparison. All these three tests were performed at the flooding rate
of 0.0254 m/sec. However, Exp. 7166 was performed with inlet liquid subcooling of 83 K whereas Exp. 8023 was performed at the system pressure of 413.69 kPa. Despite of these differences, it can be seen that significant thermal non-equilibrium exists above the quench front in all three tests, which confirms the observations made for Exp. 7151. In addition, it has been found that the flooding rate and the system pressure have non-negligible effect on the actual quality value as well as on the extent of thermal non-equilibrium, while the effect of liquid inlet subcooling was found to be rather insignificant.

![Graphs showing axial variation of quality](image)

Figure 5-3: Axial Variation of the Quality at the QF Location of 2.023 m, Exp. 7151

It should be mentioned that, the rod bundle heat flux variation above the quench front was actually oscillating, being sometimes larger or smaller than the specified power input value. In addition, its oscillating frequency increased gradually as the quench front moved closer to the outlet, indicating that there was a feedback mechanism playing between the quench front and the
Therefore, this oscillation is most possibly induced by the propagation of pressure and/or vapor density waves above the quench front since the vapor phase is highly compressible.

Figure 5-4: Comparison of Quality Variations among RBHT Reflood Tests

In addition to examining the mass quality axial variation for a fixed time instant, it is also very useful to compare the transient variation of mass quality at different axial locations. In Figure 5-5, the time variation of two-phase mass quality at three different axial locations are presented. One location was selected at the middle of the rod bundle (2.032 m), the other one at the peak power location (2.743 m) and the third one near the exit of the rod bundle (3.447 m). As expected, initially very high mass quality values were observed for all locations since during this time the entire rod bundle was almost completely filled by vapor. Soon after that when the liquid phase began to exist at the upper portion of the bundle, the quality started to decrease as the quench front marching upward along the test section. At higher locations, the mass quality was found to be higher. After the quench of each location, the mass quality achieved steady state and
maintained constant ever since. It should be noted that the quality increase right before quench is mainly due to the measurement of steam temperature, which was influenced by the quench front.

![Figure 5-5: Transient Variation of the Mass Quality at 2.743 m, Exp. 7151](image)

**5.2 Model Development for the Two-Phase Flow Hydraulic Non-Equilibrium**

For hydraulic non-equilibrium, the relative velocity between phases, \( u_r = u_{vap} - u_{liq} \), or the slip ratio, \( s \), are generally used. The slip ratio is defined in Eq. (5.3) as,

\[
s = \frac{u_{vap}}{u_{liq}}
\]  

(5.3)

where, \( u_{vap} \) and \( u_{liq} \) are the local vapor and liquid velocities, respectively. In the RBHT tests, velocity of each droplet passing through the sampling area captured by the lasers imaging system is determined. By grouping and processing the measured droplet data every 20 sec, very detailed distributions for the droplet size and velocity could be obtained at the measurement location for each time period. Due to the increasing turbulence in the flow and the increasing droplet size
entrained in vapor as the quench front marching toward the measurement location, droplet in-focus count will gradually reduce until reaching a certain minimum count below which the sample size is too small to yield statistically valid results. Based on previous droplet uncertainty studies by Jin et. al. (2018a, 2018b) [33, 34], the droplet count lower limit of 30 is found to be a good practice in the current analysis.

5.2.1 Model Derivation

In the DFFB regime formed inside a vertical channel, the upward motion of entrained droplets is mainly determined by the surrounding vapor flow. For an individual droplet shown in Figure 5-6, three of the most important forces acting on it are: inertial force, buoyancy force and the interfacial drag force. Force induced by the flow field pressure gradient along the axial direction is relatively small compared with the other forces due to very small droplet size. In some cases, the lift force due to droplet rotation and diffusion force due to droplet concentration gradient also need to be considered in the analysis. If there exists significant interfacial mass and heat transfer between droplet and vapor, like in the current reflood case, then the blowing effect due to phase change at the interface also plays a role, generally reducing the interfacial drag. Therefore, in order to study the two-phase relative motion, a force balance analysis need to be performed by tracking the motion of a specific droplet in its vapor surroundings, from which the vapor velocity can be determined provided that the droplet velocity and other relevant heat transfer and flow parameters are known. As such, a Lagrangian formulation describing the droplet motion is considered to be more suitable for the problem at hand than working on conservation equations written in the Eulerian form.
As a result, the equation of motion for an individual droplet can be written as:

\[
\rho_d \frac{\pi d^3}{6} \frac{du_d}{dt} = -\frac{\pi d^3}{6} \frac{\partial P}{\partial z} + \tau_i + \frac{\pi d^3}{6} (\rho_{vap} - \rho_d) g + F_i
\] (5.4)

where, the first term on the RHS is due to the pressure gradient in the vapor flow surrounding the droplet. The second term, \(\tau_i\), represents the general interfacial drag. The third term represents the body force and the last term, \(F_i\), accounts for any other forces acting on the droplet. In the current analysis, it is assumed that the droplets are spherical in shape and the fluid properties within these droplets are uniformly distributed. In addition, the internal circulation and droplet rotation are also neglected due to their minor effects on the overall two-phase flow behavior during reflood.

The general interfacial drag can then be further divided into the steady-state term; the added mass term, which is the force required to accelerate the apparent mass of the surrounding phase when the relative velocity changes; and the Basset history term, which accounts for the effect of the acceleration on the viscous drag and boundary-layer development (Aggarwal and Peng 1995 [35]). Odar and Hamilton (1964) [36] experimentally determined a correlation incorporating all these terms for Reynolds number, \(Re\), up to 62. Their expression is given as:
\[ \tau_i = C_D \frac{1}{2} \rho_{vap} |u_{vap} - u_d| (u_{vap} - u_d) A_p + C_A \frac{\pi d^3}{6} \rho_{vap} \frac{d}{dt} (u_{vap} - u_d) \]
\[ + C_H \frac{d^2}{4} \sqrt{\pi \rho_{vap} \mu_{vap}} \int_{t_0}^{t} \frac{d}{\sqrt{t-t'}} (u_{vap} - u_d) dt' \] (5.5)

where, \( C_D \) is the standard drag coefficient. \( C_A \) and \( C_H \) are empirical coefficients accounting for high \( Re \) for the added mass and history term, respectively. \( A_p \) (\( A_p = \frac{\pi d^2}{4} \)) is the projected area of the droplet having a diameter of \( d \). As discussed by Ishii 1979 [30], the forms of the two transient terms are not firmly established and thus need further research. In the current analysis, a quasi-steady state two-phase flow is assumed. This is because the reflood tests under current investigation have relatively low flooding rates (less than 0.0508 m/sec (2 in/sec)) such that the quench front propagated upward relatively slowly. In addition, each group of droplet data used for analysis is taken from a 20 sec timespan average, any transient variations of the droplets within this period cannot be accounted. Therefore, a quasi-steady state approach is considered to be satisfactory for the current problem. Neglecting the transient terms in Eq. (5.5), the general interfacial drag can be simplified as:

\[ \tau_i = C_D \frac{1}{2} \rho_{vap} |u_{vap} - u_d| (u_{vap} - u_d) A_p \] (5.6)

It can be seen that the steady-state drag force is a function of the relative velocity, interfacial area and the interfacial drag coefficient, \( C_D \). Its direction always opposes the relative motion, acting as a resistance force. Under intermediate or higher Reynolds number regime, the generalized interfacial drag includes the effects of pressure distribution around the droplet surface induced by the relative motion and flow separation, the shear force at the liquid-vapor interface, and the thrust force (blowing effect) due to phase change at the interface (Renksizbulut and Yuen 1983b [25]). In the literature, the value of \( C_D \) is usually determined by experiments or by numerical analysis. Ishii and Chawla 1979 [30] reported an empirical correlation for a single
particle (solid particles, droplets or bubbles) in the infinite medium for the viscous regime. Their correlation has the form,

\[
C_D = \frac{24}{Re_{vap}} \left(1 + 0.1Re_{vap}^{0.75}\right) \tag{5.7}
\]

and,

\[
Re_{vap} = \frac{\rho_{vap}|u_{vap} - u_d|d}{\mu_{vap}} \tag{5.8}
\]

In deriving Eq. (5.7) the adiabatic condition between phases was assumed and thus no phase change due to interfacial heat transfer was considered in their study. In reality, however, many thermal-hydraulic equipment and systems involving two-phase flow are operated under elevated system pressure and temperature condition. For example, in the spray cooling, spray combustion and, specifically, in the reflood stage of the current study, effects of interfacial mass and heat transfer are significant and need to be considered. It has been found by Yuen and Chen 1976 [37] that the modification of boundary layer profile due to the blowing effect of liquid droplet evaporation generally reduces the total drag. In the numerical work performed by Renksizbulut and Yuen 1983b [25], they reported a correlation for the interfacial drag coefficient taking into consideration of the phase change effect. Their correlation is given in Eq. (5.9) as,

\[
C_D \left(1 + B_{film}\right)^{0.2} = \frac{24}{Re_{vap}} \left(1 + 0.2Re_{vap}^{0.63}\right), \quad (10 < Re_{vap} < 300) \tag{5.9}
\]

In Eq. (5.9) the film temperature is used to evaluate the thermophysical properties, except for the density in \(Re\), for which the free stream vapor density is used. \(B_{film}\) is the mass transfer number without radiation heat transfer and has the form:

\[
B_{film} = \frac{c_p(T_{vap} - T_d)}{h_fg} \tag{5.10}
\]

The \((1 + B_{film})^{0.2}\) factor in Eq. (5.9) accounts for reduction in the interfacial drag due to the blowing effect. In the DFFB regime of the current RBHT tests, however, the effect of radiation heat transfer might be significant due to extremely high rod surface temperature that has
achieved. Hence, the mass transfer number is modified to account for the effect of radiation heat transfer. **Eq. (5.11)** presents the modified mass transfer number.

\[ B_m = B_{film}(1 + \frac{q''_{rad}}{q''_{conv}}) \]  

(5.11)

where, \( q''_{rad} \) and \( q''_{conv} \) are the radiation and convective heat fluxes for a single droplet, respectively. Neglecting the radiation heat transfer between the vapor and liquid droplet, \( q''_{rad} \) can be expressed by:

\[ q''_{rad} = \pi d^2 \varepsilon_{rod} \sigma (T_{rod}^4 - T_d^4) \]  

(5.12)

where, \( \sigma \) is the Stefan-Boltzmann constant and \( \varepsilon_{rod} \) is the emissivity for Inconel which is temperature dependent. In **Eq. (5.12)**, the emissivity of liquid droplet, \( \varepsilon_d \), is assumed to be unity for simplicity.

The interfacial convective heat transfer, on the other hand, is given by **Eq. (5.13)** in terms of the Nusselt number using the empirical correlation reported by Renksizbulut and Yuen 1983b [25] in **Eq. (5.14)**.

\[ q''_{conv} = Nu \left( \frac{k_{vap}}{d} \right) \pi d^2 (T_{vap} - T_d) \]  

(5.13)

\[ Nu_{film}(1 + B_{film})^{0.7} = 2 + 0.57 Re_{vap}^{1/2} Pr_{vap}^{1/3} \quad (10 < Re_{vap} < 2000) \]  

(5.14)

Similar to **Eq. (5.9)**, the film temperature is used to evaluate the thermophysical properties, except for the density in \( Re \), for which the free stream vapor density is used. The term \( (1 + B_{film})^{0.7} \) accounts for the blowing effect on heat transfer due to phase change.

Once the modified mass transfer number is determined, it can be substituted into **Eq. (5.9)** to calculate the interfacial drag coefficient. Based on **Eq. (5.7)** and **Eq. (5.9)** for the drag coefficient with or without phase change, the corresponding interfacial drag force can be adequately determined. Last, using **Eq. (5.4)** and assuming quasi-steady state, the vapor velocity surrounding an individual droplet can be calculated. Under quasi-steady-state conditions, the forces acting on the droplets are mechanically balanced and thus droplets are in their terminal
velocities. While larger liquid droplets may take longer time to reach their terminal velocities after being entrained, smaller droplets are expected to catch up with their terminal velocities quickly due to their significantly smaller masses. Moreover, when the liquid droplets are extremely small, they can be considered to be completely entrained by the vapor flow, leading to a slip ratio essentially equal to unity. Therefore, the current force balance analysis method is considered to be more valid for small droplets that can respond to the surrounding vapor flow promptly. Thus, it is considered to be appropriate to determine the vapor velocity based on the small droplet group.

In the DFFB regime, the droplet temperature can be assumed at saturation. While the surrounding vapor temperature is obtained directly from the steam probe measurements and is considered to be valid in the region far from the quench front and representative for that specific location. However, cautions should be taken in interpreting the measured vapor temperature that is close to the quench front since early wetting of the steam probes might occur. Vapor thermodynamic properties are determined according to the instant operating pressure. Knowing the transient variation of droplet velocity as a function of droplet size and having determined the corresponding vapor velocity, a detailed distribution of slip ratio as a function of droplet size and time can be obtained. A further average of the calculated vapor velocity can be performed in order to obtain a single value for each time point (i.e., every 20 sec).

5.2.2 Determination of Vapor Velocity

In the region downstream of the quench front, liquid-wall direct contact is prohibited due to the significant wall superheat. The surface is above the minimum film boiling temperature of the wall. Therefore, the two-phase flow regime is mainly characterized by the dispersed liquid droplets flow. Based on the force balance model described in previous section, for each droplet
captured, a corresponding vapor velocity can be determined. As discussed in Section 4.2.1, there is a distribution both for droplet size and velocity in the DFFB regime. Thus, within a specific time span, the calculated vapor velocities can be further processed as needed to obtain the transient variation of the vapor velocities.

The measured droplet velocity and the corresponding vapor velocity at the upstream of SG 6 (2.743 m) for the time period of 20-40 sec for Exp. 7151 are shown in **Figure 5-7** and **Figure 5-8** based on the interfacial drag model of Ishii and Chawla (1979) [30] and Renksizbulut and Yuen (1983b) [25], respectively. In total there were 446 liquid droplets being captured by the laser system during this period. For each data set, a correlation of either exponential form or power form can be obtained by curve fit. During the time period of 20-40 sec, droplet velocities at 2.743 m elevation are found to be between 3–14 m/sec. As explained above, the scatter in measurements is mainly caused by temporal and spatial effects as well as droplet size distribution. It can be clearly seen that the calculated vapor velocities were always larger than the corresponding liquid droplet velocities, indicating that the droplet upward motion was driven by the vapor flow through interfacial drag during reflood. For small droplets, the calculated vapor velocities were very close to their measured velocities. For large droplets, on the contrary, the calculated velocity differences were very significant. Therefore, it can be seen that while there is a strong dependence of the droplet velocity on the droplet size, the vapor velocity is found to be relatively independent on the droplet size as expected.
Figure 5-7: Calculated Vapor Velocity Between 20-40 sec without Phase Change, Exp. 7151

Figure 5-8: Calculated Vapor Velocity Between 20-40 sec with Phase Change, Exp. 7151
In order to further investigate the effect of phase change, a direct comparison between the calculated vapor velocity with and without considering the interfacial mass and heat transfer processes is shown in Figure 5-9. As expected, since additional convective and radiation heat transfer mechanisms were considered, the calculated vapor velocity was found to be larger than the vapor velocity calculated without phase change. In addition, the difference in the vapor velocity between the two methods increased as the droplet size became larger. As discussed previously, the blowing effect due to phase change process at the interface tends to reduce the interfacial drag, which serves as the driving forces for droplet motion. In order to achieve the same droplet velocity as measured by the camera, smaller driving force felt at the interface by the droplet naturally requires a higher vapor velocity.

Figure 5-9: Comparison of Calculated Vapor Velocity Between 20-40 sec, Exp. 7151

This phenomenon is found to be more significant for larger droplets because of larger interfacial heat transfer area and relative velocity for an individual droplet. For both methods used, despite of the scatter, it can be observed that the general trend of vapor velocity remained at

\[ y_{\text{inlet}} = 3.084\exp(-6.924\times 10^3\times 3x) + 7.203\exp(2.363\times 10^3\times 4x) \]

\[ y_{\text{final}} = 3.246\exp(-7.495\times 10^3\times 3x) + 7.182\exp(1.741\times 10^3\times 4x) \]
a relatively constant value. As a result, from Figures 5-7 to 5-9 it is known that together with other flow parameters and the flow channel geometry, the two-phase slip ratio should also be a function of droplet size. The larger the liquid droplets measured in the DFFB regime, the larger the slip ratio would be.

5.2.3 Transient Variations of the Vapor Velocity and Slip Ratio

From previous sections, it can be clearly seen that, the slip ratio between the liquid droplet and vapor is a function of the reflood time and droplet diameter. Smaller droplets, once generated, will have relatively shorter duration of initial acceleration than those needed for larger droplets. Due to their smaller momentums, it is relatively easy for them to catch up with the vapor phase than those droplets with larger size. By calculating the slip ratios for each droplet captured within each time period before quench during reflood, it is possible to study the time variations of vapor velocity and slip ratio as the quench front propagates upward.

By plotting the slip ratio between liquid and vapor phases as a function of droplet size and for different periods, Figure 5-10 shows the transient slip ratio variation during reflood for Exp. 7151. Depending on different droplet sizes and time periods, different slip ratio values ranging from 1-4 were obtained. For small droplets the corresponding slip ratio is close to unity, indicating that the relative motion is very small for very small droplets. Thus, small droplets can be considered to be fully entrained in the vapor phase during reflood. On the other hand, slip ratio as large as 2-4 has been observed for very large droplets. Nevertheless, it should be noted that for all time periods of reflood, due to the log-normal droplet size distribution only few large droplets were captured in the DFFB regime, especially when the droplet size is greater than 1000 μm. As the reflood time increases, a gradually increasing trend was observed for the slip ratio between phases. Such changes were more significant for larger droplets. As a result, it is expected that the
hydraulic discrepancies between two phases are generally larger when the quench front is closer as well as when droplets are larger.

![Graph showing slip ratio variation as a function of time and droplet size, Exp. 7151](image)

**Figure 5-10:** Slip Ratio Variation as a Function of Time and Droplet Size, Exp. 7151

It can be clearly seen from **Figure 5-10** that the slip ratio has a distribution depending on the droplet size. In standard thermal-hydraulic analysis, however, due to the lack of detailed information for the droplet group, usually only one single value of slip ratio is used to account for the relative motion between phases. Therefore, how to pick the time-dependent averaged slip ratio is extremely important in subsequent two-phase flow and heat transfer analysis. Due to the log-normal distribution of liquid droplets, the majority of liquid mass and/or volume is stored in those large droplets but only having a small population. On the contrary, the total droplet number as well as the interfacial surface area are mainly dominated by small droplets due to their large population. Taking this into consideration, it can be inferred that in the DFFB regime the larger droplet group is more important in hydraulic behaviors while the smaller droplets as a group are
more thermally active. Therefore, different droplet groups will have different effects on the mass and heat transfer processes. For the mass, momentum and energy transfer induced by the hydraulic behaviors such as droplet entrainment, deposition and two-phase flow pressure drop, large droplet group may control the entire processes and thus the droplet volume or mass weighted average slip ratio need to be applied in the analysis. However, if the mass, momentum and energy transfer processes are mainly induced by thermal aspect from the two-phase interaction, such as interfacial heat transfer, phase change, then use of the droplet number or area weighted slip ratio such that more weight can be given to small droplet group in the analysis might be more appropriate. As a result, how to take the average of the two-phase slip ratio is of crucial importance in the DFFB thermal-hydraulic analysis since different values will result in completely different two-phase flow behaviors. Under certain situations where the two-phase relative motion need to be carefully characterized, a distribution of the slip ratio as a function of droplet size should be used. Similarly, judicious selection of the droplet size averaging method and many other droplet distribution related statistical parameters, if needed, are also subject to this type of consideration.

In order to illustrate the difference induced by different averaging methods and to investigate the subsequent effect, the following experimentally measured data has been processed in two different ways: a). the entire droplet group are processed and averaged to obtain the vapor velocity and slip ratio; b). only those droplets with diameter smaller than 300 μm are used for vapor velocity calculation. However, the droplet average velocity is always based on the entire droplet group measured.
Figure 5-11: Variations of Averaged Droplet and Vapor Velocities at 2.743 m, Exp. 7151

It is useful to present the averaged droplet and vapor velocity as a function of reflood time for Exp. 7151, as shown in Figure 5-11. In the current study in order to be statistically valid, only the data with droplet in-focus count larger than 30 was used. As the quench front moving upward, it can be seen that both the droplet and vapor velocity decreased from their initial values to smaller values. Comparison between different vapor velocity calculating methods shows that the one averaged from only small droplet group yields a larger velocity. This result is expected since the velocity of smaller droplets are closer to the vapor phase velocity. For the calculation based on the entire droplet group, the vapor phase velocity determined was smaller since those larger and slower droplets were also accounted for.

In Figure 5-12 the transient variation of the averaged slip ratios are presented based on the different vapor velocity calculations. Starting from the initial liquid injection, the slip ratio at 2.743 m elevation (i.e., peak power location) gradually increased from ~1.1 to ~1.4. It can be seen that even though the slip ratios obtained from two different droplet groups had the same trend,
their actual values were different. As expected, using the vapor velocity determined from the entire droplet group generally yields a smaller slip ratio. Thus for different applications, a judicious selection of the slip ratio averaging method is necessary.

Figure 5-12: Variations of Averaged Slip Ratio at 2.743 m, Exp. 7151

5.3 Model Development for the Two-Phase Flow Mass Quality

The two-phase flow local mass quality can be affected by many factors related not only to the two-phase flow regimes but also to the corresponding heat transfer regimes. Additionally, different flow and heat transfer regimes are determined by the system pressure, mixture mass flow rate, two-phase flow thermal-hydraulic non-equilibrium status, the way of heating power being applied, the interface evolution, and so forth. For the DFFB regime of reflood transients, in particular, the mass quality is also affected by the quench front propagation, liquid droplet entrainment, deposition and breakup processes. The change of droplet size and distribution within the flow channel could significantly alter the mass quality value. Due to its extremely
complicated characteristics, the modeling of the actual quality variation within the rod bundle geometry during reflood is of crucial importance.

Starting from the drift flux formulation, the two-phase flow void fraction can be expressed by:

\[ \alpha = \frac{j_v}{C_0 j + u_{vj}} \]  \hspace{1cm} (5.15)

where \( j_v \) and \( j \) are the vapor and total superficial velocity, respectively. \( C_0 \) is the two-phase distribution parameter and \( u_{vj} \) is the vapor phase drift velocity. All the variables are averaged over the cross-sectional area of the flow channel. Based on the relationship between the void fraction and actual quality, we have:

\[ \alpha = \frac{1}{1 + \left( \frac{1-x}{x} \right) \left( \frac{\rho_v}{\rho_l} \right) s} \]  \hspace{1cm} (5.16)

where, \( s \) is the slip ratio between the two separate phases. Combining Eq. (5.15) and (5.16) and eliminating \( \alpha \) yields:

\[ x = \left( 1 + \left( \frac{C_0 j + u_{vj}}{j_v} \right) \left( \frac{\rho_l}{\rho_v} \right) \left( \frac{1}{s} \right) \right)^{-1} \]  \hspace{1cm} (5.17)

As can be seen, superficial velocities and fluid properties in Eq. (5.17) can be readily obtained from experiments. \( C_0 \) is found to be very close to 1 in the DFFB regime [3]. In addition, the slip ratio between two phases in the DFFB regime can be determined by droplet measurement based on the laser imaging system [20, 21]. The last term that needs to be determined in order to obtain \( x \) through Eq. (5.17) is to correlate the vapor drift velocity, \( u_{vj} \), with known parameters. Therefore, in order to develop a semi-empirical model for the two-phase flow local mass quality in the DFFB regime, a semi-empirical correlation need to be developed for the vapor drift velocity, \( u_{vj} \), which can be related to the relative velocity, \( u_r \), between the vapor velocity, \( u_v \), and the liquid droplet velocity, \( u_d \), as:

\[ u_{vj} = (1 - \alpha) u_r \]  \hspace{1cm} (5.18)
From Eq. (5.18) it can be seen that, if the relative velocity and the void fraction are known the vapor drift velocity can be determined.

The most appropriate way to start with is to work on the basic conservation equations for each phase, considering all the possible flow and heat transfer factors influencing the two-phase flow transport behavior.

### 5.3.1 Derivation of Governing Equations

**Figure 5-13** shows a typical control volume (CV) within which the current theoretical derivation is applied. As can be seen from the left figure, the conservation equations are written for a sub-channel area in the radial direction expressed by the hydraulic diameter, $D_h$, and for a length which is comparable to $D_h$ in the axial direction, as shown on the right of **Figure 5-13**. In the DFFB regime the two phases are neither at the same velocity nor at the same temperature due to interfacial interactions. Therefore, conservation equations should be written for each phase separately in order to account for the separate flow effects.

![Figure 5-13: Control Volume for Model Development](image)
The one-dimensional transient mass and momentum equations for phase $k$ (can be either $d$, for the liquid droplet phase or, $v$, for the vapor phase) is given in Eqs. (5.19) and (5.20) below:

$$\frac{\partial}{\partial t} (\alpha_k \rho_k) + \frac{\partial}{\partial z} (\alpha_k \rho_k u_k) = \Gamma_k'''$$ (5.19)

$$\frac{\partial}{\partial t} (\alpha_k \rho_k u_k) + \frac{\partial}{\partial z} (\alpha_k \rho_k u_k u_k) = -\alpha_k \frac{\partial \rho_k}{\partial z} + \alpha_k \rho_k g + u_d \Gamma_k'' + \tau_k'' + \tau_i$$ (5.20)

where $\Gamma_k'''$ is the mass transfer rate per unit volume due to the interfacial heat transfer between the liquid droplet and vapor. This quantity is always negative for the droplet phase and positive for the vapor phase due to the direction of energy flows. The RHS terms in Eq. (5.20) are the pressure drop, body force, momentum transfer due to interfacial mass and heat transfer, viscous stress, $\tau_k'''$, and the interfacial drag, $\tau_i$, respectively. In writing Eq. (5.20) it is assumed that the turbulent stress can be neglected for both phases, which is valid for the reflood transients within the rod bundle geometry, as long as the CV is not immediately downstream of a spacer grid. It should be noted that the viscous stress term for the vapor phase should also include the friction of the wall, which doesn’t exist for the dispersed liquid phase. The lift force and dispersion force can be ignored for the dispersed phase, since in a two-phase flow system, the diffusion of phases is a macroscopic process governed by the interfacial geometry the body-force field as well as the interfacial energy and momentum transfer.

In addition, the conservation of momentum for the two-phase flow mixture requires:

$$\tau_v + \tau_d = 0$$ (5.21)

Instead of also formulating an energy equation for each phase, the existing empirical interfacial heat transfer correlations will be used to simplify the problem. Assuming steady state and neglecting the viscous stress, conservation Eqs. (2.19) and (5.20) for the liquid droplet phase can be simplified as:

$$\frac{\partial}{\partial z} (\alpha_d \rho_d u_d) = -\Gamma_d'''$$ (5.22)

$$\frac{\partial}{\partial z} (\alpha_d \rho_d u_d u_d) = -\alpha_d \frac{\partial \rho_d}{\partial z} - \alpha_d \rho_d g - u_d \Gamma_d'' + \tau_i$$ (5.23)
In the current formulation, the upward direction has been selected as the positive direction.

According to the mass continuity requirement for the two-phase mixture, we have:

\[
\frac{\partial}{\partial z} (\alpha_d \rho_d u_d) + \frac{\partial}{\partial z} (\alpha_v \rho_v u_v) = 0
\]  

(5.24)

**Equation. (5.24)** will be used to derive a relation between vapor and liquid droplet velocities later such that the vapor velocity can be expressed by the droplet velocity in Eq. (5.18).

Expanding the LHS of Eq. (5.23) using chain rule gives:

\[
u_d \frac{\partial}{\partial z} (\alpha_d \rho_d u_d) + \alpha_d \rho_d u_d \frac{\partial}{\partial z} (u_d) = -\alpha_d \frac{\partial \rho_d}{\partial z} - \alpha_d \rho_d g - u_d \Gamma_d'' + \tau_i
\]  

(5.25)

Substituting the \(u_d \frac{\partial}{\partial z} (\alpha_d \rho_d u_d)\) term using Eq. (8) yields:

\[
\alpha_d \rho_d u_d \frac{\partial u_d}{\partial z} = -\alpha_d \frac{\partial \rho_d}{\partial z} - \alpha_d \rho_d g + \tau_i
\]  

(5.26)

We further work on the interfacial drag term, \(\tau_i\). The drag force acting on one single liquid droplet by its surrounding vapor phase under steady-state condition (Jin et al. 2018c 15) can be given as:

\[
F_D = \frac{1}{2} C_D \rho_v |u_r| A_d
\]  

(5.27)

where \(C_D\) is the interfacial drag coefficient, \(u_r\) is the relative velocity between the two phases and \(A_d\) is the droplet projected area \(A_d = \frac{\pi D_d^2}{4}\). Based on the study performed by Renksizbulut and Yuen (1983b) [17], their correlation proposed for the interfacial drag coefficient taking into consideration of the interfacial phase change effect is given by Eq. (5.28) as:

\[
C_D \left(1 + B_{f \mu m}\right)^m = \frac{24}{Re_v} (1 + 0.2 Re_v^{0.63}), \quad (10 < Re_v < 300)
\]  

(5.28)

\[
Re_v = \frac{\rho_v |u_r| D_d}{\mu_v}
\]  

(5.29)

In Eq. (5.28) the film temperature (1/2 rule) is used to evaluate the thermophysical properties, except for the density in \(Re_v\), for which the free stream vapor density is used. \(B_{f \mu m}\) is the mass transfer number and has the form:
The \((1 + B_{film})^m\) factor in Eq. (5.30) accounts for reduction in interfacial drag due to the blowing effect induced by the interfacial mass and heat transfer (in Renksizbulut and Yuen 1983b [17], \(m = -0.2\)). As can be seen from Eq. (5.30), it is mainly related to the vapor phase superheat and fluid properties, thus measuring the two phase thermal non-equilibrium. In order to simplify the derivation, instead of using the original form of Eq. (5.28), a more general expression, Eq. (5.31), for the interfacial drag is used.

\[
C_D = \frac{c}{Re_v} (1 + B_{film})^{-m} \tag{5.31}
\]

where, \(n\) approaches 1 when the Reynolds number is small leading to \(C_D = \frac{c}{Re_v} (1 + B_{film})^{-m}\), while \(n\) approaches 0.37 when the Reynolds number is large leading to \(C_D = \frac{c}{Re_v^{0.37}} (1 + B_{film})^{-m}\). Using the general power-law expression for the interfacial drag as a function of the fluid Reynolds number is a conventional approach adopted in fluid mechanics and heat transfer studies. Coefficient \(c\) is a constant. On the other hand, the interfacial drag per unit volume, \(\tau_i\), in Eq. (5.26) can be related to the drag force on a single liquid droplet in terms of the droplet volume and void fraction as:

\[
\tau_i = \alpha_d \frac{F_D}{V_d} \frac{6 \alpha_d F_D}{\pi D_d^3} \tag{5.32}
\]

Substituting Eqs. (5.27) to (5.32) into Eq. (5.26) yields:

\[
\alpha_d \rho_d u_d \frac{\partial u_d}{\partial z} = -\alpha_d \frac{\partial P_d}{\partial z} - \alpha_d \rho_d g + \left(\frac{3 \alpha_d \rho_d u_r |u_r|}{4 D_d}\right) \left(\frac{c}{Re_v} (1 + B_{film})^{-m}\right) \tag{5.33}
\]

Eq. (5.33) is a one-dimensional quasi-steady-state momentum conservation equation describing the liquid droplet behavior during reflood transients.
5.3.2 One-Dimensional Scaling Analysis

In order to obtain a relationship for the two phase velocities in terms of the other fluid and system parameters, a one-dimensional scaling analysis for the two phase quasi-steady-state mass and momentum conservation is applied.

First, each variable in the two-phase continuity equation Eq. (5.24) can be scaled as follows:

\[ u_d \sim U_d, u_v \sim U_v, \text{ and } z \sim D_h \]  

(5.34)

The axial length is scaled to \( D_h \) because the current CV selected has a comparable axial length with the rod bundle hydraulic diameter. It will be shown that the geometry selected does not restrain the final correlation developed since \( D_h \) is eventually eliminated in the derivation process. Based on Eq. (5.33), the derivatives in the continuity equation can be scaled as:

\[ \frac{\partial}{\partial z}(\alpha_d \rho_d u_d) \sim \frac{\alpha_d \rho_d U_d}{D_h}, \text{ and } \frac{\partial}{\partial z}(\alpha_v \rho_v u_v) \sim \frac{\alpha_v \rho_v U_v}{D_h} \]  

(5.35)

Substituting all these scaled variables back into Eq. (5.24) yields:

\[ U_v \sim \frac{\alpha_d \rho_d}{\alpha_v \rho_v} U_d \sim \frac{(1-\alpha_v) \rho_d}{\alpha_v \rho_v} U_d \]  

(5.36)

It can be seen that the relation between the two phase velocity in the DFFB regime is mainly determined by the void fraction and the density of each phase. Equation (5.36) can be used to determine the two-phase velocity as well as the slip ratio for later use. Applying the similar methodology used for the continuity equation, variables in the droplet phase momentum equation, Eq. (5.33), can be scaled as:

\[ P_d \sim P, u_r \sim U_r = U_v - U_d, C_D \sim \frac{1}{R_e_v^{m}} (1 + B_{film})^{-m} \]  

(5.37)

In writing Eq. (5.37), it was assumed that the surface tension effect can be neglected and therefore we have \( P_v \sim P_d \sim P \). Then, working on Eq. (5.33) for the case of co-current flow we have:
In the DFFB regime, the liquid droplet flow is mainly determined by the interfacial drag and body force. Therefore, the pressure term is of secondary effect [15]. In order to account for the effects of the interfacial heat transfer during this process, it is most appropriate to scale the LHS of Eq. (5.38) with the last term on the RHS of Eq. (5.38) since this term includes effects of droplet size, two phase velocities, void fraction and vapor superheat, all of which are important characterizations of dispersed droplet flow. The local variation of droplet size can be further correlated to the quench front location during reflood. In doing so and by neglecting the constants we have:

\[
\frac{\alpha_d \rho_d U_d U_d}{D_h} \sim \left( -\frac{\alpha_d p}{D_h} - \alpha_d \rho_d g + \frac{3 \alpha_d \rho_d^2 - n u_d^2 - n \mu_d^2}{4 D_h^{1+n}} (1 + B_{film})^{-m} \right)
\] (5.38)

It can be seen from Eq. (5.39) that this expression include both the droplet velocity, \(U_d\), and two-phase relative velocity, \(U_r\). Therefore, in order to obtain an explicit expression only for the relative velocity, an alternative expression for the LHS term of Eq. (5.39) is required. This can be found by scaling the LHS of Eq. (5.38) with the body force term on the RHS of the equation to include the inertial effect, which yields:

\[
\frac{\alpha_d \rho_d U_d U_d}{D_h} \sim \frac{\alpha_d \rho_d^2 - n u_d^2 - n \mu_d^2}{D_h^{1+n}} (1 + B_{film})^{-m}
\] (5.39)

Substituting Eq. (5.40) into Eq. (5.39) results in:

\[
U_r \sim \left( \frac{\rho_d h_d^{1+n}}{\rho_v^{1-n} \mu_v} \right)^{\frac{1}{2-n}} \left( 1 + B_{film} \right)^{\frac{m}{2-n}}
\] (5.41)

According to the relation between \(U_r\) and \(U_{vj}\) (Eq. (5.18)), the following expression for the \(u_{vj}\) is finally obtained as:

\[
u_{vj} = a (1 - \alpha_v) \left( \frac{\rho_d h_d^{1+n}}{\rho_v^{1-n} \mu_v} \right)^{\frac{1}{2-n}} \left( 1 + B_{film} \right)^{b}
\] (5.42)
Note that $D_d$ in Eq. (5.42) is in meters. It can be seen that the drift velocity is correlated to the void fraction, interfacial heat transfer (or vapor superheat), droplet size and fluid properties. Since the two-phase flow system is usually a strong function of the system pressure, the coefficient $a$ and $b$ can be correlated with the system pressure instead of being treated as a constant, if needed. It should also be noted that the $D_h$ term has been eliminated during the derivation and thus Eq. (5.42) holds for any two-phase flow geometry (rod bundle, tube, etc.).

5.3.3 Determination of Coefficients

As discussed in the previous section, once the $u_{ef}$ is obtained, the two-phase flow actual quality can be determined. It has been found in the current RBHT test and also in the study by Ishii (1977) [3] that the distribution parameter, $C_0$, for the DFFB regime is very close to unity ($1 \leq C_0 \leq 1.1$). This is because the dispersed phase in the channel tends to distribute uniformly and liquid droplet slips near the wall, leading to a more flatten vapor velocity distribution. Therefore, $C_0$ equals to 1 is a very good approximation that can be used in the present study. Based on the mass balance calculation for various reflood tests, the relative error involved in the mass into and out of the test section was found to be well within 5%. This further leads to a mass quality relative error of about 8~10% based on the energy balance analysis for the DFFB regime [15]. Therefore, the mass quality data can be used to determine the coefficients. In order to complete the current derivation, the coefficient $n$ in Eq. (5.42) is taken as 1. This corresponds to the low Reynolds number case since the current reflood tests were performed under low flooding rate conditions (0.0191 to 0.0508 m/sec). If more data for the mass quality as well as for the flooding rate become available, then the coefficients in Eq. (5.42) can be further adjusted to fit the trend.

Based on the RBHT droplet measurement at various test conditions, the liquid droplet Sauter mean diameter, $D_d$, is correlated as a function of the distance from the measurement
location to the quench front location. Figure 5-14 shows measurement as well as the curve fitting. The corresponding expression can be written as:

\[ D_d = 536.84L^{-0.219} \]  

(5.43)

where \( D_d \) is in micron and \( L \) is the distance from the quench front to the measurement location in meter. It should be noted that the current correlation is developed based on real-time local parameters only. Therefore, no upstream information is actually needed to calculate the mass quality if all the local variables are known. The reason for correlating the droplet size data with respect to the distance to quench front in the current model development is to facilitate the calculation process.

![Figure 5-14: Droplet Size Variation vs. QF Location in the RBHT reflood Tests](image)

We next find the coefficient \( b \) for \((1 + B_{film})\) term in Eq. (5.42). To do this, all the other terms are moved to the LHS of Eq. (5.42) and the result is plotted as a function of \((1 + B_{film})\), which is shown in Figure 5-15 for all the RBHT reflood tests selected. As can be seen from the figure, an increasing trend of the data as a function of \((1 + B_{film})\) is observed. Under current low
system pressure test conditions (138 - 414 kPa), it was found that the system pressure does not play a significant role. Therefore, the vapor drift velocity is found to be correlated following a power relationship with the \(1 + B_{film}\) term as:

\[ u_{vj} \sim (1 + B_{film})^{1.379} \]  

(5.44)

Finally, we can find coefficient \(a\) based on Eq. (5.45) below:

\[ u_{vj} = a(1 - \alpha_v) \left( \frac{\rho_d g D_d^{1+n}}{\rho_v^{1-n} \mu_v^n} \right)^{\frac{1}{2-n}} (1 + B_{film})^{1.379} \]  

(5.45)

Figure 5-15:  Parametric Trend vs. \((1 + B_{film})\)

By plotting both sides of Eq. (5.45) in the same plot, Figure 5-16 shows the curve fitting results for the RBHT reflood data. It can be seen that the current experimental results generally follow a linear trend. In addition, the effect of different system pressures is not distinguishable. Based on the current analysis, the coefficient \(a\) was found to be 0.0356 and the final form of Eq. (5.45) can be rewritten as:

\[ u_{vj} = 0.0356(1 - \alpha_v) \left( \frac{\rho_d g D_d^{1+n}}{\rho_v^{1-n} \mu_v^n} \right)^{\frac{1}{2-n}} (1 + B_{film})^{1.379} \]  

(5.46)
Having obtained an expression for the vapor phase superficial velocity, the local mass quality in the DFFB regime can then be determined using Eq. (5.17).

**Figure 5-16: Variation of the Vapor Drift Velocity**

### 5.3.4 Data Comparison

Using Eqs. (5.46) and (5.17), the two-phase flow local mass quality during reflood transients can now be calculated and compared with the mass quality measured for the DFFB regime from the RBHT tests as well as from other reflood tests available in the literature.

As mentioned previously, the RBHT reflood tests cover a wide range of test conditions that could be encountered under accident scenarios of a LWR. In the present model development, the experimental system pressure ranges from 138 to 414 kPa. The inlet flooding rate varies between 0.0191 and 0.0508 m/sec. The rod bundle peak power input ranges from 0.98 to 1.97 kW/m and, the inlet liquid subcooling temperature varies between 11 and 83 K. These system parameters correspond to low pressure, low flooding rate conditions of a LWR during LOCA.
Various sets of reflood experimental data collected from other sources available in the literature are also used for model comparison. These include the works performed by Unal et. al., 1988, 1994 [13, 14], Gottula and Nelson (1983) [12], Evans 1983 [26]. While Unal’s work was performed in a rod bundle geometry, all the other reflood experiments were carried out in a heated pipe geometry. Moreover, beside the newly developed correlation, the vapor drift velocity correlation developed by Ishii 1977 [3] for the liquid dispersed flow regime under adiabatic conditions is also compared. Ishii’s correlation is given by the following equation:

\[ u_{\nu f} = (1 - \alpha_v) \times \sqrt{2} \left( \frac{\sigma g \Delta \rho}{\rho_\nu^2} \right)^{1/4} \]  

(5.47)

Figure 5-17 through 5-21 show the comparison of RBHT reflood data at rod bundle peak power location (2.74 m) with predictions by the present correlation and Ishii’s correlation, respectively. Note that the mass quality has been averaged every 20 seconds. The errorbars added on top of the measured RBHT data represent a ±10% error. If more accurate data for mass quality becomes available, then the coefficients in Eq. (5.42) can be further adjusted to fit the trend. Figure 5-17 presents the results for the RBHT Exp. 8009, which was performed at a system pressure of 276 kPa (40 psia), inlet velocity of 0.0254 m/sec (1 in/sec), inlet liquid subcooling temperature of 22 K (40 °F) and rod bundle peak power input of 1.31 kW/m (0.4 kW/ft). It can be seen that, as the reflood time increases, the mass quality dropped gradually from above 0.8 to around 0.5. This is because the quench front was getting closer with more liquid droplets being entrained at the measurement location. Good agreement was achieved between the present correlation and the experimental data. The relative error is well within the ±10% range. On the other hand, it was observed that the correlation proposed by Ishii (1977) generally under-predict the current reflood data. This is probably due to the fact that Ishii’s correlation was developed and verified mainly for the pipe geometry under adiabatic conditions, while the reflood conditions are highly thermal-hydraulic non-equilibrium.
Figure 5-18 to 5-21 present the mass quality comparisons with other selected tests. Compared to Exp. 8009, Exp. 8023 were performed at the system pressure of 414 kPa (60 psia), which has a relatively higher operating pressure. Exp. 7166 was performed at the inlet liquid subcooling temperature of 83 K (150 °F) representing the highest subcooling among the RBHT reflood test series. Exp. 7116 has an inlet liquid velocity of 0.0508 m/sec (2 in/sec). Only limited data were obtained for this test due to the fast varying transients. Last, Exp. 8013 was performed under elevated bundle power input (peak power of 1.97 kW/m (0.6 kW/ft)). Due to the longer duration of the quenching process for this test, more data was obtained in the DFFB regime to evaluate the performance of the correlations. From these figures it can be seen that while Ishii’s correlation tend to under-estimate the mass quality data, the new correlation developed in the present study is able to give satisfying predictions for all the test conditions explored.

Figure 5-17: Model Comparison with RBHT Reflood Test, Exp. 8009
Figure 5-18: Model Comparison with RBHT Reflood Test, Exp. 8023

Figure 5-19: Model Comparison with RBHT Reflood Test, Exp. 7166
Figure 5-20: Model Comparison with RBHT Reflood Test, Exp. 7116

Test No. 7116
Pressure: 275.79 kPa
Inlet Velocity: 0.0508 m/sec
Peak Power: 1.31 kW/m
Inlet Subcooling Temp.: 56 K

Figure 5-21: Model Comparison with RBHT Reflood Test, Exp. 8013

Test No. 8013
Pressure: 275.79 kPa
Inlet Velocity: 0.0254 m/sec
Peak Power: 1.97 kW/m
Inlet Subcooling Temp.: 53 K
Figure 5-22 further shows the mass quality data comparison for the DFFB regime with the entire RBHT constant reflood data set. The dashed lines represent a ±10% error. The current RBHT data lies between 0.4 and 0.85, representing high quality two-phase flow conditions. It is found that the new correlation is able to predict all the experimental data well within ±10% error. Ishii’s correlation, however, slightly under-predicts the data in the lower quality region.

The new correlation derived in the present study and the Ishii’s correlation were also compared with the experimental data obtained by Unal et. al. (1988, 1994) [13, 14]. Their experiments were performed at the system pressure slightly above the atmosphere pressure. A 3 × 3 rod bundle assembly with a total heated length of 1.22 m was used in their experiments. In order to freeze the quench front movement at the inlet and outlet of the test section and to maintain steady state post-dryout DFFB conditions, the hot patch technique, originally developed by Groeneveld et. al. (1978) [25] in their post-CHF experiments, was adopted by Unal et. al.. By modifying the aspirated probe technique of Nijhawan et. al. (1980) [11], the vapor superheats at two different axial locations were measured and reported. Since the local vapor temperature was measured in Unal’s experiment, in order to make use of the data for comparison the actual mass quality can be inferred by an energy balance calculation based on the given system pressure, mass flow rate, rod power input as well as the inlet quality and other boundary conditions. In addition, the droplet size was predicted using Eq. (5.43). Due to the lack of information on the two-phase flow slip ratio, a constant value of 1.75 was assumed in the data reduction process wherever needed.
Figure 5-23 presents the comparison with Ishii’s correlations and the newly developed correlation. Compared with the RBHT data, Unal’s mass quality lies in much lower value region (from 0.2 to 0.6). The correlation proposed by Ishii 1977 were found to under-predict Unal’s data. On the other hand, good agreement (within ±10% error) was achieved between the newly developed correlation and Unal’s data. However, it should be noted from Figure 5-23 that for high quality region, the current correlation tended to slightly over-predict the experimental data while for low quality region it tended to slightly under-predict the data. This is because, instead of a constant value for the two-phase slip ratio, for high quality region which usually corresponds to the heat transfer region far away from the quench front location, the slip ratio is actually smaller or even close to unity since the two phases are flowing in similar velocities. On the contrary, for the lower quality region that are often considered to occur near the quench front location, relatively larger slip ratios are expected between phases. As a result, the two phase relative motion actually varies with the quality and the quench front location.
In Figure 5-24, the correlation predictions are compared with Gottula and Nelson (1983) [12] data. Their experimental test setup is similar to the one used in Unal’s study, except that a vertical heated tube was used instead of the rod bundle geometry. The system pressure corresponded to the low pressure conditions (0.2 to 0.7 MPa), which is comparable to the RBHT conditions. The vapor temperature was measured at three different locations close to the test section outlet. The mass quality data was directly obtained from the figures presented in their work. Again, Eq. (5.43) was used for droplet size predication and a constant slip ratio of 1.75 was selected. The new correlation developed in the present study was found to be able to predict the experimental data well within ±10% error. On the other hand, Ishii’s correlation was found to under-predict the data in the lower quality region.
Comparisons of correlation predictions were further made with the experimental data obtained at a system pressure of 378 kPa by Evans (1983) [26]. Evan’s quality range mainly
covers the high quality region, which serves as a good complementary for the model verification. In the calculations, the slip ratio was assumed to be 1.75. As shown in Figure 5-25, both the current correlation and the Ishii correlation were able to predict the experimental data well within ±10% error.

5.4 Model Development for the Liquid Droplet Breakup at a Dry Spacer Grid

The droplet breakup process at the leading edge of a dry spacer grid under reflood conditions is studied both experimentally and theoretically based on the RBHT test facility. In the experiment, the droplet distributions as well as droplet velocities across the spacer grid are measured and analyzed. The droplet generation is found to be affected by the condition of the spacer grid, the quench front location and the inlet liquid subcooling. Moreover, a theoretical model is developed for the dry spacer grid droplet breakup by considering the spacer blockage ratio, the conservation of liquid mass, the conversion of incoming droplet kinetic energy into outgoing droplet surface energy, and the small droplet group loss coefficient γ. In order to complete the current model, several key parameters are then determined by correlating them to relevant thermal and hydraulic variables based on the experimental data currently available. Detailed comparison of the model predictions with experimental data shows that a rather good agreement is obtained, indicating that the model proposed is valid. In addition, sensitivity analyses are performed to investigate the relative importance of the key parameters obtained in current study. The model developed in the current study can be incorporated into the nuclear reactor thermal-hydraulic and safety analysis codes to further improve their capabilities of predicting the droplet behaviors and the fuel rod Peak Cladding Temperature (PCT) under postulated accident scenarios such as LOCA.
Figure 5-26 shows a sketch of the droplet breakup process at the SG location. In the current derivation, the secondary droplets after breakup are represented using a two-droplet-group model, in which the large droplets have comparable size with the incoming droplets while the small droplet group is significantly smaller than the incoming droplets. As shown in Figure 5-26, in total there are $N_o$ incoming liquid droplets being considered and part of the incoming droplets are expected to impinge onto the bottom edge of the SG and break up into smaller droplets. The fraction of the droplets that will breakup is taken as the blockage ratio of the SG, $\varepsilon$. Therefore, there will be a number of $\varepsilon N_o$ droplets impacting and breaking up while the remaining droplets are expected to travel through the grid without breakup, which is $(1 - \varepsilon)N_o$. For each liquid droplet, in the current analysis, the Sauter mean diameter of the incoming droplets is taken as $d_o$. The droplet has velocity of $u_d$.

Figure 5-26: Sketch of Droplet Breakup at SG

Cossali et. al. (2008) studied the droplet breakup process unto a heated flat plate, in which the droplet atomization is only due to the shattering effect. Their study shown that upon
impinging, a mist consisting of large amount of extremely fine droplets is formed in addition to shattered droplets that are relatively large in size compared to the droplet mists. Both are thermally active. **Figure 5-27** shows the photographic results for droplet breakup due to shattering obtained by Cossali et. al. 2008 at two non-dimensional time frames.

![Figure 5-27: Droplet Breakup on a Hot Plate with Surface Temperature of 230 °C and We = 285](image)

The droplet breakup at the SG is considered to be a combination of cutting and shattering effect. Upon impinging the lower edge, a droplet is splitted into several droplets of sizes comparable to the original droplet and a large amount of fine droplets. The number and the size of the two droplet groups are \((n_t, d_t)\) and \((n_s, d_s)\), respectively. These daughter droplets, once generated, will be entrained in the vapor flow and pass through the SG region, eventually reaching the measurement location at the downstream of SG together with the remaining droplets.

During this period, significant mass and heat transfer are expected to take place since the rod and vapor temperature are quite high in the DFFB regime. This is especially true for the small droplet group due to their significantly large interfacial heat transfer area resulted from a large droplet population. In addition, the presence of a SG will serve to further enhance the heat transfer process downstream via turbulent mixing. Subsequently, for a single liquid droplet traveling within a rod bundle geometry during reflood, its size is constantly reducing due to evaporation at the interface.
In addition to droplet size, the results of Yao et. al. (1988) [35] show that unlike those large droplets that only slightly changed their velocity directions after breakup (4~27 deg), the injection angles for small droplet group are observed to be very large (65~90 deg). After breakup, the small droplets generated by shattering are expected to bounce back and forth between rod bundle and SG surfaces, exchanging heat with those surfaces until completely evaporated due to their small heat capacities. In Figure 5-26, the droplet breakup at the spacer gird bottom edge is shown as well as the large and small droplet group trajectory. For large droplets produced by cutting effect, they are expected to travel through the SG easily since they are injected in relatively small angels. On the other hand, for those small droplets with larger injection angels, they may bounce several times between the hot surfaces before exiting the SG, losing liquid mass along the way due to evaporation as a result of convection, radiation and direct heat transfer between droplets and walls. Conceivably, a large portion of the small droplets, especially the fine droplet mist created by shattering, might be completely evaporated during this process despite of their different trajectories. Therefore, this portion of liquid droplet mass is actually lost before moving to the downstream location.

In order to take into account of the liquid mass loss during the forward-marching period of the droplets within the SG, a liquid droplet mass loss coefficient $\gamma$ is thus assumed to represent the mass percentage of those small droplets that are lost due to heat transfer and/or any other possibilities such as the droplet deposition. As a result, for the total small droplet number of $n_s$ generated immediately after the breakup of a single droplet, only $(1 - \gamma)n_s$ small droplets can survive and make it to the measurement location at some distance downstream of the SG. Therefore, when deriving the outgoing droplets Suater mean diameter the loss of liquid mass has to be taken into consideration. However, for large droplets, no such loss coefficient is applied since their mass and size changes with regard to themselves are not remarkable as compared with the small droplets.
5.4.1 Droplet Breakup Model Development

For each of the droplet subject to breakup, it is assumed that in total \( n \) daughter droplets will be generated on the average. Hence, the total droplet number immediately downstream of the SG is

\[
N_{\text{tot}} = (1 - \varepsilon)N_o + \varepsilon N_o n \tag{5.48}
\]

The \( n \) daughter droplets can be further expressed as:

\[
n = n_l + n_s \tag{5.49}
\]

Based on the setup described above, the conservation of liquid mass for a single droplet can be written as:

\[
\rho_o \frac{\pi d_o^3}{6} = n_l \rho_l \frac{\pi d_l^3}{6} + n_s \rho_s \frac{\pi d_s^3}{6} \tag{5.50}
\]

Assume that all the droplets before and after the breakup are in spherical shape and the liquid density is held constant at the saturation temperature, then Eq. (5.50) can be rewritten as:

\[
d_o^3 = n_l d_l^3 + n_s d_s^3 \tag{5.51}
\]

Like many previous studies, in the current model development the entrained droplet size are expressed as the droplet Sauter Mean Diameter denoted as \( d_{32} \),

\[
d_{32} = \frac{\sum n_j d_j^3}{\sum n_j d_j^2} \tag{5.52}
\]

where \( n_j \) is taken as the droplet number for a particular size group \( j \) within the droplet population while \( d_j \) represents the diameter of that particular size group. It has been found out that the Sauter mean diameter is an effective way to characterize the shattered new droplets after breakup, since the Sauter mean diameter seeks to conserve the ratio of droplet volume to droplet surface area, which is an important parameter heat and mass transfer calculations. For this reason, both the up- and down-stream droplet sizes will be studied based on the Sauter mean diameter.
As was proposed by Cheung and Bajorek (2011) [38], the droplet breakup process is considered to be a mass and energy transfer process. Upon impinging, a portion of the kinetic energy of the incoming droplet is converted to the surface energy required to form \( n \) daughter droplets as well as other forms of energy involved. Eq. (5.53) presents the energy balance for a droplet during the breakup process

\[
\Delta E_{KE} = \Delta E_{SE} + \Delta E_{viscous} + \Delta E_{potential}
\]

(5.53)

where

\[
E_{KE} = \frac{1}{2} \rho \left( \frac{\pi d^3}{6} \right) u^2
\]

(5.54)

for incoming droplets, \( d \) and \( u \) are given as \( d_o \) and \( u_o \), respectively. The change of droplet surface energy is taken as

\[
\Delta E_{SE} = \pi \sigma \left( n_t d_t^2 + n_s d_s^2 - d_o^2 \right)
\]

(5.55)

From Eq. (5.53) it can be seen that the decrease in the incoming droplet kinetic energy is converted into the increase in the surface energy, the viscous dissipation of the droplet which adds up to the internal energy, and the change in potential energy of gravity. Note that the heat transfer process from the surface to liquid droplet upon impinging is neglected since the time period for liquid-wall contact is quite short. In addition, the variation in viscous dissipation and gravity can be neglected in the current derivation due to the fact that they only contribute to a minor portion of the total energy transformation. The fraction of the kinetic energy of the incoming droplet that is converted to the surface energy of newly generated droplets is denoted as \( k \) in the following expression:

\[
k = \frac{\Delta E_{SE}}{E_{KE, in}} = \frac{\pi \sigma \left( n_t d_t^2 + n_s d_s^2 - d_o^2 \right)}{\frac{1}{2} \rho \left( \frac{\pi d_o^3}{6} \right) u_o^2} = \frac{(n_t d_t^2 + n_s d_s^2 - d_o^2) / d_o^2}{We/12}
\]

(5.56)

where, the incoming droplet Weber number is written as:

\[
We = \frac{\rho u_o^2 d_o}{\sigma}
\]

(5.57)
In the work of Cheung and Bajorek (2011) [38], the energy conversion fraction $k$ was correlated to the incoming droplet Weber number based on the RBHT test data as:

$$k = 2.164 We^{-0.442}$$  \hspace{1cm} (5.58)

According to Eq. (5.52), the Sauter mean diameter downstream of SG can be written as

$$d_{32} = \frac{(1-\varepsilon)N_o d_o^3 + \varepsilon N_l n_l d_l^3 + (1-\gamma)\varepsilon N_s n_s d_s^3}{(1-\varepsilon)N_o d_o^3 + \varepsilon N_l n_l d_l^3 + (1-\gamma)\varepsilon N_s n_s d_s^3}$$  \hspace{1cm} (5.59)

the above equation can be simplified to:

$$d_{32} = \frac{(1-\varepsilon) d_o^3 + \varepsilon n_l d_l^3 + (1-\gamma)\varepsilon n_s d_s^3}{(1-\varepsilon) d_o^3 + \varepsilon n_l d_l^3 + (1-\gamma)\varepsilon n_s d_s^3}$$  \hspace{1cm} (5.60)

Apply the conservation of mass Eq. (5.51) to the numerator and energy balance Eq. (5.56) to the denominator to get:

$$d_{32} = \frac{1-\gamma \varepsilon n_s d_s^3}{1-\gamma \varepsilon n_s d_s^3 + \varepsilon k We_{12}}$$  \hspace{1cm} (5.61)

Hence, the final droplet Sauter mean diameter ratio takes the form of Eq. (5.61). In order to actually apply it in the analysis, the value for $\gamma$, $n_l$, $n_s$, and $d_{32}/d_o$ need to be determined.

### 5.4.2 Determination of Key Parameters

First, the small droplet number $n_s$ can be determined by introducing a variable $C$ which represents the volume ratio of the large droplet group to small droplet group immediately after breakup. Assuming the liquid density to be constant, $C$ has the form of:

$$C = \frac{n_l d_l^3}{n_s d_s^3} = \frac{n_l d_l^3}{n_s d_s^3}$$  \hspace{1cm} (5.62)

then, $n_s$ can be expressed by Eq. (16) below:

$$n_s = \frac{n_l d_l^3}{C d_s^3} = \frac{n_l d_l^3}{C d_s^3}$$  \hspace{1cm} (5.63)
Applying the conservation of droplet mass, Eq. (5.51), and substituting \( n_i \) using Eq. (5.63) yields:

\[
d_i^3 = n_i d_i^3 + \frac{n_j}{C} d_i^3
\]  

(5.64)

Therefore, from Eq. (5.64) the ratio of large group Sauter mean diameter to the incoming droplet Sauter mean diameter is obtained as:

\[
\frac{d_l}{d_o} = \left( \frac{C}{(1+C)n_l} \right)^{\frac{1}{3}}
\]  

(5.65)

Substituting the Eq. (5.65) into Eq. (5.56), then the ratio of small group Sauter mean diameter to the incoming droplet Sauter mean diameter can now be expressed as:

\[
\frac{d_s}{d_o} = \left[ \left( \frac{k We}{12} + 1 - n_l \frac{d_i^2}{d_o^2} \right) \left/ \left( \frac{n_l d_i^2}{C d_o^2} \right) \right\right]^{-1}
\]  

(5.66)

From Eq. (5.66) it can be seen that, knowing the volume ratio, \( C \), and the droplet number for large group, \( n_l \), the Sauter mean diameter ratio for both large and small droplet groups can be determined. In the study performed by Yao et. al. (1988) [35], the number for large droplet after each breakup is generally between 1~2 on average. To complete the derivation \( n_l = 1 \) is used for now. However, a sensitivity study will be perform on this parameter together with other key parameters in the breakup process.

As for the variable \( C \), again Yao’s data is used to find an appropriate correlation for \( C \). Physically, the ratio of large droplets volume to small droplet volume is considered to be related to \( We \) and the ratio of the incoming droplet diameter to the SG thickness \( W \). If the \( We \) is larger, which indicates that incoming droplets impinge onto the SG with larger velocities, then a small value of \( C \) is expected since the effect of shattering gets more important. In addition, a larger \( d_o/W \) will result in a larger \( C \) value since in this case the cutting effect would be more pronounced. Table 5-1 summarizes Yao’s original data while Table 5-2 presents the rearranged data for the large droplet group as a function of \( d_o/W \) and \( We \). As discussed by Yao et. al. (1988) [35], for a low \( We \), a droplet may rebound as a single or double droplets. While at high \( We \),
droplet disintegration may be expected. The exact threshold value of $We$ for breakup has not been well established. In the current analysis, a breakup threshold of $We = 6$ as obtained by Yao et. al. 1984 through simple derivation was selected in order to be consistent with data.

Table 5-1: Summary of Droplet Data by Yao et. al. (1988) [35]

<table>
<thead>
<tr>
<th>Case</th>
<th>$We_o$</th>
<th>$d_o$</th>
<th>$d_o/W$</th>
<th>Volume Ratio</th>
<th>Injection Angel ($deg$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Case 1:</td>
<td>$We_o = 95.8$, $d_o = 1.27\ mm$, $d_o/W = 3.8$</td>
<td></td>
<td></td>
<td>0.042</td>
<td>90</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.958</td>
<td>27</td>
</tr>
<tr>
<td>Case 2:</td>
<td>$We_o = 534$, $d_o = 1.27\ mm$, $d_o/W = 3.8$</td>
<td></td>
<td></td>
<td>0.1</td>
<td>65</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.9</td>
<td>6</td>
</tr>
<tr>
<td>Case 3:</td>
<td>$We_o = 862$, $d_o = 1.27\ mm$, $d_o/W = 3.8$</td>
<td></td>
<td></td>
<td>0.11</td>
<td>76</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.89</td>
<td>4</td>
</tr>
<tr>
<td>Case 4:</td>
<td>$We_o = 1035$, $d_o = 1.52\ mm$, $d_o/W = 4.6$</td>
<td></td>
<td></td>
<td>0.1</td>
<td>76</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.9</td>
<td>4</td>
</tr>
<tr>
<td>Case 4:</td>
<td>$We_o = 317$, $d_o = 1.0\ mm$, $d_o/W = 5.7$</td>
<td></td>
<td></td>
<td>0.04</td>
<td>76</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.96</td>
<td>4</td>
</tr>
<tr>
<td>Case 6:</td>
<td>$We_o = 1322$, $d_o = 1.0\ mm$, $d_o/W = 5.7$</td>
<td></td>
<td></td>
<td>0.09</td>
<td>76</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>0.91</td>
<td>4</td>
</tr>
</tbody>
</table>
Table 5-2: Data for Large Droplet Group

<table>
<thead>
<tr>
<th>Data Set 1: $d_o/W = 3.8$</th>
<th>Data Set 2: $d_o/W = 4.6$</th>
<th>Data Set 3: $d_o/W = 5.7$</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>$We$</strong></td>
<td><strong>Vol. ratio</strong></td>
<td><strong>$We$</strong></td>
</tr>
<tr>
<td>6</td>
<td>1</td>
<td>6</td>
</tr>
<tr>
<td>95</td>
<td>0.958</td>
<td>1035</td>
</tr>
<tr>
<td>534</td>
<td>0.9</td>
<td>-</td>
</tr>
<tr>
<td>862</td>
<td>0.89</td>
<td>-</td>
</tr>
</tbody>
</table>

As is shown in Figure 5-28, a decreasing trend is obtained for the large droplet volume fraction to the total droplet population according to Table 5-2. For different values of $\frac{d_o}{W}$, the slopes of these trend lines are different. Based on a logarithmic data fit, the final function for large droplet volume fraction obtained has the form:

\[
F_l = a \cdot \ln(We) + b
\]  

(5.67)
From Figure 5-28 it can be seen that the values of “b” are quite similar for different \( \frac{d_o}{W} \) cases. Therefore, in the current model, a constant value of 1.05 is taken for \( b \) to complete the derivation process. The next step is to correlate “a” with \( \frac{d_o}{W} \) so that the effect of the droplet diameter and SG thickness can be included. To accomplish this, another linear curve fit is performed, as is presented in Figure 5-29. An increasing trend following the increase of \( \frac{d_o}{W} \) is obtained for “a”.

![Figure 5-29: Data Fit for Coefficient “a”](image)

Thus, “a” is expressed as a function of \( \frac{d_o}{W} \) in the form

\[
a = 0.0042 \left( \frac{d_o}{W} \right) - 0.0386
\]

(5.68)

Finally, the function for large droplets volume fraction is

\[
F_l = \left( 0.0042 \left( \frac{d_o}{W} \right) - 0.0386 \right) \ln(We) + 1.05
\]

(5.69)

Once the \( F_l \) is obtained, the variable \( C \) can be determined easily through Eq. (5.70) as
145

\[ C = \frac{F_1}{1 - F_i} \]  

(5.70)

We further work on the small droplet group mass loss coefficient \( \gamma \). As discussed earlier, \( \gamma \) represents the liquid droplet mass loss due to evaporation during the entire breakup and transport process. Therefore, it is closely related to the heat transfer processes. Eq. (5.71) presents the mass loss coefficient and its relation with the interfacial heat flux term.

\[
\gamma = \frac{m_{\text{loss}}}{m_{\text{in}}} = \frac{\pi d_s^2 q_i'' n_s L}{n_f^2 \rho_d n_s} \tag{5.71}
\]

\( L \) is the distance from initial droplet breakup point. The interfacial heat flux \( q_i'' \) includes both the interfacial convective heat transfer and the radiation heat transfer between the liquid droplet and the heated wall. \( q_i'' \) may be thus written as:

\[
q_i'' = \frac{k_v}{d_s} N_u_f (T_v - T_d) + \varepsilon \sigma (T_W^4 - T_d^4) \tag{5.72}
\]

where,

\[
N_u_f = \left( 2 + 0.57 Re_v^{0.5} Pr_f^{0.33} \right) (1 + B_f)^{-0.7} \tag{5.73}
\]

\[
Re_v = \frac{\rho_d u_r d_s}{\mu_v} \tag{5.74}
\]

\[
B_f = \frac{c_p (T_v - T_d)}{h_f \theta} = \frac{J a_f}{h_f \theta} \tag{5.75}
\]

In Eq. (5.74), \( u_r \) represents the relative velocity between the liquid droplet and vapor phases. The mass transfer number \( B_f \) is actually equal to the \( J a_f \) in the above equation. In addition, all the fluid properties are evaluated at the film temperature except the vapor density used in the \( Re_v \), in which the free stream vapor density is used. Substituting Eqs. (5.72) to (5.75) into Eq. (5.71) gives:

\[
\gamma \sim \frac{\pi d_s^2 \left( \frac{k_v}{d_s} N_u_f (T_v - T_d) + \varepsilon \sigma (T_W^4 - T_d^4) \right) n_s L}{n_f^2 \rho_d n_s} \tag{5.76}
\]
After manipulation and combination, we eventually have:

\[ \gamma \sim \frac{k_v}{c_p \mu v} \frac{N_u_f}{h_f g} \left( \frac{L}{d_y} \right) + \frac{\varepsilon \sigma (T_v - T_d)}{h_f g \rho d u_d} \left( \frac{L}{d_y} \right) \]  \hspace{1cm} (5.77)

\[ \gamma \sim \left( \frac{N_u_f f a_f}{P_r f R e_d} + Y \right) \left( \frac{L}{d_y} \right) \]  \hspace{1cm} (5.78)

where,

\[ R e_d = \frac{\rho d u_d d_s}{\mu v} \]  \hspace{1cm} (5.79)

\[ Y = \frac{\varepsilon \sigma (T_v - T_d)}{h_f g \rho d u_d} \]  \hspace{1cm} (5.80)

The dimensionless variable \( Y \) in Eq. (5.80) represents the relative strength of radiation heat transfer against the latent heat transport term. As a result, the final form obtained for the small droplet group mass loss coefficient can be expressed as:

\[ \gamma = m \left( \frac{N_u_f f a_f}{P_r f R e_d} + Y \right) \left( \frac{L}{d_y} \right)^n \]  \hspace{1cm} (5.81)

It is seen that the mass loss coefficient \( \gamma \) is closely related to the interfacial convective heat transfer, the vapor phase superheat, the radiation heat transfer as well as the distance away from the initial breakup point.

The coefficients \( m \) and \( n \) in Eq. (5.81) can then be obtained from the RBHT droplet measurement across SG 6. By comparing with the data obtained only for the dry SG condition and performing regression analysis, the coefficients \( m \) and \( n \) were found to be 0.55 and 0.27, respectively. Therefore, the final form of the droplet breakup correlation can be written as:

\[ \frac{d_{32}}{d_o} = \frac{1 - \gamma n_s d_{32}^3}{1 - \gamma n_s d_{32}^3} \frac{ekwe}{12} \]  \hspace{1cm} (5.82)

where,

\[ n_s = \frac{n_l d_l^3 d_o^3}{c d_o^3 d_l^3}, \]
\[
\frac{d_s}{d_o} = \left[ \left( \frac{kWe}{12} + 1 - n_l \frac{d^2_s}{d^2_o} \right) \left( \frac{n_l}{C} \right) \frac{d^2_s}{d^2_o} \right]^{-1}, \quad \frac{d^l}{d^o} = \left( \frac{c}{1+C} \right) \frac{1}{n_l} \frac{1}{q}, \quad C = \frac{F_l}{1-F_l},
\]

\[
F_l = \left( 0.0042 \left( \frac{d_o}{W} \right) - 0.0386 \right) \ln(We) + 1.05, \quad k = 2.164 We^{-0.442},
\]

\[
\gamma = 0.55 \left( \frac{Nu_f J_f}{Pr_f Re_d} + Y \right) \left( \frac{L}{d_o} \right)^{0.27}, \quad Y = \frac{\epsilon \sigma (T_W - T_d)}{h_f \rho_d d_u d}.
\]

In the Figure 5-30 below, the current model predictions were compared with the RBHT droplet data set in detail. It can be seen that during the DFFB regime at various reflood test conditions, the current model developed was able to predict the droplet size ratio between the up and down stream of the SG well within 17% error, which is a great improvement from the existing models.
5.4.3 Sensitivity Analysis for Key Parameters:

In order to study the effects of different key parameters implemented in the current model on the droplet breakup prediction and to verify that the current model correctly captures the physics involved, a sensitivity analysis is performed by varying each of these parameters under investigation while keeping all the other parameters constant. In the current analysis, the system pressure is fixed at a constant value of 275.79 kPa (40 psia). The parameters investigated include: incoming liquid droplet size, $d_o$, rod cladding wall temperature, $T_W$, vapor temperature, $T_v$, larger droplet number after breakup, $n_l$, distance from the initial breakup point, $L$, and the SG blockage ratio, $\varepsilon$. Besides, the slip ratio between the two phases is assumed to be 1.1 in the DFFB regime for simplicity, whenever it is needed. And $\frac{d_o}{W}$ is assumed to be 1. When performing the sensitivity analysis, it is also assumed that these parameters are independent with each other.

Table 5-3: The Baseline Condition Selected for Droplet Breakup Sensitivity Analysis

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Incoming Droplet Size, $d_o$, ($\mu m$)</td>
<td>500</td>
</tr>
<tr>
<td>Rod Cladding Temperature, $T_W$, ($K$)</td>
<td>1000</td>
</tr>
<tr>
<td>Vapor Temperature, $T_v$, ($K$)</td>
<td>800</td>
</tr>
<tr>
<td>Number of Large Droplets, $n_l$, (-)</td>
<td>1</td>
</tr>
<tr>
<td>SG Blockage Ratio, $\varepsilon$, (-)</td>
<td>0.362</td>
</tr>
<tr>
<td>Distance from Initial Breakup Location, $L$, ($m$)</td>
<td>0.1778</td>
</tr>
</tbody>
</table>

Table 5-3 gives the values of different key parameters for the baseline case. These terms are determined primarily based on the RBHT experimental data and are considered to be the most important parameters that affect the liquid droplet breakup process upon a spacer grid in the DFFB regime during reflood. Different variations for each of these parameters will be performed.
through a numerical experiment formulated in MATLAB to investigate in greater detail of current model developed.

5.4.3.1 Effect of Incoming Liquid Droplet Size, $d_o$

The incoming liquid droplet size directly affect the incoming $We$, $\frac{d_o}{W}$, as well as the droplet size after breakup. Five different initial droplet diameters were explored to investigate the effect of this parameter on the breakup process. The values used are: 200, 400, 700, 1000, 1500 $\mu m$. As can be seen in Figure 5-31, as the liquid droplet size getting smaller, the diameter ratio between the incoming droplet and daughter droplets increases gradually. This is because smaller droplets will be obtained for smaller incoming droplet after breakup, leading to the increased interfacial area exposed to vapor and heated surface for heat transfer, leading to higher liquid mass evaporation for small droplet group. When the incoming droplets are significantly small, there is a possibility that all the liquid mass in the small droplet group get completely lost as the $We$ becomes larger due to phase change, which in turn will increase the diameter ratio as a result. Such phenomenon is shown for case when droplet diameter equals 200 $\mu m$. However, this effect diminishes as the incoming liquid droplet size becomes larger and larger, when the large droplet group becomes more important.
5.4.3.2 Effect of Rod Cladding Temperature, $T_W$

During reflood, the rod bundle heat generation provides the ultimate driving force for the energy flow from the cladding to fluid. Surface temperature variation is closely correlated with the two-phase flow mass and heat transfer process. Therefore, cladding temperature is an important parameter for liquid droplet mass and heat transfer. However, in the current numerical experiment, on the other hand, the cladding temperature is de-coupled with other parameters especially the vapor temperature. This indicates that changes in cladding temperature will not affect the vapor temperature. One should always keep this in mind when analyzing the data and interpret the results. For rod bundle cladding temperature, the different values studied are: 800, 900, 1000, 1200, 1500 K. As can be seen in Figure 5-32, the wall temperature does not play a significant role in the droplet breakup process. This is because in the DFFB regime without direct liquid-wall contact heat transfer, the effect of the wall temperature, when de-coupled from
the other parameters, is mainly felt by the liquid droplets through the radiation heat transfer between the liquid droplet and wall surface. However, during a reflood transient the convective heat transfer was found to be the dominating force compared with the radiation term. As a result, very small difference was observed for different wall temperatures. In reality, changes in cladding temperature will also affect the convective heat transfer by changing the vapor temperature as well.

Figure 5-32: Model Prediction for Different Wall Temperatures

5.4.3.3 Effect of Vapor Temperature, $T_v$

As discussed, vapor temperature is one of the dominating parameters for the liquid droplet size variation before and after the breakup due to its role played in two-phase flow interfacial heat transfer. In this current study, the values studied for vapor temperature are: 410, 600, 700, 800, 1000 $K$. The 1000 $K$ case corresponds to very high vapor temperature close to the wall temperature, while the 410 $K$ corresponds to the condition close to saturation, in which the
heat transfer is minimized. It can be seen in Figure 5-33 that, as expected, the higher the vapor temperature, the higher the droplet size ratio would be due to more liquid mass evaporation.

Figure 5-33: Model Prediction for Different Vapor Temperatures

5.4.3.4 Effect of Large Droplet Number after Breakup, $n_l$

Even though in Yao’s study the experimental data was presented on the large droplet number, $n_l$, for specific tests. However, the situation in a rod geometry with reflood might be quite different from the heated strap. Therefore, it is important to further investigate the effect of the large droplet number generated after each breakup. In the current study, different values used include: 1, 5, 10, 30. In addition, instead of a fix value for $n_l$, it is expected that $n_l$ is actually a function of the incoming droplet Weber number. This is because with larger kinetics energy of the droplet, more energy can be converted into the liquid droplet surface energy, leading to more daughter droplets produced. As an attempt to study this effect of $n_l$ numerically, it is assumed that $n_l$ changes as a linear function of the incoming droplet $We$ given as Eq. (5.83):
\[ n_l = l \cdot We + 1 \]  

(5.83)

where, constant \( l = 29 \times 10^{-4} \). The above function is designed in such a way that as the \( We \) goes to 0, only one large droplet is generated since actually no breakup is assumed to occur in this case. On the other hand, there will be 30 large droplets generated from the above equation for \( We \) of \( 10^4 \). Nevertheless, more studies, either theoretical or experimental, need to be done to quantify the large droplet number in future work.

![Figure 5-34: Model Prediction for Different Large Droplet Numbers, \( n_l \)](image)

**Figure 5-34** shows the variations on the droplet size ratio using different \( n_l \). It can be seen that, \( n_l \) also has significant effect of the final prediction of the liquid droplet size ratio. As \( n_l \) increases from 1 to 30, the ratio decreases accordingly on the entire Weber number range, as expected. On the other hand, the effect of using Eq. (5.83) for \( n_l \) can be also observed from **Figure 5-34**. When the \( We \) is small, the prediction is close to \( n_l = 1 \) and gradually approaches \( n_l = 30 \) when the \( We \) is large. It should also be noted that the decrease in the droplet size ratio for larger droplet number at low \( We \) range is not physically realistic because the total droplets
that can be generated is limited by the We number. For very small We, \( n_l = 30 \) is less likely to happen, thus leading to non-physical drop in the droplet size ratio.

5.4.3.5 Effect of Distance from the Initial Breakup Point, \( L \)

Next, the effect of the distance traveled by droplets after breakup is shown in Figure 5-35 at various values: 0.001, 0.05, 0.1778, 0.25 and 0.35 m. As expected, with longer traveling distance after breakup, the droplet size ratio is higher since in this case more liquid mass is lost through evaporation. This effect is found to be more significant for liquid droplets with higher velocities, since the interfacial convective heat transfer is the dominating mechanism during this process.

![Figure 5-35: Model Prediction for Different Distances from the Breakup Point \( L \)](image-url)
5.4.3.6 Effect of the SG blockage Ratio, $\varepsilon$

The last effect investigated is the spacer grid blockage ratio. In the current model, the blockage ratio is taken as the possibility for an incoming liquid droplet to breakup. Thus, its value will significantly affect the final results. As can be seen from Figure 5-36 below, larger blockage ratio means that more incoming liquid droplets will breakup upon engaging with the spacer grid while less droplets can pass through without breaking. This leads to smaller average droplet size as well as smaller droplet size ratio downstream of the spacer grid. If the blockage ratio is zero, then no breakup is expected, thus the droplet size ratio is found to be 1. On the other hand, if the blockage ratio is 1 instead, all liquid droplets will breakup and thus the averaged liquid droplet size downstream of the spacer grid is the smallest.

![Figure 5-36: Model Prediction for Different SG Blockage Ratios, $\varepsilon$](image)
5.5 Model Development for the Spacer Grid Pressure Drop

As having been discussed in Section 4.4, the accurate prediction of the spacer grid pressure drop in DFFB regime during reflood is one of the key quantities that needs to be determined in order to have an accurate characterization for the two-phase flow behavior as well as for the rod bundle thermal hydraulics. Based on the RBHT constant reflood data, a new spacer grid pressure drop correlation is developed in this section. Significant improvement in the predicting capability has been achieved.

5.5.1 Experimental Data Reduction

There are 23 DP transducers installed in the experimental facility, covering the different axial spans over the entire axial length of the test section. In the current study, the pressure drop across SG 5 location will be investigated in detail. SG 5 is located at the upper location (2.175 m) of the test section. It is close to the peak power location (2.743 m) and the DP cell arrangement near SG 5 facilitates the study, as is shown in Figure 4-13. The experimental data reduction method adopted was described in detail in Section 4.4.1. However, instead of assuming the two-phase flow slip ratio to be either 1 or 2, the void fraction, \( \alpha \), that is used to calculate the mixture density in Eq. (4.8) and other flow quantities is obtained from the newly development vapor drift flux model as expressed in Eqs. (5.15) and (5.46). They are repeated here as Eqs (5.84) and (5.85).

\[
\alpha = \frac{j_v}{c_{oj} + u_{vj}} \quad (5.84)
\]

\[
u_{vj} = 0.0356(1 - \alpha_v) \left( \frac{\rho_d \beta_d^{1+n_v}}{\rho_v^{1-n} \mu_v} \right)^{\frac{1}{2-n}} (1 + B_{fj} \mu_m)^{1.379} \quad (5.85)
\]

where \( n \) is taken as 1 for low flooding rate condition.
Table 5-4 shows the RBHT test matrix selected for the spacer grid pressure drop correlation development. From previous system parametric study it has known that the reflood flooding rate is the most dominating factor for the pressure drop behavior. In addition, the inlet liquid subcooling temperature is found to have negligible effect of the two-phase flow thermal-hydraulics in the DFFB regime. Therefore, the tests collected have the inlet liquid injection rate ranging from 0.0191 (0.75) to 0.1016 (4) m/sec (in/sec). All the reflood tests were performed at the system pressure of 275.8 kPa (40 psia) with the peak power input of 1.31 kW/m (0.4 kW/ft) and initial rod bundle PCT of 1033 K (1400 °F). In the current model development, the difference in the inlet liquid subcooling temperature is neglected due to its minor effect.

Table 5-4: RBHT Test Matrix Selected for Spacer Grid Pressure Drop Correlation Development

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Pressure (kPa)</th>
<th>Flooding Rate (m/sec)</th>
<th>Subcooling (K)</th>
<th>Initial Peak Power (kW/m)</th>
<th>Initial Rod PCT (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Exp. 7151</td>
<td></td>
<td>0.0191</td>
<td>53</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Exp. 8011</td>
<td></td>
<td>0.0254</td>
<td>53</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Exp. 7116</td>
<td></td>
<td>0.0508</td>
<td>56</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Exp. 8009</td>
<td></td>
<td>0.0254</td>
<td>22</td>
<td></td>
<td>1.31</td>
</tr>
<tr>
<td>Exp. 8038</td>
<td></td>
<td>0.0508</td>
<td>22</td>
<td></td>
<td>1033</td>
</tr>
<tr>
<td>Exp. 7121</td>
<td></td>
<td>0.1016</td>
<td>22</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Exp. 7123</td>
<td></td>
<td>0.1016</td>
<td>22</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Owing to the complexity and limitations in various two-phase flow experiments with spacer grids, very few studies were performed on the two-phase flow pressure drop induced by a spacer grid and/or other flow structures (such as support plate, orifice, etc.). Furthermore, almost no previous study was conducted for the spacer grid pressure drop in the DFFB regime except the work by Unal et. al., 1994 [82]. As a result, there is a gap in modeling the two-phase flow spacer grid pressure drop between the bulk liquid boiling regime and the single-phase vapor regime. The spacer grid pressure drop correlations proposed can only be applied to bulk liquid boiling condition while no correlation, to the best of author’s knowledge, was developed for the DFFB regime. Yet an accurate prediction of the spacer grid pressure drop in the liquid droplet dispersed
flow is one of the crucial factors that needs to be determined in order to better predict the rod bundle heat transfer behavior during reflood since the rod bundle PCT occurs in the DFFB regime. In the literature, the existing correlations developed generally follow the same approaching of using the single-phase pressure loss coefficient to calculate the two-phase pressure drop by introducing a two-phase multiplier of various forms (Maskal and Aydogan, 2017 [84]). The general expression can be expressed as:

\[ \Delta P_{SG, tp} = \Delta P_{SG, liq}^{sp} \phi_{liq}^{tp} \]  

where, \( \Delta P_{SG, tp} \) is the two-phase flow spacer grid pressure drop and \( \Delta P_{SG, liq}^{sp} \) is the single-phase liquid flow spacer grid pressure drop when the liquid mass flow rate is the same with the two-phase mixture mass flow rate. \( \phi_{liq}^{tp} \) is the two-phase multiplier expressed in terms of the liquid phase. \( \Delta P_{SG, liq}^{sp} \) can be further expressed as:

\[ \Delta P_{SG, liq}^{sp} = C_{SG} \frac{G_{mix}^2}{2 \rho_{liq}} \]  

where, \( C_{SG} \) is the single-phase flow spacer grid loss coefficient, which is usually determined from experiments as a function of the Reynolds number and the spacer grid geometry (blockage ratio). \( G_{mix} \) is the two-phase mixture mass flux.

Based on different researchers, the expression for \( \phi_{liq}^{tp} \) takes different forms. But they are, in general, a function of the two-phase mass quality, void fraction and other fluid properties. A good collection for the two-phase flow spacer grid pressure drop multiplier is provided in the work by Maskal and Aydogan, (2017) [84]. Table 5-5 summarizes several typical correlations proposed for the two-phase multiplier. As can be seen, in order to obtain a correlation for the two-phase multiplier, various assumptions have been made in these studies. For DFFB regime in particular, some of these assumptions might not be valid, such as the HEM model, no phase change, etc.
On the other hand, both TRACE and COBRA-TF use the spacer grid pressure drop model originally developed by Yao, Loftus and Hochreiter (1984) [85] (hereafter referred to as the Y-L-H correlation), which has a different form. This model was derived after a detailed review of Rehme’s data [75] at low Reynolds number for single-phase flow. Y-L-H correlation is shown in Eq. (5.87) as a function of the $Re$ and blockage ratio.

\[
C_{sg} = 196Re^{-0.333} \varepsilon^2 \quad for \quad 10^3 < Re < 10^4
\]

\[
C_{sg} = 41Re^{-0.16} \varepsilon^2 \quad for \quad 10^4 < Re < 10^5
\]

\[
C_{sg} = 6.5\varepsilon^2 \quad for \quad 10^5 < Re
\]

As mentioned by Yao et. al. [35], a multiplier of 1.4 ($f_{loss}$) need to be applied to Eq. (5.87) in order to account for the SG sharp leading edge. Therefore, the final form of the SG loss coefficient can be expressed as:

\[
C_{sg,m} = 1.4C_{sg}
\]  

(5.88)

Table 5-5: Various Correlations Proposed for Two-phase Flow Spacer Grid Pressure Drop Multiplier

<table>
<thead>
<tr>
<th>Researcher</th>
<th>$\varphi_{liq}^{tp}$ Multiplier Correlation</th>
<th>Description</th>
</tr>
</thead>
</table>
| Richardson (Lottes 1961 [79]) | $\varphi_{liq}^{tp} = \frac{(1-x)^2 (2-\varepsilon)}{1-\alpha} \div 2$ | • Based on energy balance;  
• Void fraction assumed constant;  
• Energy loss proportional to blockage ratio. |
| Romie (Lottes 1961 [79]) | $\varphi_{liq}^{tp} = \left( \frac{\mu_{liq}}{\mu_{vap}} \right)^{x} \div \left( \frac{1-x}{1-\alpha} \right)$ | • Based on the mass and momentum balance for two phases;  
• Void fraction assumed constant. |
| Mendler et al. (1961) [80] | $\varphi_{liq}^{tp} = \left( 1 + x \frac{v_{vap} - v_{liq}}{v_{liq}} \right)$ | • Based on the phase densities in HEM at thermodynamic equilibrium;  
• No phase change assumed. |
| Lahey and Moody 1993 | $\varphi_{liq}^{tp} = \left( 1 + x \frac{v_{vap} - v_{liq}}{v_{liq}} \right)^n \div \left(1 + x \frac{\mu_{liq}}{\mu_{vap}} - 1 \right)^n$ | • Corrected Mendler correlation;  
• Account for fluid properties based on liquid-vapor viscosity ratio;  
• $n \approx 0.25$ for turbulent flow. |
\[
\phi_{\text{liq}}^{\text{tp}} = \left( 1 + x \left( \frac{\rho_{\text{liq}}}{\rho_{\text{vap}}} - 1 \right) \right)^{0.8} \left( 1 + x \left( 3.5 \left( \frac{\rho_{\text{liq}}}{\rho_{\text{vap}}} - 1 \right) \right)^{0.2} \right)
\]

- A variation of HEM
- Based on the proportion of liquid density to mixture density;
- Empirical corrections applied.

5.5.2 Comparison of Existing Correlations with RBHT Data

The above Y-L-H correlation together with other spacer gird pressure drop correlations in the literature were then compared with the current RBHT data (at SG 5 location) to evaluate their performance in predicting the two-phase spacer grid pressure drop for the DFFB regime. Data from three tests Exps. 7151, 7116 and 7123 were explored with inlet liquid injection rate of 0.0191 (0.75), 0.0508 (2) and 0.1016 (4) m/sec (in/sec), respectively. The results for Exp. 7151 are shown in Figure 5-37. For this very low flooding rate test (0.0191 m/sec (0.75 in/sec)), the slow propagation of the quench front resulted in a longer duration of the DFFB regime and a higher mass quality in the upper portion of the bundle. Under such condition, the liquid droplets tend to distribute uniformly with the flow channel and the assumption of HEM may be applied to some extent, as long as the vapor and liquid droplet velocities are close enough. However, it has been found previously that significant thermal-hydraulic non-equilibrium actually exists in the DFFB regime. This limits the use of HEM assumption in the current pressure drop prediction. As can be seen from Figure 5-37, while Beattie’s correlation tends to under-predict the experimental data initially and over-predict the data later, all the other correlation significantly over-predict the experimental data. In addition, even though the mixture Reynolds number was used for the Y-L-H correlation, poor performance of this correlation is expected since all its coefficients were obtained based on the single-phase pressure drop experimental data.
Similar trend can be also observed in Figure 5-38 and Figure 5-39. The higher the mass flow rate, the higher spacer grid pressure drop induced and the higher over-prediction of the data.
based on these correlations. This is because these correlations were developed in the two-phase flow regime in which liquid is the continuous phase. Whereas in the DFFB regime, the continuous phase is vapor. Comparisons made in this section shows the need to develop a new correlation for predicting the spacer grid pressure drop in the DFFB regime.

![Graph showing various correlation predictions compared with RBHT data in DFFB, Exp. 7123](image)

Figure 5-39: Various Correlation Predictions Compared with RBHT Data in DFFB, Exp. 7123

### 5.5.3 Development of New Two-Phase Flow Spacer Grid Pressure Drop Correlation

In order to accurately account for the two-phase flow pressure drop induced by the spacer grid in the DFFB regime, new correlation needs to be formulated based on the RBHT data. In the current study, the new correlation is formulated in a similar manner as the Y-L-H correlation. the spacer grid loss coefficient, $C_{sg}$, is correlated with the two-phase mixture Reynolds number and a spacer grid geometry correction factor, $f_{loss}$. This correction factor is used to account for the different spacer grid manufacturing conditions as well as the geometry differences. In the original
work by Yao et al. (1984), $f_{\text{loss}} = 1$ for round edge spacer grid while $f_{\text{loss}} = 1.4$ for sharp leading edge spacer grid. In order to account for the mixture viscosity used in the Reynolds number, the correlation obtained by Ishii (1977) by extending Taylor’s correlation for the mixture viscosity along the Roscoe-type power relation is used. Ishii’s correlation is written as:

$$ \frac{\mu_m}{\mu_c} = \left( 1 - \frac{\alpha_d}{\alpha_{dm}} \right)^{-2.5} \alpha_{dm}^{\alpha_d (\mu_d + 0.4 \mu_c) (\mu_d + \mu_c)} $$  \hspace{1cm} (5.89)

where,

$\mu_m =$ mixture viscosity;

$\mu_c =$ viscosity for continuous phase;

$\mu_d =$ viscosity for dispersed phase;

$\alpha_d =$ void fraction for dispersed phase;

$\alpha_{dm} =$ maximum packing fraction for dispersed phase (can be assumed to be 1).

According to the above argument and consideration, the new spacer grid pressure drop correlation is proposed as follows:

$$ C_{\text{sg}} = m R e_{\text{mix}}^n f_{\text{loss}} $$  \hspace{1cm} (5.90)

where,

$$ R e_{\text{mix}} = \frac{\rho_{\text{mix}} D_h}{\mu_{\text{mix}}}, $$

$$ \mu_{\text{mix}} = \min(\mu_{\text{bubbles}}, \mu_{\text{drops}}), $$

$$ \mu_{\text{bubbles}} = \mu_t (1 - \alpha)^a, \hspace{0.5cm} a = \frac{-2.5(\mu_t + 0.4 \mu_t)}{\mu_t + \mu_t}, $$

$$ \mu_{\text{drops}} = \mu_t \alpha^b, \hspace{0.5cm} b = \frac{-2.5(\mu_t + 0.4 \mu_t)}{\mu_t + \mu_t}. $$

Coefficients $m$ and $n$ in Eq. (5.90) will be determined by the RBHT test. The mixture viscosity is taken as the smaller value between $\mu_{\text{bubbles}}$ and $\mu_{\text{drops}}$ with maximum packing fraction for the dispersed phase to be 1. This approach is also used to determined the mixture viscosity in the COBRA-TF sub-channel analysis code. Note that when compared with the Y-L-H
correlation, the spacer grid geometry (blockage ratio) term is dropped out in Eq. (5.90) because only one type of SG was used in the RBHT test facility.

Before we work on determining $m$ and $n$, a different $f_{\text{loss}}$ other than 1.4 should be used in the current study due to the different spacer grid geometry used in the RBHT experiment. This is done by comparing the RBHT single-phase liquid spacer grid pressure drop data with the Y-L-H correlation predictions. Figure 5-40 shows the prediction result. It was found that the value of 1.75 for flow best fit the current RBHT single-phase data. Therefore, in the following derivation, $f_{\text{loss}} = 1.75$ will be used in the two-phase correlation development.

Next, the two-phase flow spacer grid loss coefficient, $C_{sg}$, can be correlated with the $Re_{mixture}$, as already defined in Eq. (5.90). Figure 5-41 shows the curve fit results for the spacer grid loss coefficient in the DFFB regime using RBHT experimental data (0.0191-0.1016 m/sec (0.75-4 in/sec)). Taking $f_{\text{loss}} = 1.75$, it was found that $m = 28.57$ and $n = -0.46$ best fit the current data.

Figure 5-40: Y-L-H Correlation Predictions for Single-Phase RBHT Data with $f_{\text{loss}} = 1.75$
As a result, the new correlation is given as Eq. (5.91):

\[ C_{sg} = 28.57 \times Re_{mix}^{-0.46} f_{loss} \]  \hspace{1cm} (5.91)

### 5.5.4 New Correlation Predictions for RBHT Data

**Figure 5-42** to **Figure 5-48** show the new model predictions for the RBHT data set. It can be seen that very good agreement in spacer grid pressure drop prediction was achieved using the derived correlation. Despite of the different liquid inlet subcooling temperature, the current correlation is able to predict the spacer grid pressure drop with significantly improved accuracy. This confirms the fact that liquid subcooling temperature at the inlet has a rather minor effect on the flow and heat transfer behavior for the DFFB regime during reflood. It can be seem from **Figure 5-42** to **Figure 5-48** that the spacer grid induced pressure drop generally increases as the two-phase flow mixture mass flow rate increases in the DFFB regime. This is because with increased mass flow rate more liquid droplets can be entrained in the vapor flow, leading to
increased pressure loss across a spacer grid. In addition, due to the nature of the two-phase flow, larger oscillatory was observed for the tests with higher inlet liquid flooding rate.

Figure 5-42: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 7151

Figure 5-43: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 8011
Figure 5-44: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 8009

Figure 5-45: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 7116
Figure 5-46: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 8038

Figure 5-47: Spacer Grid Pressure Drop Prediction Based on New Correlation, Exp. 7121
In **Figure 5-49**, the model prediction using the newly developed correlation is compared with the entire RBHT data set selected. In order to mitigate the data scatter caused by oscillations especially for higher flooding rate tests due to the higher transient two-phase flow behavior, both the experimental data and predicted results have been averaged every 5 seconds. It can be seen from the Figure 5-49 that the current correlation was able to predict the RBHT data within 25% error.

From **Table 5-4** it can be seen that the current was developed based on the reflood tests performed at 27579 kPa (40 psia). In order to investigate the performance of the current correlation for tests at other system pressures, the RBHT data set with varying system pressure was used. The comparison results are shown in **Figure 5-50** below. The newly developed correlation was able to predict the spacer grid pressure drop at different system pressure conditions well within the 25% range. For the test Exp. 8023 with the highest pressure (413.69 kPa (60 psia)), under-prediction of the data was observed. However, cautions need to be taken...
when interpreting the experimental data for this test. For Exp. 8023, large system pressure oscillations were observed during the reflood, as can be also seen in Figure 4-16. Such oscillation in system pressure will affect accurate measurement of the fluid quantities, leading to relatively large scatter. This further results in larger scatter in the two-phase flow mass quality and void fraction calculations based on measurements. Therefore, this is the reason why under-prediction was observed. Last, it can also be clear seen from Figure 5-50 that the higher system pressure tests had lower spacer grid pressure drop in the DFFB regime during reflood transients.

The correlation prediction is then compared with the two reflood tests performed at different rod bundle power levels, i.e., Exp. 7168 with 0.98 kW/m (0.3 kW/ft) and Exp. 8013 with 1.97 kW/m (0.6 kW/ft). Figure 5-51 shows the results. Good agreement was achieved for these two tests as well. The relatively large scatter for Exp. 7168 is mainly due to the two-phase flow void fraction prediction and the following mixture density calculation.

![Figure 5-49: RBHT Data Compared with Prediction Using Equation (5.91) in DFFB](image)
In the literature, very few studies were performed for the spacer grid pressure in the DFFB regime, mainly because of the limited experimental capability and lack of fundamental fluid quantities during the complicated reflood transients. Unal et. al. (1994) [82], based on their experimental data obtained for a 3 by 3 rod bundle in the DFFB regime, investigated the spacer grid pressure drop. Therefore, the current trend obtained between the \( Re \) and \( C_{sg} \) based on the RBHT tests can be further compared and verified by Unal’s data. The spacer grid loss coefficients were obtained directly from the figures presented in their study. Since Unal’s data lacks key information on the two-phase flow void fraction and fluid quantities at the local measurement location, the \( Re_{mix} \) defined in Eq. (5.90) cannot be calculated. In order to compare the RBHT trend with Unal’s data, the RBHT data was re-processed based on the general mixture \( Re \) with the mixture viscosity be defined as below (McAdam 1942 [102]):

\[
\mu_{mix} = \left( \frac{1-x}{\mu_l} + \frac{x}{\mu_v} \right)^{-1} \tag{5.92}
\]

Then, by presenting the RBHT data and Unal’s data on the same plot, as is shown in Figure 5-52, it can be seen that Unal’s data generally falls into the trend obtained from the RBHT data despite of rather different SG geometries and two-phase flow conditions for these two experiments. It was found that with increased flow mixture Reynolds number, the spacer grid loss coefficient decreases accordingly. Therefore, it indicates that the current RBHT data correctly reflect the SG pressure drop trend as a function of \( Re \) and the current correlation developed should yield a good prediction for Unal’s data as well if the local fluid quantities are known.
Figure 5-50: Equation (5.91) Prediction Compared with Other RBHT Data in the DFFB Regime
(Effect of System Pressure)

Figure 5-51: Equation (5.91) Prediction Compared with Other RBHT Data in the DFFB Regime
(Effect of Rod Bundle Power Input)
Figure 5-52: RBHT Data Compared with Unal’s Data in the DFFB Regime
Chapter 6

Numerical Simulation and Comparison of Reflood Transients Using Sub-Channel Analysis Code COBRA-TF

One of the important applications of the RBHT reflood data is to validate the numerical computational tools for nuclear reactor thermal-hydraulic analyses, such as the sub-channel analysis code COBRA-TF. Accurate predication of the thermal-hydraulic behavior within a reactor core under normal operation and/or accident scenarios is of crucial importance in nuclear reactor thermal-hydraulic design and safety. These codes, either in one-dimensional or three-dimensional frame, are generally based on the conservation equations for mass, momentum and energy of the fluid together with many flow and heat transfer models. Due to the lack of detailed experimental data on the reflood transients, especially for the DFFB flow regime above quench front, models used in these thermal-hydraulic codes are subject to over simplified assumptions and large uncertainties. In order to improve the predication capability of the thermal-hydraulic analysis code, the RBHT reflood data is an ideal experimental data set that can be used for model validation and development. Since the DFFB regime is the most important phase during reflood as the peak cladding temperature usually occurs in this flow regime, in this chapter, the COBRA-TF predictions for the fluid mass and heat transfer transport are compared with the RBHT data with an emphasize on the DFFB regime. The prediction capability of COBRA-TF for the DFFB regime is investigated in detail. Also, new pressure drop models developed for the spacer grid in the DFFB regime is incorporated into the code to further enhance the code capacity.
6.1 COBRA-TF Sub-Channel Analysis Code

The COBRA-TF computer code was originally developed at the Pacific Northwest Laboratory under the sponsorship of the United States Nuclear Regulatory Commission. Further improvements and developments have been made to COBRA-TF ever since. It was specifically designed to provide best-estimate thermal hydraulic analyses of a LWR vessel for design basis accidents and anticipated transients. Therefore, COBRA-TF is one of the most suitable codes for LWR rod bundle accident analysis, particularly the LOCA.

6.1.1 Introduction

COBRA-TF uses a two-fluid three-field (i.e., liquid field, vapor field and droplet field) solution scheme for the flow field calculation. Each field is modeled with its own set of conservation equations with the exception being that the liquid and droplet fields are assumed to be in thermal equilibrium and, thus, they share one energy equation. These sets of conservation equations can be formulated either in a Cartesian coordinate system (3-D solution) or in a simplified sub-channel approach. The equations are solved simultaneously using the Semi-Implicit Method for Pressure-Linked Equations (SIMPLE). There is a complete set of criteria incorporated into COBRA-TF for selecting different heat transfer regimes as need. It is also able to simulate the droplet entrainment/de-entrainment dynamics as well as its breakup process on spacer grids. Moreover, a top liquid deluge model is used for its hot wall flow regimes such that the falling film from the top of the bundle can be captured (Paik et al. 1985 [34]). It has been experimentally confirmed that such top-down quench phenomenon may occur during reflood [11-13, 101].
For a given computational cell, mass, momentum, and energy are conserved. This behavior is captured for each field using three conservation equations (with the exception of the liquid and droplet fields sharing an energy equation). This is the heart of the two-fluid model by modeling each phase with its own set of mass, momentum, and energy equations. The conservation equations, of course, are dependent on one another and are linked by interaction terms that account for things like mass and heat transfer between phases (e.g. evaporation/condensation or entrainment/de-entrainment). The sub-channel approach is a simplification of the conservation equations that only considers two flow directions - axial flow and lateral flow. The lateral flow directions are not a set of fixed coordinates; instead, the term, “lateral flow” covers any orthogonal direction to the vertical axis. Because fixed coordinates are not defined for the lateral direction in the sub-channel approach, lateral flow has no direction once it leaves a gap. Lateral flow enters a sub-channel volume through “gaps” between the volume and other adjacent sub-channel volumes. This is a suitable assumption for the axially-dominated flow of a reactor fuel bundle because the relatively minuscule lateral flows transfer little momentum across sub-channel mesh cell elements. The result of this assumption is one less momentum equation for each of the three fields.

6.1.2 COBRA-TF Mesh Structure

For the aforementioned conservation equations to be of practical use, they must be applied in some way to the modeling geometry. This is achieved by generating a mesh (grid) of volumes and then setting up the collection of mass, momentum, and energy equations for each field in each of the mesh cells. Actually, two meshes are utilized which are staggered from one another. One mesh – further called the scalar mesh - is used to define the scalar variables (e.g. \( \alpha \), \( p \), \( h \), and fluid properties). The second mesh - further called the momentum mesh - is used to
defined the fluid velocity field. The choice for a staggered mesh comes down to numerical
stability and accuracy issues which are discussed in more detail by Patankar [103].

Technically speaking, there are actually three different meshes; the momentum mesh is
comprised of transverse momentum cells and axial momentum cells. This is due to the fact that
we solve for both transverse and axial velocities. Figure 6-1 [2] shows how scalar mesh cells and
axial momentum mesh cells overlap. Note that the scalar mesh cell and the axial momentum mesh
cell just above it have the same index identifier (in the figure and this document, they are
differentiated by capital and lowercase letters, but in the actual COBRA-TF source code, the case
is the same).

The scalar quantities are defined at the center of the scalar mesh cells and the velocities
are defined at the center of the momentum cells. As a result of this fact and the staggered mesh
approach, the axial velocities will be available at the top and bottom faces of the scalar mesh cell.
In a similar fashion, the transverse velocities will be available on the side faces of the scalar mesh
cells because the transverse momentum cells overlap the scalar mesh cell sides, as shown in
Figure 6-1 [2]. Note that in Figure 6-1, the adjacent lower- and higher numbered scalar mesh
cells are labeled with indices ii and jj. The mesh generation process is handled by COBRA-TF
after basic model information is provided by the user. That basic information includes the number
of model sections, channels per section, scalar mesh cells per channel, and channel connection
information. A section is a grouping of channels which are all of the same total length. Sections
communicate with one another via mass, energy, and momentum transport, but only in the axial
direction (i.e. there are no gaps between sections). The user defines the number of channels
located within each section and then tells COBRA-TF how the channels communicate with
channels above or below the section they reside in (or if they don't communicate with any
channels, be they next to a boundary).
Figure 6-1: Scalar Mesh Cell and Axial Momentum Mesh Cell Configuration

Inside the section, the user declares which sub-channels communicate laterally via gaps, the channel geometry information, and the number of scalar mesh cells within the channels. It is also possible to have variable scalar mesh cell lengths in a section, should it be necessary to obtain greater flow field detail in a certain model location.

6.1.3 COBRA-TF Numerical Solution Scheme

COBRA-TF uses a form of the Semi-Implicit Method for Pressure-Linked Equations (SIMPLE) to solve the conservation equations that have previously been defined. The steps of the SIMPLE algorithm, taken from Patankar [103], are:

1. Guess the pressure field, \( p^* \).
2. Solve the momentum equations to obtain fluid velocities, \( u^* \), \( v^* \), and \( w^* \).
3. Use the continuity equation to solve for the pressure field correction, \( p' \).
4. Calculate the corrected pressure field, \( p \), by adding \( p' \) to \( p^* \).
5. Calculate the corrected velocity field \((u, v, \text{ and } w)\) using the corrected pressure field.
6. Solve remaining discretized equations that influence the flow field (i.e. energy equation).
7. Treat the corrected pressure, \( p \), as the new guessed pressure, \( p^* \) and repeat steps 1-6 until convergence is reached.

Each of these steps is discussed with regards to the COBRA-TF solution process, as it differs in some respects. For Step 1, the user must provide a reference pressure to COBRA-TF. It will calculate the pressure field accounting for hydrostatic forces using this value and use that as the initial guess. COBRA-TF performs a solution of the conservation equations for each time step in the modeled transient - every time step after the first will use the previous time step calculated pressure field as a guess for the new one.

For Step 2, the transverse momentum equations are solved first and then the axial momentum equations. As for step 3, Patankar was considering an unheated case where fluid energy was constant; however, for our case, it is necessary to use both the continuity and energy equations to determine the pressure correction. The independent scalar cell properties and momentum cell mass flow rates are solved for using the previous time step values and the effect of the pressure correction. Step 3 forms what is known as the “inner iteration” which requires the solution of the pressure correction equations. Since there will be one pressure correction equation for each scalar cell in the mesh, this can be a very large matrix to solve. COBRA-TF is capable of solving this matrix by direct Gaussian elimination or by using one of the iterative Krylov methods.

With the corrections to the pressure field calculated, the current-iteration pressure field is then determined in Step 4. Additionally, back-substitution is performed in order to get the current-iteration values for the other dependent variables (e.g. void and enthalpy). Since the pressure field was changed, the velocities are updated accordingly in Step 5.

For Step 6, other equations solved by COBRA-TF include the interfacial area transport equation for tracking the interfacial area of the droplet field and possibly fuel rod heat transfer and decay heat equations. Steps 1-6 form one cycle of what is known as an “outer iteration”.
COBRA-TF has a set of convergence criteria that are checked upon the completion of Step 6. If they are not met, the current iteration is attempted again, but with a smaller time step. This is done up to a user-specified number of times for each outer iteration. If the outer iteration converges, COBRA-TF then moves onto the next time step, repeating Steps 1-6 and marching through time until the simulation is completed.

There are three main steps of this algorithm that will now be described in greater detail: solution of the momentum equations, solution of the mass and energy equations, and solution of the system pressure matrix. These steps are not mutually exclusive, as the momentum equations are initially solved and then later corrected after solution of the pressure matrix, and the derivation of the mass and energy equations are used for solution of the pressure matrix. However, it is sufficient for our purposes to lead the discussion in this manner while making note of these matters along the way.

With the momentum equations, we are interested in solving for the field mass flow rates for the current time step. In the momentum equations, the new-time mass flow rates appear in the temporal term and the shear terms. Everywhere else where the mass flow rate is needed (e.g. advection terms), the old-time value is used. The solution process is to solve the transverse momentum equations first and the axial momentum equations second. Whether we use the sub-channel or Cartesian form of the momentum equations, COBRA-TF will solve the liquid, droplet, and vapor transverse momentum equations simultaneously for one gap at a time.

One further simplification can be made by substituting A for the explicit terms, B for the terms that multiply by pressure drop, C for terms that multiply by new-time liquid mass flow rate, D for terms that multiply by new-time vapor mass flow rate, and E for terms that multiply by new-time entrained mass flow rate. This yields:

\[
\dot{m}_v = A_2 + B_2 \Delta P + C_2 \dot{m}_l + D_2 \dot{m}_v + E_2 \dot{m}_e
\] (6.1)
The subscript is used to identify the field and “2” was used to stay consistent with the COBRA-TF source code (“1” is for liquid and “3” is for droplets). Similar to the transverse vapor momentum equation, reductions can be performed for the other fields in both directions. The forms of the axial and transverse momentum equations are the same. The reduced liquid and entrained momentum equations are shown below.

\[ \dot{m}_l = A_1 + B_1 \Delta P + C_1 \dot{m}_l + D_1 \dot{m}_v \]  \hspace{1cm} (6.2)

\[ \dot{m}_e = A_3 + B_3 \Delta P + C_3 \dot{m}_v + E_3 \dot{m}_e \]  \hspace{1cm} (6.3)

Once the A - E terms are defined in COBRA-TF, as well as the old-time pressure drop, Eqs. (6.2)-(6.3) can be rewritten in matrix form, as follows:

\[
\begin{bmatrix}
 C_1 - 1 & D_1 & 0 \\
 C_2 & D_1 - 1 & E_2 \\
 0 & D_3 & E_3 - 1
\end{bmatrix}
\begin{bmatrix}
 \dot{m}_l \\
 \dot{m}_v \\
 \dot{m}_e
\end{bmatrix}
= \begin{bmatrix}
 -A_1 - B_1 \Delta P \\
 -A_2 - B_2 \Delta P \\
 -A_3 - B_3 \Delta P
\end{bmatrix}
\]  \hspace{1cm} (6.4)

Once the above system of equations is solved by using Gaussian elimination, the mass flow rates for the phases are evaluated in terms of the pressure gradient across the momentum cell \( \Delta P \). When the flow rates are known, the tentative velocities for the phases can be calculated. These tentative velocities are then used to solve mass and energy equations.

The mass and energy equations are non-linear equations since they consist of terms which include the multiplication of the unknowns. In order to solve the mass and energy equations, the right hand side of the equation is moved to the left hand side. When the solution converges, the right hand sides of the new equations, i.e., the residual errors in the equations become zero.

The mass and energy equations are solved by taking the variation of each of the independent variables into account using Newton-Raphson method. In order to do this, the equations are linearized with respect to the independent variables, \( p, \alpha_v, \alpha_v H_v, (1 - \alpha_v) H_l, \alpha_e \) and \( \alpha_g \). The unknown correction term can be solved using Gaussian elimination if there were an equal number of unknowns and equations. However, the matrix obtained by Newton-Raphson
method will not be a $n \times n$ matrix because of the effect of pressure in the connecting cells and, in turn, there will be a greater number of unknowns for the current scalar cell. The mass and energy equations are setup for one scalar mesh cell. Every cell in the mesh will have its own Jacobian and its own set of residuals. This problem is remedied by forming and solving the pressure matrix for the mesh. Either direct inversion for small computational meshes, or Gauss-Siedel iterative technique for large meshes can be used to solve the pressure matrix.

With the pressure corrections calculated for the mesh, equations can be solved for each scalar mesh cell to determine its new time step pressure. With the bottom of the reduced Jacobian matrix known in every scalar cell, back-substitution can be used to calculate the other new time step variables. The back-substitution would complete one iteration of the Newton-Raphson process and, at this point, it would be possible to perform further iterations; however, COBRA-TF performs a non-iterate solution in which the first solution obtained by the Newton-Raphson method is used as that time step's solution. By limiting the time step size, the code solution will remain stable throughout the calculation.

The velocity field is corrected for the newly calculated pressure field using the derivative of velocity with respect to pressure. It is a linear relationship which can be obtained directly from the momentum equations. If the convergence criteria are satisfied, the iteration is completed and the code moves on and performs the calculations for the next time step. If the convergence limits are not satisfied for specified number of outer iterations, i.e., the iterations performed to solve momentum equations, then outer iteration is considered to have failed. All fluid conditions are reset to the previous time step value, the time step size is reduced by half and the calculation is repeated.
6.1.4 COBRA-TF Flow and Heat Transfer Models

The heat transfer package in COBRA-TF consists of a library of correlations and a selection logic which allows the code to predict a boiling curve as a function of the computational cell void fraction, pressure, mass flow and the heated surface temperature. The heat transfer package which is used calculates both the wall to fluid heat transfer as well as the interfacial heat transfer between the phases. Since separate energy equations are used for the phases, a non-equilibrium flow will be calculated in some cases. Therefore, the interfacial heat transfer and the interfacial heat transfer area are calculated to determine the temperature of each phase. Heat transfer models incorporated into COBRA-TF cover all the heat transfer regimes including single phase liquid and vapor, nucleate boiling, subcooled nucleate boiling, critical heat flux, minimum film boiling temperature, transition boiling, inverted annular film boiling, and dispersed flow film boiling.

The existence of spacer grid reduces the flow area, causing contraction and expansion. They also induced higher pressure drop due to form loss. Thus, effect of spacer grid on the flow and heat transfer need to be accounted. In COBRA-TF, models for the convective heat transfer enhancement, grid rewetting, and droplet breakup upon spacer grid are used to capture these processes.

6.2 Numerical Simulation Model for RBHT Reflood Tests

In order to simulate the reflood transients for the RBHT reflood tests using COBRA-TF, a model that reflects the RBHT test facility characters as well as the boundary conditions should be built accordingly.
Figure 6-2 presents the COBRA-TF modeling strategy for the RBHT test section discussed previously in Chapter 3. Three modeling sections were used in the current analysis with Section 1 (containing 4 sub-channels and 31 nodes) modeling the heated length (3.66 m (144 in)) within test section and Sections 2 (containing 4 sub-channels and 2 nodes) and Section 3 (containing 1 sub-channels and 2 nodes) modeling the upper plenum geometry. Transport parameters between Sections are properly defined. In addition, mass and heat transfer across sub-channel within each Section are also defined (through gaps and thermal connections) according to the RBHT geometry.

For each sub-channel, a certain number of heated rods as well as the un-heated structures (spacer grids) are specified. In total the test section has been divided into 30 scalar nodes (31 momentum nodes) with varying lengths for these nodes taking into account the TC’s locations on the heather rods as well as the DP cell arrangement on the flow housing. The 7 spacer grids used in the RBHT test are also modified in the current code study. They corresponds to the axial nodes of 2, 5, 9, 14, 19, 25, and 29.

The model inlet conditions are specified as the flooding rate and inlet liquid enthalpy, which are applied to the first node for each sub-channel in Section 1 according to their geometries. The outlet boundary conditions specified as the pressure boundary is applied to the second node of Section 3.

In Figure 6-3 the cross-sectional view of the COBRA-TF model for Section 1 is shown. It can be clearly seen that four sub-channels are used to represent the entire rod bundle area assuming axisymmetric condition. CH 1 is the central sub-channel in the heated section accounting for the hot channel within the inner 3 × 3 rod bundle geometry. CH 2 and CH 3 take the regions between 3 × 3 and 7 × 7 bundle geometry, respectively. While CH 4 accounts for the periphery region between the rod bundle and the flow housing. These is a gap between every two sub-channels to account for any transverse exchanges of mass, momentum and energy.
In the current study, in order to simulate the 49 rods while at the same time save the computational time, based on their different radial locations and power input, five types of heated rods together with one type of unheated support rods are simulated. A unique multiplication factor has been applied to each of these rod types in order to have the correct representation of the power generation. Each type of heater rod has the same color as is shown in Figure 6-3 and different colors represent different locations within the rod bundle. These rods, either heated or unheated, will exchange energy with the fluid in the vicinity.
Both the heated structures (heater rods) and the unheated structures (unheated support rods, flow housing) have been further divided into several radial temperature nodes to account for the heat conduction within the geometry. Figure 6-4 shows the nodalization for the heated rods. As the same with the heater rods actually used in the RBHT test facility, four material regions are divided for the COBRA-TF modeling. Material properties in Regions 1 and 3 are specified as the Boron-Nitride, in Region 2 is as the heat coil and in Region 4 as the cladding. Typically three temperature nodes are defined for each region. In addition, the flow housing has also been divided into three temperature nodes with an insulation material region surrounded.
6.3 Simulation Result Comparison and Discussion

In this section, the COBRA-TF calculation results for selected RBHT reflood tests are shown and compared with actual data. Various COBRA-TF runs were performed covering all the test conditions encountered in experiments in order to complete a comprehensive and detailed verification and validation process for various thermal-hydraulic models related to the reflood transients, such as the quench front propagation, two-phase flow mass and heat transfer incorporated, two-phase flow pressure drop, liquid droplet entrainment, spacer grid heat transfer and pressure, etc. The results obtained in this section are useful not only for appropriately modeling the reflood transients but also for future model improvement and development.

The current COBRA-TF predictions are compared with the RBHT constant reflood tests selected. The test conditions for these tests are given in Table 6-1. In total, 18 tests were used for comparison. They cover the pressure range from 124.11 (18) to 413.69 (60) kPa (psia), liquid injection rate from 0.0191 (0.75) to 0.2032 (8) m/sec (in/sec), peak power from 0.98 (0.3) to 2.30 (0.7) kW/m (kW/ft) and inlet subcooling from 5 (9) to 150 (83) K (°F). A corresponding COBRA-TF calculation was performed for each of these tests in order to have a direct comparison for various two-phase flow and heat transfer phenomena involved.
Table 6-1: Test Conditions for Selected RBHT Constant Reflood Tests

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Pressure kPa (psia)</th>
<th>Flooding Rate m/sec (in/sec)</th>
<th>Inlet $\Delta T_{sub}$ K ($^\circ$F)</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Initial PCT K ($^\circ$F)</th>
</tr>
</thead>
<tbody>
<tr>
<td>8089</td>
<td>124.11 (18)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8021</td>
<td>137.90 (20)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
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<td>5 (9)</td>
<td>1.31 (0.4)</td>
<td>1033 (1400)</td>
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6.3.1 Transient Simulation for Reflood

COBRA-TF is capable of calculating and providing information for almost all important flow and heat transfer quantities at each time step for all the cells and channels modeled, from rod surface temperature, heat flux, heat transfer coefficient, to two-phase flow void fraction, temperature, velocity, and interfacial heat transfer for each phases, and to pressure drop, spacer grid quenching conditions, etc. Therefore, it is very convenient and quite useful to present the
transient variations of these quantities as the time progressing for the entire calculation domain. For reflood tests specifically, the visualization of the transient variation for a certain parameter within the entire test section will greatly help the analyst to get a comprehensive understanding of the cooling and quenching processes during reflood transients.

In this section, visualization for two of the most important quantities, the two-phase flow void fraction and the rod cladding temperature, are presented for Exp. 8009 at four distinct times, covering the entire reflood transients. Due to the axisymmetric assumption, only data from the first quadrant of Figure 6-3 is shown.

**Figure 6-5** first shows the transient variations for the void fraction and rod cladding temperature at 21 sec into reflood, which corresponds to the beginning of liquid injection. It can be seen that at this moment, the subcooled liquid was just injected into the test section from the bottom and the major portion of the rod bundle was still covered by vapor phase with the void fraction being 1. At the same time, the rod bundle axial temperature distribution generally followed the power profile applied, with the peak cladding temperature being identified around the peak power location. The subcooled water quickly cooled down the bottom portion of the rod bundle and a quench front propagated upward gradually, delivering two-phase flow mixture above the quench front to pre-cool the rod bundle surface. Note that the temperature of the support rod (labeled as “HR 7”) at the corner was a little bit lower than the other rod since it is not heated.

100 sec later, the quench front achieved an axial elevation of around 1 m along the rod bundle, as is shown in Error! Reference source not found.. The majority portion of the rod bundle at this time was still surrounded by vapor phase as shown in the void fraction distribution plot while the test section entrance remained as single-phase liquid. Due to the quench front propagation and the effect of pre-cooling achieved by the continuous vapor flow with liquid droplet dispersed, the rod cladding temperature tended to decrease from their initial value,
indicating that the rod temperature has turned around after reaching its peak between 20-100 sec period.

Figure 6-5: Transient Variations of the Sub-Channel Void Fraction and Rod Cladding Temperature at 21 sec after Reflood, Exp. 8009

The channel void fraction and rod cladding temperature distributions at the time of 301 sec are shown in Figure 6-7. Up to this time, the quench front has propagated deep into the test section, cooled down a large portion of the rod bundle surface. The portion around peak power location was still hot due to higher power generation. Also noticed is that, the top-down quench front (top-deluge regime) was predicted by COBRA-TF, indicating that there was liquid falling back into the rod bundle area from outlet of the test section due to liquid accumulation. In the current practice of the COBRA-TF model, the top deluge regime (occurring when void fraction is below 0.8) consists of large liquid slugs, with diameter equal to the channel hydraulic diameter. It is resulted from the existence of a top quench front within the calculation cell. The top quench, on the other hand, is determined in a mesh cell that is in the hot wall regime with a normal wall regime in the mesh cell above. The corresponding void fractions in four sub-channels are presented on the LHS of Figure 6-7.
Figure 6-6: Transient Variations of the Sub-Channel Void Fraction and Rod Cladding Temperature at 101 sec after Reflood, Exp. 8009

Figure 6-7: Transient Variations of the Sub-Channel Void Fraction and Rod Cladding Temperature at 301 sec after Reflood, Exp. 8009

Last, the channel void fraction and rod cladding temperature distributions at the end of COBRA-TF simulation, 749 sec, are shown in **Figure 6-8**. COBRA-TF predicted fully quench of the entire rod bundle at this time with the cladding temperature slightly above the saturation temperature. While the bottom portion of the rod bundle remained single-phase liquid, two-phase flow dominated the rest of the bundle. In addition, due to constant heat addition into the fluid, the void fraction near the outlet of the test section was very close to unity. This indicated that a larger amount of vapor was generated through boiling and droplet evaporation.
Figure 6-8: Transient Variations of the Sub-Channel Void Fraction and Rod Cladding Temperature at 749 sec after Reflood, Exp. 8009

6.3.2 COBRA-TF Calculations for Quench Front Propagation

In order to have a detailed and quantitative investigation of the quench front propagation process, the quench front locations predicted by COBRA-TF are compared with the actual RBHT reflood experiments presented in Table 6-1. Tests are grouped together according to their various conditions. Figure 6-9 below shows the COBRA-TF simulation comparisons with RBHT tests at different system pressures. All these reflood tests were performed at the same test conditions except for operating pressure. The inlet liquid injection rate was 0.0254 m/sec (1 in/sec) with 22 K (40 °F) subcooling and 1.31 kW/m (0.4 kW/ft) peak power, while the pressure varied from 124.11 (18) to 413.69 (60) kPa (psia). Due to the current RBHT linear power profile with peak located at 2.74 m (108 in), COBRA-TF predicted top-down quench near the outlet of the test section for all the reflood tests, which was only observed for Exps. 8009 and 8023. Other than this, the overall trends predicted by COBRA-TF agreed relatively well with the experimental data. However, early quenching was predicted for Exps. 8021, 8041 and 8009.
Figure 6-9: Comparison of COBRA-TF Quench Front Predictions with RBHT Experiments for Different System Pressures

Next, simulations were performed based on the experimental tests at different inlet subcooling temperatures to examine the code prediction capabilities. Figure 6-10 presents the comparison between COBRA-TF simulations and RBHT tests. The reflood tests in this group were performed under system pressure of 275.79 kPa (40 psia) with 0.0254 m/sec (1 in/sec) liquid injection rate, and 1.31 kW/m (0.4 kW/ft) peak power. The only difference was the inlet liquid subcooling temperatures as listed on these plots. Very similar performance in quench front prediction can be observed for these tests as well. The overall profiles were close with each other. However, early quenching was again predicted for several tests, especially for higher rod bundle location where the power input is higher. It can be also observed that, as the liquid subcooling increased, the predictions became better and better.
Since the quench front behavior predicted by COBRA-TF had significant difference for liquid injection rate at different inlet liquid subcoolings, they were compared separately with one test group for low subcooling conditions and another test group for high subcooling conditions. Figure 6-11 first evaluates the COBRA-TF performance for various mass flow rates at low inlet liquid subcooling conditions. Large discrepancies were observed for these tests, especially for tests with higher liquid injection rates. The most difference occurred for test Exp. 7140 with 0.2023 m/sec (8 in/sec) flooding rate. COBRA-TF predicted the bundle quench at around 50 sec after reflood whereas the actual test did not quench until a much later time (around 200 sec). Model improvements are thus required for the quench front prediction under low subcooling conditions with high flooding rates.
Figure 6-11: Comparison of COBRA-TF Quench Front Predictions with RBHT Experiments for Different High Flooding Rates at Low Inlet Subcooling Conditions

Situations for the flooding rate effect under high subcooling conditions were much better predicted by COBRA-TF, as is shown in Figure 6-12. From Table 6-1, it is known that subcoolings for these tests were not exactly the same. However, they are all considered to be of high liquid subcooling tests. For these tests, COBRA-TF was able to reproduce the quench front propagation with slight early prediction of the quench, as expected from previous discussions. In addition, COBRA-TF was also able to correctly model the top-down quench phenomenon that actually took place in Exps. 7136, 7138 and 8032. Such comparison for liquid injection rate shows that quench models and possibly the mass and heat transfer models used for reflood are quite sensitive to the inlet liquid subcooling condition. This suggests the need to perform a thorough and comprehensive uncertainty and sensitivity analysis for COBRA-TF predictions against RBHT data.
The capability of COBRA-TF to predict the quench front for different rod bundle power inputs are further shown in Figure 6-13. Despite the top-down quench modeled for every test by the COBRA-TF flow and heat transfer regime selection algorithm, the general quench fronts matched with their experimental counterparts relatively well, with only slight early prediction.

It has been found that the predicted quench front fits the data well in early reflood stage. However, early quenching of the bundle was predicted for later times into reflood transients. The code is able to predict the quench front behavior with decent accuracy for various different test settings except for the reflood conditions with high flooding rate and low liquid inlet subcooling. Efforts need to be taken in the future to investigate and evaluate in detail the models used under reflood condition. For code verification and validation purposes, uncertainty and sensitivity analyses for various input parameters need to be performed.
6.3.3 COBRA-TF Calculations for Thermal-Hydraulic Responses during Reflood

Having made the overall quench front prediction comparison in the previous section, in this section, the rod bundle transient thermal-hydraulic response during reflood predicted by COBRA-TF is evaluated in greater detail by dividing the task into several parts including the actual rod, spacer grid and vapor temperature variations at specific locations interested. Also studied is the two-phase flow pressure drop prediction over the entire reflood transient for the selected rod bundle region (between two spacer girds). Such direct comparison with experimental data is expected to provide greater detailed information about the performance of COBRA-TF from different mass and heat transport aspects, based on which models can be validated and revised and new models can be developed.

The current comparisons for rod cladding temperature were made at three separate locations, i.e., at 2.02, 2.42 and 2.69 m, respectively. The spacer grid temperature investigated is focused on the spacer grid 6 that is located at the 2.79 m (110 in) along the rod bundle. There are 6 TC’s installed on the spacer grid 6 at different axial and radial locations. They were all plotted in the same figure for comparison. The axial location for vapor temperature evaluation was
selected at 2.54 m elevation. Cautions need to be taken for vapor temperature comparison owing to early wet of the steam probes during reflood as a result of entrained liquid droplets deposition. The DP cell used for pressure drop comparison is located at the bare bundle region from 2.54 to 2.74 m without spacer grid in between.

The rod bundle thermal-hydraulic responses for Exp. 7151 are shown in Figure 6-14. It is seen that the COBRA-TF predicted rod cladding temperature profiles at three locations for Exp. 7151 generally had similar trends as compared with the experimental measurements. The initial temperature raise was well predicted since it was mainly the single-vapor convective heat transfer. However, the rod temperatures for three locations in the DFFB regime were found to be under-estimated. The higher the temperature, the more significant under-estimation it did. The DFFB regime is known as the continuous vapor phase flow with liquid droplets entrained. The presence of these liquid droplets, which have wide distributions in size and velocity and serve as additional dispersed heat sinks, will significantly alter the mass and heat transport process within the rod bundle during reflood transients. In order to accurately predict the rod cladding temperature during the DFFB regime, advanced mass and heat transfer models are needed for the two-phase flow mixture, especially the models that are able to consider the liquid droplet size and velocity distributions, interfacial mass and heat transport, etc. In the COBRA-TF simulation, the liquid droplet phase is modeled as two groups (large droplet group and small droplet group) with each group having a uniform size. Also incorporated into the code are liquid droplet entrainment models (Holowach, 2002 [104]) and liquid droplet breakup model (Paik et. al. 1985 [34]) at the spacer grid location as well as the droplet de-entrainment models based on previous studies. Due to the lack of actual experimental data, relatively large uncertainties may exist for these models, leading to under-prediction of the current rod bundle temperature. On the other hand, comparison with the TC measurements for the spacer grid showed that COBRA-TF tended to over-predict the data, though the rod bundle temperature is under-predicted. Also, COBRA-TF predicted later
quench time for this test. The vapor phase temperature at 2.54 m (100 in) location is further compared. Large oscillations existed in the code prediction due to complex two-phase interactions. It can be observed that for Exp. 7151, the vapor temperature was slightly under-predicted before quenching. Large fluctuations also existed for the pressure drop prediction. While the pressure drop for the post-dryout regime was found to be relatively close to the RBHT experimental data, the pressure drop predicted by COBRA-TF after quenching had significant differences. The COBRA-TF uses a two-phase pressure drop model based on the work by Wallis [106], in which the two-phase flow multiplier is defined as a function of the void fraction. The single-phase friction coefficient is calculated based on the Blasius correlation.

Figure 6-14: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 7151

The rod bundle thermal-hydraulic responses for Exp. 8011 are next presented in Figure 6-15. Compared with the experimental data, it can be seen that the COBRA-TF under-predicted the rod cladding temperature at all locations and predicted early quenching. It has been found out that the $T_{\text{min}}$ and CHF are the two crucial parameters affecting the rod bundle quenching behavior.
While $T_{\text{min}}$ determines the minimum surface temperature below which liquid-wall direct contact is possible, the CHF determines the point of actual quenching. In COBRA-TF, the $T_{\text{min}}$ is calculated using either the correlation based on homogeneous nucleation temperature or the modified Berenson’s correlation by Henry [107], whichever value is larger. Under the forced convection boiling condition, calculation of the CHF is based on the correlations proposed by Biasi [108], which consists separate correlations for low- and high-quality flow, respectively. For Exp. 8011, the spacer grid temperature agreed relatively well except for the initial portion, where the temperature was over-predicted. The spacer grid quenching prediction was well within the experimental measurements. However, it can be observed that the vapor temperature was under-predicted. Last, as similar to Exp. 7151, the two-phase flow mixture pressure drop after quenching was under-predicted and subjected to large fluctuations whereas the results for the post-dryout regime were much better.

**Figure 6-16 to Figure 6-19** show the rod bundle thermal-hydraulic responses for other selected tests including: Exp. 7168 performed at relatively low rod bundle power input (0.97 kW/m (0.3 kW/ft)), Exp. 8013 performed at high power input (1.97 kW/m (0.6 kW/ft)), Exp. 8089 performed at low system pressure (124.11 (18 psia)) and Exp. 7138 performed at very high flow rate condition (0.2023 m/sec (8 in/sec)).

Based on the above comparisons, it was found that COBRA-TF tended to under-predict rod bundle temperature in the DFFB regime for low flooding rate test while over-predict the rod cladding temperature for high flooding rate test. Also, early quenching were observed when compared with all the experimental data. Despite of this difference, predictions of rod bundle early quenching was consistent. On the other hand, both under- and over-predictions of the spacer grid and vapor temperatures were observed for these tests. It should also be noted that COBRA-TF predicted dryout of spacer grid at a later time of reflood transient for Exp. 8089, which did not occurred in the actual test. Such code behavior could be due to the effect of low system pressure.
for this test. The spacer grid quenching front model is used to track the fraction of the grid surface that is wet. The wet region heat balance calculation determines whether the quench front grows or recedes. This is done by comparing the liquid film evaporation rate on the spacer grid due to radiative and convective heat transfer with the liquid droplet deposition rate. The quench front velocity is then calculated using the two-region analytical conduction solution by Yamanouchi [109]. Due to the early wet-out of the steam probe, no direct comparison for the vapor phase temperature can be made for Exp. 7138. COBRA-TF predictions for pressure drop were also found to be consistent with previous observations. That is, while slightly under-prediction existed for pressure drop in the post-dryout regime, large discrepancy occurred after the quenching. As a result, further improvement of the two-phase flow pressure drop model is needed to better fit the data.

Figure 6-15: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 8011
Figure 6-16: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 7168

Figure 6-17: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 8013
Figure 6-18: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 8089

Figure 6-19: Rod Bundle Transient Thermal-Hydraulic Response, Exp. 7138
6.3.4 COBRA-TF Calculations for Liquid Droplet Velocity

Due to the two-fluid three-field solution scheme adopted for the two-phase flow calculation, COBRA-TF is also capable of modeling the liquid droplet entrainment/dedetainment, liquid droplet breakup at spacer grid as well as the interfacial mass and heat transfer process. As mentioned previously, the liquid droplet entrainment model consists of two types of entrainment: entrainment from a liquid film and entrainment from a quench front. The detailed model description can be found in the work performed by Holowach (2002) [104]. The interfacial drag between the liquid droplet and vapor phase is calculated based on the droplet Reynolds number and the two-phase mixture viscosity as proposed by Ishii (1977) [3]. While the interfacial heat transfer coefficient is determined based on the work performed by Yuen and Chen (1978) [105], in which the vapor phase superheat is taken into account.

The COBRA-TF predicted liquid droplet velocity for various reflood tests were compared and evaluated. In order to have a direct comparison with the experimental measurement based on the laser imaging system, the COBRA-TF results have been averaged every 20 sec.

Figure 6-20 first shows the numerical simulation results for reflood tests at various system pressures. It is seen that for low system pressure test, the code predictions agreed with the experimental measurement well. However, for high system pressure test, under-predictions were identified. Nevertheless, COBRA-TF was able to correctly capture the decreasing trend in the droplet velocity as the quench front propagating upward along the bundle.

The droplet velocity calculation results for high flooding rate tests are shown in Figure 6-21. The significant increase in the two-phase mass flow rate further complicated the liquid droplet measurement, leading to less data points obtained. Both under- and over-predictions of the liquid droplet velocity were observed for the code predictions. An uncertainty and sensitivity analysis for liquid droplet models would be helpful in quantifying these prediction errors.
Figure 6-20: Transient Variation of the Liquid Droplet Velocity under Different System Pressures

Figure 6-21: Transient Variation of the Liquid Droplet Velocity under Different Liquid Injection Rates
Last, the droplet velocity results are shown for tests with different rod bundle power inputs. It appears that the low power tests may under-estimate the droplet velocity while the high power tests tended to have a good agreement. However, more experimental data is needed to further confirm the current observation.

![Figure 6-22: Transient Variation of the Liquid Droplet Velocity under Different Rod Bundle Power Inputs](image)

**6.3.5 COBRA-TF Calculations for Two-Phase Flow Void Fraction**

Void fraction is a very important two-phase flow quantity characterizing the two-phase flow mixture composition, interfacial geometry and heat transport. Therefore, accurate prediction of the void fraction is of crucial importance in the reactor thermal-hydraulic and safety analysis. In COBRA-TF, the void fraction is calculated along solving the conservation equations for each phase and it is used to distinguish different flow and heat transfer regimes. In the current study,
A two-phase flow mass quality correlation for the DFFB regime has been developed based on the RBHT data in Section 5.3, which can be used to evaluate the DFFB void fraction estimations based on COBRA-TF runs. Due to significant fluctuations in the code predicted void fraction data, a moving average of 5 sec was performed both for the experimental data and for the numerical results.

**Figure 6-23** shows the COBRA-TF predictions for the two-phase flow void fraction variations at different system pressures. It can be seen that initially the void fraction was very close to unity since single-phase convective cooling by the vapor flow was the dominated heat transfer mechanism. As the liquid droplets gradually being entrained in the vapor flow, the void fraction started to decrease. However, the values predicted were above 0.9, indicating that the flow regime was the liquid droplet dispersed flow. By comparing with the corresponding RBHT reflood tests it can be observed that COBRA-TF tended to under-predict the experimental void fraction evaluated by Eqs. (5.15) and (5.46), indicating that COBRA-TF predicted more liquid presence within the vapor flow. With more heat sinks distributed in the vapor flow, the interfacial heat transfer is enhanced, leading to lowered vapor temperature and eventually the lowered rod bundle temperature in the DFFB regime. This confirms the observations made in Section 6.3.3 for the rod bundle surface temperature, for which COBRA-TF always under-predicts the rod surface temperature in the post-dryout regime. Even through the current data has been averaged every 5 sec, large fluctuations still existed for the code predictions, especially for the low system pressure tests.
Figure 6-23: Comparison of Void Fraction in the DFFB Regime for RBHT Tests at Various System Pressures

The void fraction predictions are further compared with experimental data preformed at different inlet liquid subcoolings. The results are shown in Figure 6-24. Despite of the general under-estimation of the data, COBRA-TF appeared to perform better for low subcooling tests than for high subcooling tests. The void fraction comparisons for different liquid injection rates and for different rod bundle power input are shown in Figure 6-25 and Figure 6-26, respectively. For high flooding rate and relatively low peak power tests, the under-prediction was more significant.
Figure 6-24: Comparison of Void Fraction in the DFFB Regime for RBHT Tests at Various Inlet Liquid Subcooling Temperatures

Figure 6-25: Comparison of Void Fraction in the DFFB Regime for RBHT Tests at Various Liquid Injection Rates
Figure 6-26: Comparison of Void Fraction in the DFFB Regime for RBHT Tests at Various Rod Bundle Power Inputs

6.3.6 Verification and Validation for COBRA-TF Calculations

With the data gathered for all the RBHT tests selected, it is very useful and informative to evaluate the overall performance and uncertainties related to variables important to the two-phase mass and heat transfer analysis. This section will investigate in detail the uncertainties involved in the code predictions for various flow and heat transfer quantities that include: the rod bundle surface temperature, spacer grid temperature, vapor phase temperature, pressure drop for bare bundle, liquid droplet velocity and the void fraction within the DFFB regime. Appropriate averages were performed in order to have a one-on-one comparison with the RBHT experimental data as well as to damp the fluctuations encountered.
The COBRA-TF overall performance for rod bundle surface temperature prediction is shown in Figure 6-27. As can be seen, COBRA-TF was able to predict the rod temperature for the post-dryout regime well within a ±15% error range with only moderate under-predictions. However, early prediction of the quenching time contributed to the maximum uncertainty, this is due to extremely complex two-phase mass and heat transport phenomenon that exists near the quench front regime, which significantly increased the modeling difficulty in numerical analysis codes. After quenching, COBRA-TF was able to accurately predict the rod temperature near saturation.

On the other hand, there was a ±20% error existing for the spacer grid temperature calculation, as is shown in Figure 6-28. For most of the time in the post-CHF heat transfer regime, the spacer grid temperature was over-predicted. In addition, COBRA-TF predicted both early and later quenching time for spacer grid under different test conditions.

Both the RBHT measurements and COBRA-TF calculations on the vapor phase temperature variations have been averaged every 5 sec in order to eliminate fluctuations before comparison. It can be seen from Figure 6-29 that the code was able to predict the vapor phase temperature within the ±20% error span. Similar to rod and spacer grid temperature predictions, the largest uncertainty was found to occur during the quenching period.
Figure 6-27:  COBRA-TF Uncertainty Related to the Rod Surface Temperature Prediction

Figure 6-28:  COBRA-TF Uncertainty Related to the Spacer Grid Temperature Prediction
The largest uncertainty for the COBRA-TF prediction, however, is found to be the pressure drop prediction. Figure 6-30 presents the uncertainty associated with pressure drop prediction after an average of 5 sec for both pressure drop data. Uncertainties more than ±50% in pressure drop existed for the bulk liquid boiling regime. While the pressure drop for the post-CHF regime were relatively good, under-predictions were observed for the pressure drop calculation after quench. Needs for significant model improvement are thus identified in order to better modeling the two-phase flow pressure drop.

Based on the liquid droplet velocity data obtained through the laser imaging system, COBRA-TF was found to be able to predict the droplet velocity with ±30% error span. Figure 6-31 Shows the comparison results. More advanced liquid droplet models considering the droplet size and velocity distributions may further improve the accuracy of the current code prediction.
Last, the two-phase flow void fraction prediction for the DFFB regime is evaluated based
of the model developed in the current study and the results are shown in Figure 6-32. Even
though the void fraction value was very close to unity for the DFFB regime, a clear trend of under-prediction can be observed. Such under-prediction will lead to more liquid presence in the vapor flow which will further lead to the under-prediction of the rod bundle temperature.

Figure 6-32: COBRA-TF Uncertainty Related to the DFFB Regime Void Fraction Prediction
Chapter 7

Conclusions and Future Work

7.1 Conclusions

As one of the most important challenges faced by the nuclear power industry, the cooling capability of the reactor core remains a major concern, especially under accident scenarios. In order to have a better understanding on the reflood transients and to provide reliable experimental data for the two-phase flow mass and heat transfer model development, in the current study, a series of constant reflood tests were performed at the NRC/PSU RBHT test facility. The tests cover a wide range of conditions that could be encountered during hypothetical accident scenarios for a LWR, accounting for various parameters that include the system pressure, inlet liquid flooding rate, inlet liquid subcooling, initial peak cladding temperature, and the rod bundle power input. In the current experimental work, transient variations of key parameters related to the two-phase flow thermal-hydraulics, such as the mass flow rate, fluid temperature, droplet behavior, as well as the rod bundle response, including the cladding temperature, and quench front propagation along the bundle, are measured using various measuring techniques. Based on these experimental results, further data reductions and analyses have been performed to specifically study the two-phase flow characteristics (parametric effects on the quench front propagation and CLL variation, rod bundle and fluid temperature variations, rod bundle pressure drop, thermal-hydraulic non-equilibrium, liquid droplet size and velocity distribution) and spacer grid effects (spacer grid pressure drop, liquid droplet breakup on spacer grid) in the DFFB regime.

In the theoretical part of the current study, based on the RBHT experimental data, theoretical models were developed describing the two-phase flow interactions as well as the rod
bundle heat transfer processes, which provide clear physical explanation for the various phenomena observed in the experiments. Specifically, a force balance analysis was performed on the liquid droplet phase in the DFFB regime to determine the relative motion between the vapor and the liquid droplet, adequately accounting for the effect of hydraulic non-equilibrium. The effects of the interfacial heat transfer/phase change and the interfacial drag have also been captured in the modeling process. In addition, a two-phase flow thermal-hydraulic non-equilibrium study has been successfully conducted during reflood transients. The latter is of crucial importance in determining the two-phase flow pressure drop and the peak cladding temperature of the system. Useful correlations have been developed including: 1). the local mass quality correlation and the vapor phase superficial velocity correlation; 2). droplet breakup correlation, and; 3) the spacer grid pressure drop correlation. All these models have shown improved performance in predicting the two-phase flow behavior when compared with the RBHT data, since they were developed based on the fundamental principles and mechanisms that are important to the reflood transients under consideration.

In the numerical part of the current work, numerical simulations have been performed for reflood transients using the sub-channel analysis code COBRA-TF, which is a thermal-hydraulic computational code for predicting the thermal-hydraulic behavior of water-cooled nuclear reactors. COBRA-TF uses a two-fluid three-field (liquid, vapor and droplet) solution scheme to solve the conservation equations of mass, momentum and energy for each field. In the current study, the COBRA-TF analysis model has been developed for the RBHT reflood tests covering wide ranges of parameters. Detailed comparisons have been made between the numerical predictions and the experimental data such that the two-phase flow and heat transfer models incorporated in COBRA-TF can be validated, especially for the mass and heat transfer process in the DFFB regime. The code overall prediction uncertainties for important two-phase flow and heat transfer quantities were obtained. Based on the comparison, modifications and improvements
to the existing models as well as developments of new analysis models could be proposed for the
two-phase flow and rod bundle heat transfer analysis.

The major findings and conclusions of the current work are summarized below:

1). Based on the unique and valuable data obtained from NRC/PSU RBHT reflood tests, it was found that the system pressure, flooding rate and rod bundle power input strongly affect the thermal-hydraulic behavior of the two-phase flow and rod bundle heat transfer. The inlet liquid subcooling, on the other hand, was found to have secondary effect on the mass and heat transport, especially in the DFFB regime.

2). The entrained liquid droplets in the continuous vapor flow were found to have a log-normal distribution in size. As a liquid droplet size becomes larger, its corresponding velocity decreases. While it is relatively difficult to distinguish the most dominating system effects on the liquid droplet size variation other than the quench front location relative to the point of measurement, the droplet velocity was found to be dependent on the flooding rate and system pressure.

3). The spacer grid was found to play a very important role in altering the two-phase flow mass and heat transfer behavior. It increased the two-phase flow pressure drop, caused the droplet breakup, deposition and re-entrainment, and enhanced the heat transfer process downstream due to increased turbulent mixing. For the dry spacer grid, the liquid droplet size was found to be much smaller than that of the upstream. In addition, the most dominating system parametric effect on the spacer grid induced pressure drop was identified to be the mass flow rate.

4). During reflood transients, the two phases were always found to be in significant thermal-hydraulic non-equilibrium. Their extent, as characterized by the vapor phase superheat and the two-phase slip ratio, were investigated quantitatively. The thermal non-equilibrium was found to increase as the elevation above the quench front went higher. On the other hand, it was observed that the two-phase slip ratio is not only a function of the distance from the quench front
location where most droplets get entrained but also a function of the liquid droplet size. Moreover, if there is thermal non-equilibrium within the two-phase flow, then the hydraulic non-equilibrium is likely to exist.

5). The several models developed in the current study are able to predict the two-phase flow mixture thermal-hydraulic behavior with significantly improved accuracy compared with correlations developed by previous researchers. This is mainly because the development of these models started from the fundamental principles and mechanisms involved in the two-phase flow mass and heat transfer processes and various factors that are important to the current problem were adequately captured. The proposed two-phase flow mass quality correlation was able to predict the data from the current RBHT tests and previous tests well within ±10% error. Model developed for the dry spacer grid liquid droplet breakup had an error within ±17%, whereas the spacer grid pressure drop model developed for the DFFB regime had an error span of ±25%.

6). The COBRA-TF numerical simulations and their detailed comparisons with the RBHT experiments provide abundant and very informative information for the code performance, based on which a verification and validation study was carried out in order to quantify the prediction uncertainties involved in reflood transients. It has been found that over the reflood conditions explored in the current study, the COBRA-TF was able to predict the rod cladding temperature within ±15% error, the spacer grid and vapor phase temperatures within ±20% error and the droplet velocity within ±30% error span. However, it was also found that COBRA-TF tended to predict early quench of the entire bundle, while under-predicting the DFFB regime void fraction variation. Most specifically, large uncertainty existed for two-phase flow pressure drop calculation.

The various results and major findings obtained in this work are significant and informative in the sense that new two-phase flow phenomena and behaviors were either discovered for the first time or investigated extensively due to abundant RBHT reflood data that
made this possible. The valuable and unique data obtained from the NRC/PSU RBHT test facility, either filled the void or greatly increased the current experimental data base for reflood transients (especially for the DFFB regime with liquid droplets present), may serve as benchmark tests that will significantly improve the performance of the numerical simulation tools. Also, the theoretical works performed in the current study provided a detailed characterization of the two-phase flow behavior by relating the basic and local flow and heat transfer quantities to variables that are of importance in two-phase flow thermal-hydraulic analysis, thus broadening our way of understanding the two-phase flow and providing more choices when tackling the problem at hand. Last, the extensive code verification and validation performed for the COBRA-TF simulations provided the basis and direction for further model improvement and development.

7.2 Future Work

To complete the much-needed reflood data base, it is strongly recommended that the current NRC/PSU RBHT reflood experiments, data reduction, and data evaluation be continued for not only the constant reflood tests but also the variable reflood tests and oscillatory reflood tests. Meanwhile, the current research focus should be further expanded by including other important mass and heat transfer regimes (such as transition boiling regime, inverted annular flow boiling regime, subcooled liquid boiling regime) as well as other important thermal-hydraulic phenomena (such as the CHF point, the $T_{\text{min}}$ point (Leidenfrost point), the 2D heat conduction within rod cladding and the effect of spacer grid under dry/wet conditions). In addition, the current experimental data, while covering a wide range of reflood conditions and being extremely useful, is limited by the system operating pressure and thus corresponds to only low-pressure reflood conditions. The two-phase flow behavior as well as the rod bundle thermal-hydraulic response under high-pressure conditions are expected to be quite different from the low-pressure
conditions. Therefore, in order to overcome this shortcoming and to complete investigations on the reflood transients over the entire pressure range, a high-pressure test facility needs to be developed to obtain reflood data under high-pressure conditions. The proposed future work would eventually provide a much more comprehensive and inclusive experimental data bank for the reflood transients, based on which advanced models could be developed and the numerical simulation tools could be further verified and validated accordingly.

Theoretically, in order to seek a better understanding of the reflood transients, the two-phase flow behavior at the quench front location need to be studied in detail, including the quench front propagation, the dryout formation, and the post-dryout heat transfer including the liquid film mass and heat transfer as well as the liquid droplet entrainment. Efforts also need to be taken to improve the interfacial mass and heat transport process by taking the liquid droplet size and velocity distributions into consideration. On the other hand, accurate models for $T_{\text{min}}$ determination under flow boiling conditions at moderate to high pressure are another major challenge for accurate prediction of quenching behavior.

Numerically, COBRA-TF is a best-estimate code as developed to perform analyses for normal operations, transients and accidents for LWRs. Therefore, a thorough uncertainty and sensitivity analysis for various COBRA-TF input parameters, models and assumptions need to be completed to verify the code capability, only after which, improvements could be made to existing models while new models could be implemented and tested in COBRA-TF to further enhance its capability.
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### Appendix A

**RBHT Bundle II Test Matrices**

Table A-1: Summary of RBHT Reflood Test Conditions for TO2 (Camera across SG 6)

<table>
<thead>
<tr>
<th>Exp #</th>
<th>Type of Test</th>
<th>Flooding Rate m/s (in/s)</th>
<th>Pressure kPa (psia)</th>
<th>Initial PCT K (°F)</th>
<th>Subcooling K (°F)</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>7056</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>9.4 (17)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7063</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>10 (18)</td>
<td>1.31 (0.4)</td>
<td>Trip after 279 sec</td>
</tr>
<tr>
<td>7090</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>10 (18)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7095</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>80 (150)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7102</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td></td>
</tr>
<tr>
<td>7106</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td>Trip after 147 sec</td>
</tr>
<tr>
<td>7112</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td></td>
</tr>
<tr>
<td>7116</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td></td>
</tr>
<tr>
<td>7121</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td></td>
</tr>
<tr>
<td>7123</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td>275.8 (40)</td>
<td>1033 (1400)</td>
<td>50 (90)</td>
<td>0.97 (0.3)</td>
<td></td>
</tr>
<tr>
<td>7126</td>
<td>Variable Reflood</td>
<td>0.0508 (2) (0&lt;t&lt;30), 0.0254 (1) (t&gt;30)</td>
<td>1033 (1400)</td>
<td>1033 (1400)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>Lost power 3 times</td>
</tr>
<tr>
<td>7128</td>
<td>Variable Reflood</td>
<td>0.0508 (2) (0&lt;t&lt;40), 0.0254 (1) (t&gt;40)</td>
<td>1033 (1400)</td>
<td>1033 (1400)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>Repeat of 7126</td>
</tr>
<tr>
<td>7132</td>
<td>Variable Reflood</td>
<td>0.0762 (3) (0&lt;t&lt;20), 0.0254 (1) (t&gt;20)</td>
<td>1144 (1600)</td>
<td>1144 (1600)</td>
<td>75 (135)</td>
<td>2.30 (0.7)</td>
<td>Scram after 129 sec</td>
</tr>
<tr>
<td>7134</td>
<td>Reflood</td>
<td>0.0762 (3) (0&lt;t&lt;20), 0.0254 (1) (t&gt;20)</td>
<td>1144 (1600)</td>
<td>1144 (1600)</td>
<td>75 (135)</td>
<td>2.30 (0.7)</td>
<td>Scram after 205 sec</td>
</tr>
<tr>
<td>7136</td>
<td>Reflood</td>
<td>0.1524 (6)</td>
<td></td>
<td>1144 (1600)</td>
<td>84 (151)</td>
<td>2.30 (0.7)</td>
<td></td>
</tr>
<tr>
<td>7137</td>
<td>Reflood</td>
<td>0.1524 (6)</td>
<td></td>
<td>1144 (1600)</td>
<td>78 (140)</td>
<td>1.97 (0.6)</td>
<td></td>
</tr>
<tr>
<td>7138</td>
<td>Reflood</td>
<td>0.2032 (8)</td>
<td></td>
<td>1144 (1600)</td>
<td>73 (132)</td>
<td>2.30 (0.7)</td>
<td></td>
</tr>
<tr>
<td>7140</td>
<td>Reflood</td>
<td>0.2032 (8)</td>
<td></td>
<td>1033 (1400)</td>
<td>5 (9)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7143</td>
<td>Variable Reflood</td>
<td>0.1524 (6) (0&lt;t&lt;50), 0.0254 (1) (t&gt;50)</td>
<td>1144 (1600)</td>
<td>1144 (1600)</td>
<td>80 (150)</td>
<td>2.30 (0.7)</td>
<td>Pump didn’t step down correctly</td>
</tr>
<tr>
<td>7149</td>
<td>Variable Reflood</td>
<td>0.0762 (3) (0&lt;t&lt;100), 0.0254 (1) (t&gt;100)</td>
<td>1144 (1600)</td>
<td>1144 (1600)</td>
<td>76 (136)</td>
<td>2.30 (0.7)</td>
<td>Revisited from 7132/7134</td>
</tr>
<tr>
<td>7151</td>
<td>Reflood</td>
<td>0.0191 (0.75)</td>
<td></td>
<td>1033 (1400)</td>
<td>53 (96)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7154</td>
<td>Variable Reflood</td>
<td>0.1524 (6) (0&lt;t&lt;50), 0.0254 (1) (t&gt;50)</td>
<td>1144 (1600)</td>
<td>1144 (1600)</td>
<td>80 (150)</td>
<td>2.30 (0.7)</td>
<td>Repeat of 7106</td>
</tr>
<tr>
<td>7157</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
<tr>
<td>7159</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>5 (9)</td>
<td>1.31 (0.4)</td>
<td></td>
</tr>
</tbody>
</table>
Table A-2: Summary of RBHT Reflood Test Conditions for TO3 (Camera between SG 5 and 6)

<table>
<thead>
<tr>
<th>Exp #</th>
<th>Type of Test</th>
<th>Flow rate m/s (in/s)</th>
<th>Pressure kPa (psia)</th>
<th>Initial PCT K (°F)</th>
<th>Subcooling K (°F)</th>
<th>Peak Power kW/m (kW/ft)</th>
<th>Camera Location</th>
</tr>
</thead>
<tbody>
<tr>
<td>7166</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>80 (150)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7168</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>51 (91)</td>
<td>0.97 (0.3)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7173</td>
<td>Reflood</td>
<td>0.0254 (1)</td>
<td></td>
<td>1033 (1400)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7174</td>
<td></td>
<td>0.0191 (0.75)</td>
<td>275.8 (40)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>7175</td>
<td>Variable Reflood</td>
<td>0.0762 (3) (0 &lt; t &lt; 100), 0.0254 (1) (t &gt; 100)</td>
<td>1144 (1600)</td>
<td>76 (136)</td>
<td>2.30 (0.7)</td>
<td></td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7177</td>
<td>Variable Reflood</td>
<td>0.0508 (2) (0 &lt; t &lt; 50), 0.0191 (0.75) (t &gt; 50)</td>
<td>1033 (1400)</td>
<td>11 (20)</td>
<td>1.31 (0.4)</td>
<td></td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7179</td>
<td>Variable Reflood</td>
<td>0.1524 (6) (0 &lt; t &lt; 50), 0.0254 (1) (t &gt; 50)</td>
<td>1144 (1600)</td>
<td>80 (150)</td>
<td>2.30 (0.7)</td>
<td></td>
<td>Between 5/6</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Nominal Amp. +/- Per. (s)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>7184</td>
<td>Oscillatory Reflood</td>
<td>0.0254 (1) 0.0254 (1)</td>
<td>4</td>
<td>1033 (1400)</td>
<td>57 (102)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7185</td>
<td>Oscillatory Reflood</td>
<td>0.0254 (1) 0.0254 (1)</td>
<td>4</td>
<td>1033 (1400)</td>
<td>20 (36)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7177</td>
<td>Oscillatory Reflood</td>
<td>0.0254 (1) 0.0254 (1)</td>
<td>2</td>
<td>1033 (1400)</td>
<td>20 (36)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>7178</td>
<td>Oscillatory Reflood</td>
<td>0.0254 (1) 0.0254 (1)</td>
<td>2</td>
<td>1033 (1400)</td>
<td>20 (36)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
</tr>
<tr>
<td>8000</td>
<td>Level Swell</td>
<td>(1.6, 1.4, 1.2, 1.0, 0.9, 0.8, 0.7, 0.6, 0.5, 0.4)</td>
<td>275.8 (40)</td>
<td>-</td>
<td>56 (100)</td>
<td>1.31 (0.4)</td>
<td>-</td>
</tr>
</tbody>
</table>
Table A-3: Summary of RBHT Reflood Test Conditions for TO4

<table>
<thead>
<tr>
<th>Test Type</th>
<th>Pressure, kPa (psi)</th>
<th>Injection Flow Rate, mm/s (in/s)</th>
<th>∆Tsub, K (°F)</th>
<th>Peak Temp., K (°F)</th>
<th>Qmax, kW/m (kW/ft)</th>
<th>Camera Location (Relative to SG6)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Oscillatory Reflood</td>
<td></td>
<td>Nominal</td>
<td>Amp. +/-</td>
<td>Per. (s)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>(12) 137.9 (20)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>10</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>206.8 (30)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>10</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>275.8 (40)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>10</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>413.7 (60)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>10</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>Variable Reflood</td>
<td></td>
<td>50.8 (2)</td>
<td>(t &lt; 30 sec),</td>
<td>11.1 (20)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>(2) 137.9 (20)</td>
<td>76.2 (3)</td>
<td>25.4 (1)</td>
<td>(t &gt; 30 sec),</td>
<td>75.55 (136)</td>
<td>1144 (1600)</td>
<td>2.30 (0.7)</td>
</tr>
<tr>
<td>275.8 (40)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>4</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>413.7 (60)</td>
<td>25.4 (1)</td>
<td>25.4 (1)</td>
<td>4</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
</tr>
<tr>
<td>Constant Reflood</td>
<td></td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Across</td>
</tr>
<tr>
<td>(11) 137.9 (20)</td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
<td></td>
</tr>
<tr>
<td>206.8 (30)</td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
<td></td>
</tr>
<tr>
<td>275.8 (40)</td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
<td></td>
</tr>
<tr>
<td>413.7 (60)</td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Between 5/6</td>
<td></td>
</tr>
<tr>
<td>203.2 (8)</td>
<td>83.3 (150)</td>
<td>1144 (1600)</td>
<td>2.30 (0.7)</td>
<td>2.30 (0.7)</td>
<td>Across</td>
<td></td>
</tr>
<tr>
<td>203.2 (8)</td>
<td>83.3 (150)</td>
<td>1144 (1600)</td>
<td>2.30 (0.7)</td>
<td>2.30 (0.7)</td>
<td>Across</td>
<td></td>
</tr>
<tr>
<td>413.7 (60)</td>
<td>25.4 (1)</td>
<td>22.2 (40)</td>
<td>1033 (1400)</td>
<td>1.31 (0.4)</td>
<td>Across</td>
<td></td>
</tr>
</tbody>
</table>
Table A-4: Summary of RBHT Reflood Test Conditions for Additional Tests

<table>
<thead>
<tr>
<th>Exp. Type</th>
<th>Pressure kPa (psia)</th>
<th>Mass Flow Rate m/s (in/s)</th>
<th>$\Delta T_{sub}$ K (°F)</th>
<th>Peak Temp. K (°F)</th>
<th>$Q_{max}$ kW/m (kW/ft)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Nominal Amp. +/- Per. (s)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1: O-A</td>
<td>Oscillatory Reflood</td>
<td>275 (40)</td>
<td>0.0254 (1) 0.0508 (2)</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>2: O-B</td>
<td>Oscillatory Reflood</td>
<td>275 (40)</td>
<td>0.1016 (4) 0.1016 (4)</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>3: O-C</td>
<td>Oscillatory Reflood</td>
<td>275 (40)</td>
<td>0.0508 (2) 0.0762 (3)</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>4: O-D</td>
<td>Oscillatory Reflood</td>
<td>275 (40)</td>
<td>0.0762 (3) 0.0254 (1)</td>
<td>76 (136)</td>
<td>1144 (1600)</td>
</tr>
<tr>
<td>5: C-PP</td>
<td>Constant Reflood</td>
<td>275 (40)</td>
<td>0.0254 (1)</td>
<td>53 (96)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>6: C-P</td>
<td>Constant Reflood</td>
<td>124 (18)</td>
<td>0.0254 (1)</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>7: S-F1</td>
<td>Step-Down Reflood</td>
<td>275 (40)</td>
<td>0.0762 (3), 0-15 s 0.0508 (2), 15-30 s 0.0254 (1), 30-45 s 0.0127 (0.5), &gt; 45 s</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>8: S-F2</td>
<td>Step-Down Reflood</td>
<td>275 (40)</td>
<td>0.0762 (3), 0-15 s 0.0508 (2), 15-30 s 0.0254 (1), 30-45 s 0.01321 (0.52), 45-500 s 0.01219 (0.48), &gt; 500 s</td>
<td>22 (40)</td>
<td>1033 (1400)</td>
</tr>
<tr>
<td>9: S-F3</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>10: S-F4</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
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</tbody>
</table>
Appendix B

Mathematical Models Used in DATARH for Cladding Temperature and Quench Front Tracking

B.1 Cladding Temperature Determination

DATARH program calculates the surface temperature, the surface heat flux and the transient heat transfer coefficient for heater rods by solving the “Inverse Heat Conduction Problem” (IHCP). The IHCP is basically the determination of the surface temperature, therefore the surface heat flux can be determined from the transient temperatures measured at an interior location of a heated structure. In the problem solved by DATARH, the initial temperature distribution is known. Detailed discussion of solution scheme for DATARH can be found in the report NUREG/CR-7152 (Section 3.1.1)

To solve the inverse heat conduction at hand, the finite difference approximation in implicit form of a one-dimensional heat conduction equation in cylindrical coordinates is used. A second order difference method is used for spatial discretization along with first order time-marching method. The finite difference approximation in implicit form is given as:

\[ B(n)T(n - 1) + A(n)T(n) + C(n)T(n + 1) = -T'(n) - Q(n); 1 \leq n \leq N \] (B.1)

where,

\[ B(n) = \frac{\Delta \theta}{(\Delta r)^2} \frac{r(n+\frac{1}{2})}{r(n)} \frac{k(n+\frac{1}{2})}{\rho(n)c(n)} \] (B.2)

\[ C(n) = \frac{\Delta \theta}{(\Delta r)^2} \frac{r(n+\frac{1}{2})}{r(n)} \frac{k(n+\frac{1}{2})}{\rho(n)c(n)} \] (B.3)

\[ A(n) = -B(n) - C(n) - 1 \] (B.4)
\[ Q(n) = \frac{q(n)\Delta\theta}{\rho(n)c(n)} \]  \hspace{1cm} (B.5)

where \( T(n) \) is the unknown temperature at node point \( n \). \( T'(n) \) is the known temperature at node point \( n \). This is also the "initial value". \( r \) is the spatial variable. \( \Delta r \) is the spatial increment. \( \Delta\theta \) is the time increment. Here, \( k, \rho, c \) are the thermal conductivity, density and specific heat, respectively. Their numerical values are computed at \( T' \). If values at half intervals are required, they are evaluated at the average of two adjacent temperatures. \( q(n) \) is the volumetric heat generation rate. \( N \) is the total number of internals. Hence, there are \( N+1 \) node points.

For \( n = 1 \), the node point is at the centerline of the rod, \( T(n-1)=T(n+1) \), since at this point the symmetry requires the flux be zero. At the point \( n = M \), \( T(M) \) is the known measured temperature and finally, at the outer boundary point, \( n = N \), \( T(n+1) \) is the desired wall temperature.

Apply Equation (B.1) to node points \( n = 1,2,\ldots,N \) and, combining the boundary condition \( T(0) = T(2) \) with the finite difference equation at \( n = 1 \) to eliminate \( T(0) \), we have a set of \( N \) linear simultaneous equation with \( N \) unknown temperatures, \( T(n); n = 1,\ldots,N+1, n\neq M \). The solution of this set of equations define the temperature field at a given time step, \( \theta \). The transient solution can thus be obtained by repeating the same procedure with successive increment of \( \Delta\theta \).

Because of the implicit finite difference scheme, the stability of the computation is guaranteed. As long as reasonable increment sizes, \( \Delta r \) and \( \Delta\theta \), are chosen, acceptable numerical accuracy can be expected.

It is noticed that if \( M = N+1 \), the inverse heat conduction problem is reduced to a more conventional type with a flux condition specified on one boundary (centerline) and a temperature condition specified on the other (wall). In this case, the coefficient matrix for the linear equations, as defined by Equation (B.1), is tri-diagonal. However, the presence of a known temperature, \( T(M), M \neq N+1 \), in the linear set to replace the wall temperature, \( T(N+1) \), destroys the tri-
diagonality and consequently complicates the solution procedure since for linear equations with a
tri-diagonal coefficient matrix, the solution can be obtained by a time saving matrix resolution
 technique which is not applicable to any other form of matrix.

Let us, for the time being, ignore the fact that $T(M), M \neq N+1$, is known and assume that
$T(N+1)$ is known instead. The linear equation, written in the usual manner is:

$$[J][\bar{T}] = \bar{F} - C(N)T(N + 1)\bar{G}$$  \hspace{1cm} (B.6)

where,

$[J]$ is the tri-diagonal coefficient matrix defined by Equations (B.1) through (B.4);

$\bar{T}$ is the solution vector with $N$ components;

$\bar{F}$ is the "source" vector with the components defined by the quantities on the right-hand side of
Equation (3-1);

$\bar{G}$ is a vector with first $N-1$ components equal to zero and the $N$th component equal to one, i.e.
$q(i)=0, i\neq N, q(i)=1, i=N$

If $\bar{X}$ and $\bar{Y}$ are, respectively, solutions of

$$[J][\bar{X}] = \bar{F}$$  \hspace{1cm} (B.7)

$$[J][\bar{Y}] = -\bar{G}$$  \hspace{1cm} (B.8)

The linearity of Equation (B.7) leads to:

$$\bar{T} = \bar{X} + C(N)T(N + 1)\bar{Y}$$  \hspace{1cm} (B.9)

Equation (B.10), in scalar form for $n = M$, gives

$$T(M) = X(M) + C(N)T(N + 1)Y(M)$$  \hspace{1cm} (B.10)

or,

$$T(N + 1) = \frac{T(M) - X(M)}{C(N)Y(M)}, \ Y(M) \neq 0$$  \hspace{1cm} (B.11)
Since all the quantities on the right hand side of Equation (B.11) are known, the wall temperature \( T(N+1) \), can be computed. The remaining temperature field can be obtained by repeated application of Equation (B.10).

If the transient temperature measurement \( T(M) \) is both accurate and frequent enough, the method outlined above will produce acceptable results. In practice, however, such accuracy and frequency as demanded by the numerical method are almost impossible to achieve. Any error in \( T(M) \), either from instrumentation or interpretation, would be amplified in the numerical process. An error is usually propagative and oscillatory and results from a successive over and under correction of the heat balance as demanded by the governing heat conduction equation. A small error in \( T(M) \) often renders the calculations for wall temperature and wall flux useless since their errors are amplified as the calculation steps through time.

Therefore, it is desirable to devise a numerical method such that the input error of \( T(M) \) could be damped during the subsequent computation steps. One such method is programmed in DATARH. Damping the input error can only improve the accuracy of the computed results by reducing the input error amplification; the inherent error due to inaccurate input data still remains. The basic principle of the method is discussed in detail in NUREG/CR-7152 report.

**B.2 Quench Front Location Determination**

Included in the DATARH source code is a method of calculating the quench front progress for the RBHT reflood experiments. Using the results from this code, the quench front propagation curves could be determined. The method used for the RBHT reflood tests are similar to that of the FLECHT-SEASET reflood test (NUREG/CR-1532, Section E-12 Quench Program).

In the program used for FLECHT-SEASET tests, it advances sequentially through all the data for each thermocouple channel, looking at five points at a time [\( T(t) \) at \( I \) through \( I + 4 \),...
Figure B-1(a)]. The first criterion applied is that the temperature, \( T(t) \), must be greater than 149 °C (300 °F) to qualify as a potential quench condition. If it is not, the remaining criteria are skipped. The second criterion checks whether the slope of the temperature-time curve between the third and fourth points is greater than 28 °C/s (50 °F/s), that is, whether

\[
\frac{T_{i+3} - T_{i+2}}{\Delta t} < -50^\circ F/s
\]  

(B.12)

The decision whether a quench exists or not is made on this basis. The third criterion checks whether the absolute value of the slope between the third and fourth points is two times greater than the absolute value of the slope between the first and second points, that is, whether \( S_2 > 2S_1 \). If so, a quench condition exists. If not, the program skips out of the search and advances to the next set of data points. Finally, the program checks the absolute value of the slope between the fourth and fifth data points \( (S'_2) \) and compares it to the absolute value of the slope between the third and fourth points \( (S_2) \). If \( S'_2 > S_2 \), as shown in Eq. (B.13),

\[
\left| \frac{T_{i+4} - T_{i+3}}{\Delta t} \right| - \left| \frac{T_{i+3} - T_{i+2}}{\Delta t} \right| > 0
\]  

(B.13)

then the quench time and temperature is defined to be the intersection of \( L'_1 \) and \( L'_2 \) (Figure B-1(b)). Eq. (B.13) can be further reduced to the form of Eq. (B.14) during the quench by considering \( (T_{i+4} < T_{i+3} < T_{i+2}) \) and remove the absolute value symbol:

\[
- \left( \frac{T_{i+4} - T_{i+3}}{\Delta t} - \frac{T_{i+3} - T_{i+2}}{\Delta t} \right) = - \frac{T_{i+4} - 2T_{i+3} + T_{i+2}}{\Delta t} > 0
\]  

(B.14)

or simply as:

\[
\left| \frac{T_{i+4} - 2T_{i+3} + T_{i+2}}{\Delta t} \right| > 0
\]  

(B.15)
Figure B-1. Determination of Quench Temperature in FLECHT-SEASET Tests (Note that the \( L'_1 \) in plot (b) was incorrectly drawn for the third and fourth points)

Similar to Eq. (B.15), in the DATARH program for RBHT reflood test, Eq. (B.16) is used to determine the difference of the slopes (first order derivative of the temperature profile) between three consecutive temperature points (as is shown in Figure B-2).

\[
\left| \frac{T_{i+1} - T_i}{\Delta t} - \frac{T_i - T_{i-1}}{\Delta t} \right| = \left| \frac{T_{i+1} - 2T_i + T_{i-1}}{\Delta t} \right| > 11 \text{ K/s} \tag{B.16}
\]

Whenever the absolute value of the difference is larger than 11 K/s (20 °F/s), this location (location I) is taken as the quench location and the corresponding temperature \((T'_i)\) is taken as the quench temperature. Due to the one-dimensional heat conduction solution scheme adopted in DATARH, uncertainties exist near the quench front location, where 2D effects are significant. The 11 K/s (20 °F/s) criterion is thus selected empirically by evaluating most of the RBHT reflood data for different tests, rods, and elevations. It has been found that 11 K/s (20 °F/s) is a good practice.
Figure B-2. Determination of Quench Temperature in RBHT Tests

Figures B-3 and B-4 show the temperature variation and the first order derivative of the temperature profile for an actual RBHT test, Exp. 1383. As seen in Figures B-3 and B-4, large spike occurred during quench and the first time the difference of the derivatives went above 11 K/s (20 °F/s) was at around 660 s. Using the data, the code calculates the time to be 662.05 s. At this time, TC location of 2.76 m (108.8 in) is considered to be the quench location. Applying the same approach for each TC location yields the quench profile for a reflood test.

Figure B-3. Cladding Temperature at 2.76 m (108.8 in), Exp. 1383
Figure B-4. Zoomed in Temperature and First Derivative at 2.76 m (108.8 in), Exp. 1383.
Appendix C

Droplet Measurement Uncertainty Quantification

In order to thoroughly study the various droplet thermal-hydraulic behaviors including droplet entrainment, deposition, and breakup along with the corresponding heat transfer processes in the DFFB regime, a series of reflood tests have been carried out in the RBHT facility under controlled conditions. Figures C-1 to C-4 show the typical droplet results obtained by the Oxford Lasers Imaging system through image processing software VisiSize for Experiment (Exp.) 7157, performed at the system pressure of 275.79 kPa (40 psia), inlet flooding rate of 0.0254 m/sec (1 in/sec), inlet liquid subcooling of 11 K (20 °F), peak power of 1.31 kW/m (0.4 kW/ft), and initial rod PCT of 1033 K (1400 °F). The current droplet results shown in these figures were averaged every 20 sec starting from the initial water injection such that statistically valid droplet data can be obtained while at the same time as much transient information as possible can be maintained.

Figure C-1 shows the software processed droplet size distribution downstream of SG 6 between 20-40 sec after reflood for Exp. 7157. As can be clearly observed, the droplet size follows a log-normal distribution, with its probability density function given by Eq. (C.1)

\[
PDF(x; \mu, \sigma) = A \frac{1}{x\sigma\sqrt{2\pi}} \exp\left(-\frac{(\ln(x)-\mu)^2}{2\sigma^2}\right)
\]

where,
\[
\mu = 4.926;
\]
\[
\sigma = 0.5514;
\]
\[
A = 530.1.
\]

It is seen from Figure C-1 that a rather good agreement is achieved between the measured data and the log-normal curve fit. Therefore, further analyses on the droplet size will be
mainly based on the log-normal distribution. Nevertheless, other distributions such as the normal distribution and T-distribution will be used for comparison. Note that the droplet arithmetic mean diameter is always smaller than the corresponding Sauter Mean Diameter (SMD). This is because the SMD seeks to conserve the ratio of droplet volume to droplet surface area and the droplet volume is dominated by large droplets. On the other hand, the arithmetic mean depends mainly on the droplet number, which is dominated by small droplets that have a large population. Such volume and number differences lead to the smaller arithmetic mean than the SMD. Definition of the SMD is given Eq. (C.2) as,

\[ d_{32} = \frac{\sum n_j d_j^3}{\sum n_j d_j^2} \tag{C.2} \]

where, \( n_j \) is the droplet number for a particular size group \( j \) within the droplet population and \( d_j \) represents the diameter of that particular size group.

Figure C-1: Droplet Size Distribution at Downstream of SG 6 between 20-40 sec, Exp. 7157
The droplet velocity distribution is shown in Figure C-2. Note that only the x-axis is in the log scale. Though there is large scatter in data, a generally decreasing trend as a function of the droplet size can be observed. As the droplet size gets larger, its velocity decreases accordingly due to increased droplet weight. In the DFFB regime, the two-phase mixture is in a significant thermal-hydraulic non-equilibrium state, with the vapor velocity being significantly higher than the droplet velocity.

Figure C-3 and C-4 present the droplet in-focus count and SMD variations as a function of the reflood time upstream and downstream of SG 6, respectively. It can be seen from Figure C-3 that, as the quench front propagated upward along the test section, the droplet counts up- and downstream of SG dropped gradually. This is due to the transition of flow regime. As the quench front moved toward the point of measurement, larger and more irregular shaped droplets are expected, making it more difficult for the imaging system to recognize the droplets. Large droplets could also be partially hidden by the rods, leading to a decreased droplet in-focus count. It can be seen from Figure C-3 that, after a certain reflood period, the upstream and downstream droplet in-focus counts dropped to very low values. As a result, these data points may not be representative for the total droplet population after that period.

Due to liquid droplet impingement and deposition, the initially dry SGs would be gradually wetted and eventually covered by a liquid film. In the RBHT experiments, transition from dry grid to wet grid is indicated by the temperature measurements of TC’s attached to each SG’s surface. Figure C-4 shows that both the upstream and downstream droplet SMDs increased gradually until the SG became wet, after which significantly larger droplet sizes and fluctuations were observed. Note that when SGs were dry, the upstream droplet diameter was always larger than that of the downstream location. Such a droplet size variation across a dry SG is expected owing to droplet breakup at a dry grid.
Figure C-2: Droplet Velocity Distribution Downstream of SG 6 between 20-40 sec, Exp. 7157

Figure C-3: Droplet In-Focus Count Analyzed by the VisiSize Software, Exp. 7157
C.1 Determination of the Required Droplet Sample Size

This section determines the minimum droplet sample size (droplet in-focus count lower limit) required to have a 95% confidence level based on three different variable distributions, including the normal distribution, T-distribution and log-normal distribution. During reflood, the actual number of droplets in the post-CHF regime over a given time period (for example, 20 sec) is unknown. The question now is how large the sample size (droplet in-focus count) should be in order to have an accurate representation of the total population. In general, total droplet counts in the DFFB regime for a 20 sec period are expected to be very large (at least on the order of $\sim 10^4$). For this reason, an infinite population size for droplets is assumed without applying any finite population correction. In this way, the minimum sample size obtained is considered to be more conservative than the case in which a finite total droplet population is assumed. Based on
specified relative level of precision (desired relative error), the calculated droplet sample size is presented in this study as a function of the sample standard deviation.

C.1.1 Droplet Sample Size Determination

The normal distribution is studied first since it is the simplest to apply. Even though in Figure C-1 it has been observed that the droplet size actually follows a log-normal distribution with its peak skewed to the left side, the central limit theorem (Hogg and Tanis, 2015) allows the normal distribution to be employed regardless of the underlying distribution. According to the central limit theorem, as the sample size increases, the sample mean drawn from a population with any shape of distribution will approach the normal distribution, provided that the variance is finite. In practical applications, the normal distribution may be used as an adequate estimator if the sample size is larger than 30.

The two-sided confidence interval for a variable \( X \) that has the normal distribution can be written as:

\[
P\left[ (\bar{x} - Z_{1-\alpha/2}s_{\bar{x}}) < \mu < (\bar{x} + Z_{1-\alpha/2}s_{\bar{x}}) \right] = 1 - \alpha
\]

(C.3)

\[
s_{\bar{x}} = \frac{\sigma}{\sqrt{n}}
\]

(C.4)

where

\( Z_{1-\alpha/2} \) = normal distribution z-score at 95% confidence (\( \alpha = 0.05 \));

\( \mu \) = mean of variable \( X \);

\( \sigma \) = standard deviation of variable \( X \);

\( \bar{x} \) = sample mean of variable \( X \);

\( s_{\bar{x}} \) = standard error of the mean;

\( n \) = sample size,
The margin of error can be related to the relative error (relative level of precision) of the sample mean as:

\[
R_{\text{err}} = \frac{z_{1-a/2}s}{\bar{x}} = \frac{z_{1-a/2}\sigma}{\bar{x}\sqrt{n}} \tag{C.5}
\]

From Eq. (C.5), the sample size \(n\) can be further determined as:

\[
n = \left(\frac{z_{1-a/2}\sigma}{\bar{x}R_{\text{err}}}\right)^2 \tag{C.6}
\]

Equation (C.6) is then used to calculate the droplet sample size that satisfies 95% confidence level (\(\alpha = 0.05\)) at a specified level of precision. However, application of Eq. (C.6) requires knowledge of the standard deviation \(\sigma\) of the variable \(X\) in advance. In reality, the actual standard deviation of a variable is usually an unknown parameter. Therefore, before determining the sample size, an estimation or guess of this unknown parameter of the population is needed. In the present analysis, the corrected sample standard deviation, \(s\), is used as an approximation for the normal distribution case. In general, the corrected sample standard deviation is defined by:

\[
s = \sqrt{\frac{1}{n-1}\sum_{i=1}^{n}(x_i - \bar{x})^2} \tag{C.7}
\]

However, in practice when the standard deviation of the population is unknown, instead of following a normal distribution, the sample mean is found to follow the T-distribution with the variable mean and standard deviation being \(\mu\) and \(\frac{s}{\sqrt{n}}\), respectively. In this case, the two sided confidence interval for the T-distribution is expressed as:

\[
P[(\bar{x} - t_\alpha s_{\bar{x}}) < \mu < (\bar{x} + t_\alpha s_{\bar{x}})] = 1 - \alpha \tag{C.8}
\]

where

\[
t_\alpha = T\text{-distribution } \alpha\text{-score at } 95\% \text{ confidence (} \alpha = 0.05\text{) for } n-1 \text{ degrees of freedom};
\]

After similar mathematic manipulations, the sample size can be obtained as:

\[
n = \left(\frac{t_\alpha s_{\bar{x}}}{\bar{x}R_{\text{err}}}\right)^2 \tag{C.10}
\]
Where, \( s \) = sample standard deviation of variable \( X \). In order to avoid iteration, t-score (2.045) at \( \alpha = 0.05 \) and degree of freedom of 29 (\( n = 30 \)) are used.

Hale (1972) [42] derived an equation for determining the sample size for variable that has a log-normal distribution. Hale’s equation is expressed as:

\[
 n = \frac{z_{\frac{\alpha}{2}, 29}^2 - 2s^2}{\ln^2(R_{err} + 1)} 
\]  

(C.11)

where,

\( S = \) sample standard deviation of the logarithms of variable \( X \) (\( Y = \log(X) \));

In deriving the above equation, the original variable \( X \) is transformed by logarithm \( Y = \log(X) \). Note that the base of logarithms does not matter. In Hale’s study as well as in the present study, \( \log_e \) is used for convenience. After transformation, the standard procedure for calculating the margin of error is applied to variable \( Y \) and the result is shown in Eq. (C.11).

In the work performed by Olsson (2005) [46], on the other hand, the best confidence interval for variable \( Y \) is given by a Modified Cox method equation as:

\[
\bar{Y} + \frac{S^2}{2} \pm t_\alpha \sqrt{\left(\frac{S^2}{n} + \frac{S^4}{2(n-1)}\right)}
\]

(C.12)

where

\( \bar{Y} = \) sample mean of variable \( Y \);

\( S^2 = \) variance of variable \( Y \),

Unlike Hale’s method, Eq. (C.12) considers the skewed log-normal distribution and thus gives an asymmetric confidence interval for the variable \( Y \). In the current analysis, the sample size is calculated based on the lower limit of the confidence interval. Starting from Eq. (C.12), the relative error (level of precision) for the lower boundary can be written as:

\[
R_{err} = \frac{\exp(\bar{Y} + \frac{S^2}{2}) - \exp(\bar{Y} + \frac{S^2}{2} - t_{\alpha} \sqrt{\left(\frac{S^2}{n} + \frac{S^4}{2(n-1)}\right)})}{\exp(\bar{Y} + \frac{S^2}{2})}
\]

(C.13)

Rearranging the above equation yields:
\[(1 - R_{err}) \exp \left( \bar{Y} + \frac{s^2}{2} \right) = \exp (\bar{Y} + \frac{s^2}{2} - t_\alpha \sqrt{\frac{s^2}{n} + \frac{s^4}{2(n-1)}}) \]  \hspace{1cm} (C.14)

Then, Eq. (C.14) can be further reduced by taking the logarithm on both sides to obtain,

\[\ln(1 - R_{err}) = -t_\alpha \sqrt{\frac{s^2}{n} + \frac{s^4}{2(n-1)}} \]  \hspace{1cm} (C.15)

Equation (C.15) is used to calculate the sample size \(n\) through iteration. Similar to the T-distribution case, the value of 2.045 is used for t-score at 95% confidence level. Note that even though the sample mean \(\bar{Y}\) appears on both sides of Eq. (C.14), it is eventually eliminated after taking the logarithm. Therefore, the sample size \(n\) is only a function of the sample standard deviation, t-score and the required level of precision.

Pashchenko (1996) [44] used a different method to determine the optimal sample size for a variable that exhibits the log-normal distribution. Based on the work originally developed by Finney (1941) [41], the best estimation, \(Y_{EST}\), of the true mean value \(\bar{Y}\) (unbiased and given the minimum variance of the differences from the true mean) is given by the following equation:

\[Y_{EST} = \exp(\bar{Y}) \varphi_n (S^2) \]  \hspace{1cm} (C.16)

where \(\bar{Y}\) and \(S^2\) are the sample mean and variance of \(Y\), respectively, and \(\varphi_n (S^2)\) is a correction function depending on the sample variance and sample size. The ratio of sample mean to its true mean, \(L\), is give as:

\[L = \frac{Y_{EST}}{\bar{Y}} \]  \hspace{1cm} (C.17)

Distribution of \(L\) is then determined by numerical integration using the Gauss method. Detailed derivation can be found in Pashchenko (1996) [44]. Each combination of the sample size \(n\) and sample standard deviation \(S\) values at a specified level of precision was calculated and presented in Pashchenko’s work.
C.1.2 Results and Discussion for Droplet Sample Size Determination

The calculated droplet sample size \( n \) based on the normal distribution is shown in Figure C-5. The sample size was determined at the 95% confidence level and at various specified levels of precision (with relative error ranging from 5% to 40%). The result was presented as a function of sample standard deviation. It can be seen from Figure C-5 that, for each level of precision as the sample standard deviation increases, the sample size needed to satisfy the specified confidence level increases accordingly. Generally speaking, two possible reasons could result in large data scatter (corresponding to a large sample standard deviation): a) limitations in the experimental facility as well as in the measurement instrumentation; b) the actual population of the variable does have a spread distribution ranging from very small values all the way to very large values. In either case a larger sample size is required if a higher level of precision is specified. Similarly, when the droplet size measured by the VisiSize software is more scattered, a larger sample size is needed to achieve a given precision level. For example in Figure C-5, for a relative error of 20% at a sample standard deviation of 150 \( \mu m \), 24 droplet counts are required to ensure 95% confidence level, whereas at a sample standard deviation of 200 \( \mu m \), 43 counts are necessary. If the sample standard deviation obtained from data gives much larger scatter, say 300 \( \mu m \), then at least 96 droplet counts are required.

Figure C-6 shows the results using the T-distribution for 95% confidence level. As mentioned previously, since the T-distribution is actually a modification of the normal distribution that takes into account that the real variable standard deviation is usually unknown and the population size might be relatively small, results thus obtained for the T-distribution are found to be quite similar to the results determined by the normal distribution.

A direct comparison of the sample size determined by the normal- and T-distributions is presented in Figure C-7. Note that comparisons are made only for a relative error ranging from
10% to 25% at the confidence level of 95%. As can be observed from the figure, for fixed level of precision and sample standard deviation, the sample size derived from the T-distribution is always larger than that from the normal distribution. In addition, the differences between these two distributions are found to continuously increase as the sample standard deviation increases. As a result, in practical applications in which the standard deviation is unknown, the sample size determined based on the T-distribution is considered to be more conservative than that based on the normal distribution.

Figure C-5: Droplet Sample Size for Various Rel. Err at 95% Confidence Level (normal distribution)
On the other hand, if the sample size measured from an experiment is given, the required sample standard deviation can then be determined based on the desired level of precision. Table C-1 summarizes the standard deviations required by measurements to ensure that the true means are within specified relative errors at 95% confidence level for normal- and T-distribution, when sample size is fixed at $n = 30$. When the specified level of precision decreases, a sample size of 30 is then considered to be statistically valid for larger standard deviations. For the sample mean to be 75% accurate (relative error of 25%) and sample standard deviation up to $\sim 200 \, \mu m$, a sample size of 30 is found to be good enough. In addition, it can be observed that T-distribution always has a stricter requirement on sample standard deviation (data scatter) than normal distribution.
Figure C-7: Comparison of Droplet Sample Size Required between Normal- and T-Distribution

Table C-1: Max. Std. at Sample Size $n = 30$ for Different Rerr at 95% Confidence Level

<table>
<thead>
<tr>
<th>Type of Dist.</th>
<th>$R_{err}$ (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>10</td>
</tr>
<tr>
<td>Normal - Std. ($\mu m$)</td>
<td>83.8</td>
</tr>
<tr>
<td>T - Std. ($\mu m$)</td>
<td>80</td>
</tr>
</tbody>
</table>

It should be pointed out that the actual droplet size follows a log-normal distribution as shown in Figure C-7. Applications of either normal- or T-distribution are mainly based on the central limit theorem. However, if the droplet size distribution is strongly skewed, one should be cautious and apply these formulas conservatively.

Next, results using the Hale’s method, Modified Cox method and Pashchenko’s method are presented. All these methods were developed directly based on log-normal distribution to determine the required sample size.

Figure C-8 shows the droplet sample size determined by the Hale’s model at various relative errors for 95% confidence level. Note that instead of using the sample standard deviation
for variable $X$ ($X$ represents the actual droplet size), the sample standard deviation of variable $Y = \ln X$ ($Y$ represents the logarithm of actual droplet size) is used. It can be seen from the figure that, similar to results of normal- and T-distribution, a gradually increasing trend in sample size is observed as the sample standard deviation of $Y$ increases. Again, focusing on the curve that has a level of precision of 80% (relative error of 20%), a sample size of 42 is needed for sample standard deviation of 0.6 while 75 is needed for 0.8. As the requirement for the level of precision becomes smaller, for a certain standard deviation, less droplet count is required.

Results obtained using the Modified Cox method recommended by Olsson (2005) [46] is presented in Figure C-9. Note that only lower limit relative error was taken as a reference when calculating the sample size. A similar trend in the sample size variation has been obtained.

Figure C-8: Droplet Sample Size for Various $R_{\text{err}}$ at 95% Confidence Level (Hale’s Method)
In addition to the above two methods, the final sample size results for a 95% confidence level derived by Pashchenko (1996) \[44\] are shown in Table C-2. For a relative error of 10% (level of precision of 90%), more than 200 observations are required when the sample standard deviation is larger than 0.6. For a relative error of 50%, on the other hand, observations needed are always less than 30 due to the reduced requirement in the data precision level.

Table C-2: Sample Size Depending on various $R_{\text{err}}$ and Std. at 95% Confidence Level by Pashchenko (1996) \[44\]

<table>
<thead>
<tr>
<th>$R_{\text{err}}$ (%)</th>
<th>Std. of $Y = \ln X$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.1</td>
</tr>
<tr>
<td>10</td>
<td>8</td>
</tr>
<tr>
<td>20</td>
<td>4</td>
</tr>
<tr>
<td>30</td>
<td>4</td>
</tr>
<tr>
<td>40</td>
<td>4</td>
</tr>
<tr>
<td>50</td>
<td>3</td>
</tr>
</tbody>
</table>

A direct comparison of the Hale’s method, Modified Cox method and Pashchenko’s method can be found in Figure C-10. Only the relative errors of 20 and 30% were selected for comparison. As can be expected, all three methods showed similar trends on sample size.
predictions. Pashchenko’s method is found to yield relatively larger sample size than the other methods and thus is considered to be more conservative. When the standard deviation was small, predictions from all three methods agreed with each other quite well. However, they started to deviate from each other when standard deviation approached larger values. This is especially true for the Hale’s and Modified Cox methods.

Figure C-10: Comparison of Droplet Sample Size for Different $R_{\text{ex}}$ at 95% Confidence Level

In Table C-3, the maximum sample standard deviations allowed in order to maintain the relative error within 20 and 30% at a fixed sample size of 30 within 95% confidence level are listed for the three different methods. As can be seen, with the lower level of data precision a larger sample standard deviation (data scatter) can be tolerated. Results from the Hale’s and Pashchenko’s methods are very close to each other while results from Modified Cox method based on the lower boundary relative error has the highest tolerance of data scatter for specified precision and confidence level. Both for 20 and 30% relative error, the Pashchenko’s method is found to be the most conservative one.
Table C-3: Max. Std. at Sample Size \( n = 30 \) for Different \( R_{err} \) at 95% Confidence Level

<table>
<thead>
<tr>
<th>Method</th>
<th>( R_{err} ) (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( 20 )   ( 30 )</td>
</tr>
<tr>
<td>Max. Std. of Hale’s Method</td>
<td>0.51</td>
</tr>
<tr>
<td>Max. Std. of Modified Cox method</td>
<td>0.56</td>
</tr>
<tr>
<td>Max. Std. of Pashchenko’s Method</td>
<td>0.50</td>
</tr>
</tbody>
</table>

According to the results and discussions above, it is now clear that given a specific confidence level, sample size requirement depends both on the specified level of precision (desired relative error) and on the extent of scattering (standard deviation) inherent in data. A lower precision level specified and a smaller standard deviation measured in data permit a smaller sample size requirement. In addition, the sample standard deviation is generally used as an estimator of the actual standard deviation since the latter is usually an unknown parameter. Therefore, whether a certain sample size is adequate for statistically meaningful analysis has to be determined by the actual experimental data acquired.

C.1.3 Determination of the Droplet Count Lower Limit for the RBHT Tests

As discussed in previous sections, the current RBHT droplet data is averaged and evaluated every 20 sec during the entire reflood test. Whether the droplet sample size obtained within a specific 20 sec time span is adequate for statistically meaningful analysis is crucial for subsequent data evaluation. From previous sections it is known that, for a 95% confidence level, the larger the sample size, the higher the accuracy level of averaged droplet values. That is, if more droplets are captured, the data will be more reliable. Unfortunately, due to limitations of the image processing technique and the extremely complicated test conditions encountered during reflood, sometimes only a small number of droplets are captured within a given time span. If the droplet count lower limit for data evaluation is set too high, then too much information regarding droplet behaviors during highly transient reflood tests will be lost. On the contrary, if the droplet
count lower limit is too low, then sometimes the droplet counts acquired are inadequate for statistically meaningful analysis, since relatively large deviations from true values may exist that may incur bias. As a result, there is trade off in determining the appropriate droplet count lower limit. In the best case, a droplet count lower limit should be set in a way such that it is adequate for statistical analysis while at the same time retain as much transient experimental information as possible.

In order to better select the droplet count lower limit, the experimental data for Exp. 7151 has been processed twice: once with a 20 sec time span and again with only a 5 sec time span for averaging. It is obvious that for 5 sec averaging, lower droplet count of each point is expected, since the time span used is much shorter than 20 sec. In this case, when changing the averaging time span from 5 sec to 20 sec the original four data points collapse into only one data point. By comparing trends from the two sets of data for the same test, effect of lower droplet count on droplet size can be observed and a droplet count lower limit can be selected accordingly. Figure C-11 shows the droplet arithmetic mean comparison between 20 sec and 5 sec averaging. In plotting Figure C-11, all the data points have been considered including the ones with extremely low in-focus counts. For each averaged value, the corresponding droplet in-focus count is printed on the plot near the data point. As can be seen from the figure, the two sets of data agree with each other quite well, indicating that trends represented by them are comparable. Nevertheless, due to the relatively smaller droplet counts, data set of 5 sec averaging is more scattered. Considering Figure C-11 together with results obtained in previous sections for droplet sample size, the droplet count lower limit may be determined.
To obtain statistically meaningful data and at the same time retain as much droplet information as possible, droplet data measured during reflood is averaged every 20 sec and the
droplet in-focus count lower limit is selected as 30. The corresponding corrected droplet count that will be used for statistical analysis is much larger than 30.

The current selection of 30 droplet in-focus count can be further justified by Figure C-12. In Figure C-12, the droplet size relative error (calculated based on the corrected droplet count) as a function of the droplet in-focus count at 95% confidence level based on the Modified Cox method (Olsson 2005 [46]) discussed in the previous section is shown both for upstream and downstream measurement locations, for Exp. 7151. As can be clearly seen from Figure C-12, droplet relative error decreases exponentially as droplet sample size increases. The relative error is found to be well within 16% when a droplet in-focus count lower limit of 30 is selected.

C.2 Determination of the Confidence Intervals for the Droplet Measurement

Droplet data obtained by the Oxford Lasers Firefly Imaging system from a series of selected RBHT experiments was processed in order to quantify the uncertainties involved in droplet size measurements. In the following sections, the 95% confidence intervals for droplet size are determined using methods introduced in section 4. Based on the central limit theorem and whether the standard deviation is known, the normal- or \( T \)-distribution can be used. The Cox method as well as the Modified Cox method are also applied to the RBHT data for comparison. The only difference between the Cox and Modified Cox method is that \( z \)-score is used in the Cox method while the Modified Cox method is based on \( t \)-score. Results for Exp. 7151 and Exp. 7157 are presented in the current paper. Exps. 7151 and 7157 were selected for detailed uncertainty analysis because these two tests are among those reflood tests with long duration of droplet measurement (> 600 sec for both Exps. 7151 and 7157). The test conditions for these two reflood tests are given in Table C-4.
Table C-4: Test Conditions for Exps. 7151 and 7157

<table>
<thead>
<tr>
<th>Exp. No.</th>
<th>Inlet Flow Velocity (m·s⁻¹/in·s⁻¹)</th>
<th>Pressure (kPa/psia)</th>
<th>Initial Rod Peak Temp. (K/°F)</th>
<th>Subcooling (K/°F)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7151</td>
<td>0.0191/0.75</td>
<td>276/40</td>
<td>1033/1400</td>
<td>53/96</td>
</tr>
<tr>
<td>7157</td>
<td>0.0254/1</td>
<td>276/40</td>
<td>1033/1400</td>
<td>11/20</td>
</tr>
</tbody>
</table>

**Figure C-13** shows the temperature variations during reflood for the rod surface, SG and steam near the SG 6 location for Exps. 7151 (a) and 7157 (b), respectively. As can be seen, the general quench profile for these two tests were similar. Due to the smaller liquid injection rate and subsequent slower quench front propagation, a higher temperature rise was observed in rod surface, SG and steam temperature transients for Exp. 7151. Also, Exp. 7151 was found to quench at a later time than Exp. 7157.

During reflood, droplet as well as other rod bundle thermal-hydraulic behaviors are highly transient and they closely interact with each other. In order to obtain as much transient information as possible while at the same time to perform reliable quantitative analysis on statistically valid droplet data, the measured droplet data is averaged every 20 sec, as mentioned in section 4. Within each time period, variables such as droplet in-focus count, arithmetic mean of droplet size, droplet size standard deviation, droplet mean velocity, etc., are obtained from raw data processed by the VisiSize software. These parameters are then used to determine the 95% confidence interval.
C.2.1 Results and Discussion for Exp. 7151

Confidence intervals were determined for droplet sizes measured at both upstream and downstream of SG 6 using normal distribution based on central limit theorem as well as log-normal distribution based on Cox and Modified Cox method. In order to compare the effect of droplet sample size on the measurement uncertainty, two droplet count lower limits were selected: 30 and 100 counts. For example, if the droplet in-focus count lower limit 100 is considered in analysis, then every droplet size data point in Figure C-4 that has a corresponding in-focus count larger than or equal 100 in Figure C-3 will be retained whereas the remaining data points will be discarded. On the other hand, if using 30 droplet in-focus count lower limit, then any data point with in-focus count lower than 30 will be discarded in analysis. Figures C-13, C-14 and C-15 present the calculated confidence intervals of droplet size measured for Exp. 7151.
using three different methods for measurements upstream and downstream of SG 6. In order to obtain the transient uncertainty variation of droplet size measurement during the entire reflood, comparisons were made considering measurements at different time periods. Therefore, the sample mean and standard deviation might be different for each point. Also, note that rather than using a point estimate of the droplet sample mean ($\bar{X} = \exp(\bar{Y} + \frac{s^2}{2})$), the same sample mean (simply as $\bar{X}$) as for the normal distribution were used for the log-normal distribution plots such that a direct comparison can be made between them. In these figures, the blue squares represent data points that have droplet in-focus counts larger than or equal to 30, while those red asterisks represent data points with in-focus counts larger than or equal to 100. For each data point, the corresponding 95% confidence interval determined from experiments is shown as error bars. Also shown in these figures are the lengths of the maximum confidence intervals identified for 30 and 100 cases.

Results based on the normal distribution are presented first. In Figure C-14 (a), it is observed that at the upstream location when changing the droplet count lower limit from 30 to 100, later portion of the data points were screened out due to their low in-focus counts. As can be seen in Figure C-14 (b), at the downstream location, however, the changes in droplet count lower limit only exclude a few points that are primarily in the late stage of reflood. This indicates that measurement downstream of SG 6 has captured more droplet than that at the upstream location. It is also necessary to point out that the confidence intervals in Figure C-14 are symmetric since the normal distribution is applied. Direct comparison of the confidence intervals for 30 and 100 droplet in-focus counts reveals the fact that for larger sample size obtained, significantly smaller errors between estimated sample mean and true mean are observed. Using 30 droplet count results in relatively larger errors for both upstream and downstream locations. As can be seen, when droplet count lower limit is selected to be 30, the maximum absolute errors for the upstream
and downstream are 31.96 and 28.35 \( \mu m \), respectively; when droplet count lower limit is 100, much smaller values of 16.45 and 9.805 \( \mu m \) are found for upstream and downstream, respectively.

![Graph](image)

**Figure C-14:** 95% Confidence Intervals Based on Normal Distribution, Exp. 7151

The 95% confidence intervals based on the Cox method are shown in **Figure C-15**. As discussed at the beginning of this section, similar to the Modified Cox method, the Cox method works directly on log-normal distribution to derive the confidence intervals. Therefore, it is obvious that the upper and lower wings of the calculated intervals are asymmetric with mean value being slightly closer to the lower bound. Despite the asymmetric characteristic, similar trends were observed for both minimum droplet count criteria using the Cox method. A close examination of the maximum absolute error reveals that the two sets of data (30 and 100 count) are very comparable with a relatively larger error being found for droplet count lower limit of 30, indicating that both methods and droplet count lower limits are able to represent the true mean with reliable statistical accuracy. However, it should be noted that due to the different methods used, the point of which the maximum absolute error is observed for the 100 case at the downstream of the SG 6 is shifted.
Last, results of the Modified Cox method for Exp. 7151 are shown in Figure C-16. Note that rather than z-score for the Cox method, t-score corresponding to 95% confidence level was used here. This is because in reality the variable standard deviation is unknown whereas, as a reasonable estimator, the sample standard deviation can be readily obtained from sampling to proceed calculation. Therefore, the Modified Cox method is considered to be conservative compared to the previous two methods. Observations of relatively wider confidence intervals
determined for both 30 and 100 counts as well as for both upstream and downstream locations further confirmed this argument.

Table C-5 gives the maximum absolute and relative errors for all the methods used while Table C-6 gives the lengths of corresponding confidence intervals for Exp. 7151. Note that due to asymmetry, here values at the upper limit bounds have been used for the Cox and Modified Cox method in order to be conservative. As can be seen, at the same requirement for the precision and confidence level, the Modified Cox Method generally gives larger absolute and relative errors as well as lager confidence intervals.

Table C-5: Comparison of Absolute and Relative Errors for Different Methods, Exp. 7151

<table>
<thead>
<tr>
<th>Methods</th>
<th>Upstream of SG 6</th>
<th>Downstream of SG 6</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Count ≥ 30</td>
<td>Count ≥ 100</td>
</tr>
<tr>
<td>A_{err} (μm)</td>
<td>R_{err} (-)</td>
<td>A_{err} (μm)</td>
</tr>
<tr>
<td>Nor. Dist.</td>
<td>31.96</td>
<td>0.113</td>
</tr>
<tr>
<td>Cox</td>
<td>32.79</td>
<td>0.116</td>
</tr>
<tr>
<td>Mod. Cox</td>
<td>34.30</td>
<td>0.121</td>
</tr>
</tbody>
</table>

Table C-6: Comparison of Length of Confidence Intervals for Different Methods, Exp. 7151

<table>
<thead>
<tr>
<th>Methods</th>
<th>Upstream of SG 6</th>
<th>Downstream of SG 6</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Count ≥ 30</td>
<td>Count ≥ 100</td>
</tr>
<tr>
<td>Nor. Dist. (µm)</td>
<td>63.92</td>
<td>32.92</td>
</tr>
<tr>
<td>Cox (µm)</td>
<td>62.12</td>
<td>33.50</td>
</tr>
<tr>
<td>Mod. Cox (µm)</td>
<td>64.83</td>
<td>34.95</td>
</tr>
</tbody>
</table>

C.2.2 Results and Discussion for Exp. 7157

Similar to Exp. 7151, the confidence intervals for Exp. 7157 based on all three methods are presented from Figure C-16 to C-19 in the same manner. In addition, the maximum absolute and relative errors and the lengths of confidence intervals are presented in Table C-7 and Table C-8, respectively.
As observed in Exp. 7151, the downstream droplet in-focus count obtained is always larger than that of upstream measurement locations. Therefore, the majority of the downstream droplet points have in-focus counts larger than or equal to 100, resulting in small uncertainty. For the upstream droplet measurement, however, droplet in-focus count dropped below 100 in the very early stage of reflood due to wet out of the viewing window as well as many other reasons discussed previously. As a result, the maximum errors identified for the upstream location is larger than that of the downstream location. Results of Exp. 7157 further confirm that the Modified Cox method tends to have conservative predictions in droplet size errors and confidence intervals.

Figure C-16: 95% Confidence Intervals Based on Normal Distribution, Exp. 7157
A detailed evaluation of Exp. 7151 and Exp. 7157 results shows that droplet size measured at the downstream of SG 6 always has better level of precision compared with that at the upstream location. The maximum relative errors for Exp. 7151 and Exp. 7157 are found to be 0.121 (12.1%) and 0.142 (14.2%), respectively, at the upstream location of SG 6 when droplet in-focus count lower limit is 30. For the droplet in-focus count lower limit of 100 at the same location, the maximum relative errors for Exp. 7151 and Exp. 7157 are 0.070 (7%) and 0.041 (4.1%), respectively. This is partially because droplet counts obtained downstream were always
larger than that of upstream, as is shown in Figure C-3. A further extensive investigation on a series of RBHT reflood tests recently performed showed similar results. Uncertainties for the droplet size measurements at the upstream of SG 6 are generally within 0.15 (15%) relative level of precision for lower limit of 30, while they are generally within 0.07 (7%) for the lower limit of 100, except for Exp. 7102 which has a maximum relative error of 0.191 (19.1%) for lower limit of 30.

Table C-7: Comparison of Absolute and Relative Errors for Different Methods, Exp. 7157

<table>
<thead>
<tr>
<th>Methods</th>
<th>Upstream of SG 6</th>
<th>Downstream of SG 6</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Count ≥ 30</td>
<td>Count ≥ 100</td>
</tr>
<tr>
<td></td>
<td>$A_{err}$ ($\mu m$)</td>
<td>$R_{err}$ (%)</td>
</tr>
<tr>
<td>Nor. Dist.</td>
<td>43.31</td>
<td>0.111</td>
</tr>
<tr>
<td>Cox</td>
<td>52.94</td>
<td>0.136</td>
</tr>
<tr>
<td>Mod. Cox</td>
<td>55.39</td>
<td>0.142</td>
</tr>
</tbody>
</table>

Table C-8: Comparison of Length of Confidence Intervals for Different Methods, Exp. 7157

<table>
<thead>
<tr>
<th>Methods</th>
<th>Upstream of SG 6</th>
<th>Downstream of SG 6</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Count ≥ 30</td>
<td>Count ≥ 100</td>
</tr>
<tr>
<td>Nor. Dist. (\mu m)</td>
<td>86.62</td>
<td>21.84</td>
</tr>
<tr>
<td>Cox (\mu m)</td>
<td>99.61</td>
<td>21.42</td>
</tr>
<tr>
<td>Mod. Cox (\mu m)</td>
<td>103.95</td>
<td>22.35</td>
</tr>
</tbody>
</table>

Another important observation, by matching the droplet uncertainty with the SG conditions during the entire period of reflood, is that relatively larger errors as well as the maximum errors always take place when the spacer grids are wet, indicating that relatively larger uncertainties maybe involved in the droplet measurement when SGs are wet. This is true for both upstream and downstream droplet measurements when droplet in-focus count lower limit is 30. When 100 droplet counts is set as a lower limit, no such phenomenon can be observed since too many data points are excluded, especially for the upstream location.
C.2.3 Calculation of the Confidence Intervals Based on the Droplet Sauter Mean Diameter

From Figure C-1 it can be seen that for the same droplet distribution, the droplet size arithmetic mean diameter is always smaller than droplet Sauter mean diameter. Therefore, due to the different ways of expressing the averaged droplet size (which is usually considered as a reference value in statistical analysis), different length of confidence intervals as well as different absolute errors are expected for the same precision and confidence level. This is because in general the standard deviation from the arithmetic mean is smaller than that from any other measure. Rewrite Eq. (C.18) as

\[ s(u) = \sqrt{\frac{1}{n-1} \sum_{i=1}^{n} (x_i - \bar{u})^2} \]  

(C.18)

Using calculus or simply by completing the square, it can be shown that \( s(u) \) has a unique minimum at the mean value: \( u = \bar{x} \).

Given the same level of precision requirement, since the droplet Sauter mean diameter is larger than arithmetic mean diameter, the corresponding confidence intervals thus determined by the Sauter mean will be larger than that determined by the arithmetic mean. Using the same approach discussed above for the droplet arithmetic mean, the absolute and relative errors as well as the confidence intervals can be calculated for the Sauter mean diameter.

Figure C-19 shows the comparison of 95% confidence intervals between the droplet arithmetic mean and the Sauter mean for the upstream measurement location of Exp. 7151. Figure C-20 shows the corresponding results for the downstream measurement location. All the calculations performed here are based on the simple normal distribution and only data points that have droplet in-focus counts larger than 30 are used. As can be seen from Figure C-19, for the upstream location the maximum droplet size uncertainties occurred within 300-400 sec after the start of reflood when very few droplets were captured. It should be noted that the point at which the maximum uncertainty occurs for the Sauter mean diameter (at 340 sec) and the arithmetic
mean diameter (at 360 sec) are not the same. This is mainly due to the fact that different averaging methods yield different mean values, the relative uncertainty is on the same level and the absolute uncertainty varies as a strong function of the averaged value. The Sauter mean diameter thus obtained at 340 sec is much larger than the one at 360 sec due to possible large droplets identified since the Sauter mean diameter seeks to conserve the ratio of volume to surface area. If at the same time (at 340 sec) many small droplets were also measured, their large population, instead, would have a significant contribution to the calculation of arithmetic mean, leading to smaller droplet size average than the one at 360 sec. As discussed previously, the confidence interval taking the Sauter mean diameter as a reference is much larger than interval calculated taking the arithmetic mean as a reference.

Figure C-19: 95% Confidence Intervals for the Upstream Droplet Size Measurement, Exp. 7151

For the downstream droplet measurement uncertainties, it can be observed from Figure C-20 that both the maximum confidence intervals were obtained during very late times of the
reflood. Again, it can be clearly seen that the confidence interval calculated for the Sauter mean diameter almost doubled than that for the arithmetic mean diameter.

Figure C-20: 95% Confidence Intervals for the Downstream Droplet Size Measurement, Exp. 7151

Despite the significant difference in the absolute errors for Sauter mean and arithmetic mean, it turned out that their corresponding relative errors are quite similar. Table C-9 summarizes the 95% confidence intervals as well as the absolute and relative errors between the two reference mean values for Exp. 7151 and Exp. 7157. As is confirmed in Table C-9, confidence intervals corresponding to the Sauter mean diameters are remarkably wider than those based on the arithmetic mean diameters. However, for a specific point, its level of precision expressed by the relative error is found to be very close between the two cases (0.092 vs 0.082 for Exp. 7151 and 0.106 vs 0.086 for Exp. 7157), indicating that same uncertainty levels are obtained for the same point, regardless of the reference values being selected for calculation.
Table C-9: Comparison of Confidence Intervals for Different Droplet Size References

<table>
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<tr>
<th>Test No.</th>
<th>Measurement Location</th>
<th>Arithmetic Mean Diameter</th>
<th>Sauter Mean Diameter</th>
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<td></td>
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<tr>
<td></td>
<td></td>
<td>Mean (µm)</td>
<td>A_{err} (µm)</td>
</tr>
<tr>
<td></td>
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<td>31.96</td>
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<tr>
<td></td>
<td>Downstream</td>
<td>307.0</td>
<td>28.35</td>
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C.3 Comparison of Two Identical Tests at Given Droplet In-Focus Count Lower Limits

In order to verify the repeatability of the RBHT reflood tests, particularly the droplet measurements, and to further investigate the general effects of selection of droplet in-focus count lower limit on the subsequent data analysis, the droplet sizes measured in two identical tests were compared. For this reason, Exp. 7102 and Exp. 7112 were selected since they have exactly the same test conditions, performed at the system pressure of 275.79 kPa (40 psi), flooding rate of 0.0254 m/sec (1 in/sec), inlet subcooling of 53 K (90 °F), rod bundle peak power of 0.98 kW/m (0.3 kW/ft) and initial PCT of 1033 K (1400 °F). The quench front profiles (the quench front location as a function of reflood time) for the two tests are shown in Figure C-21, with very good agreement were achieved between these two tests. The rod surface, SG and steam temperature variations for the two tests are shown in Figure C-22. It can be seen that very similar trends were obtained for these two tests, indicating that they are highly repeatable.
Figure C-21: Quench Profiles during Reflood for Exps. 7102 and 7112

Figure C-22: Temperature Variations during Reflood for Exps. 7102 and 7112
Table C-10 summarizes the SG 5 and SG 6 wetting times during reflood. SGs are unheated structures inside the rod bundle. It has been observed in the RBHT experiments that the SGs are initially dry upon starting of the liquid injection. Due to increased droplets entrainment downstream of the quench front, droplets would deposit onto the SG surfaces causing the grid to quench. The duration of SG quench process depends on many factors such as the flooding rate, initial rod temperature, inlet liquid subcooling, system pressure, etc.. For Exp. 7102, SG 5 (2.176 m/86.55 in) was fully quenched at 206.5 s and SG 6 (2.794 m/110 in) was fully quenched at 235.2 s, while for Exp. 7112, SG 5 was quenched at 196 s and SG 6 at 250 s. As can be seen, the quench times for the two tests were similar with the maximum time difference being 15 s.

Table C-10: SG 5 and SG 6 Wetting Times for Exps. 7102 and 7112

<table>
<thead>
<tr>
<th>Exp. No.</th>
<th>Partially Wet Period for SG 5 (sec)</th>
<th>Partially Wet Period for SG 6 (sec)</th>
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<td>74.9-235.2 (160.3)</td>
</tr>
<tr>
<td>7112</td>
<td>152-196 (44)</td>
<td>89-250 (161)</td>
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</tbody>
</table>

Figure C-23 shows a comparison of the droplet SMD between Exp. 7102 and Exp. 7112. The droplet data was compared at both upstream and downstream locations of SG 6 for four different droplet in-focus count lower limits (200, 100, 50 and 30). As the droplet in-focus count decreases from 200 to 30, more and more data points are retained in the plots. As the quench front propagates upward, both the upstream and downstream droplet sizes increased significantly. In general the droplet measurements from the two tests repeat the trend reasonably well even for the case with droplet in-focus count lower limit of 30, indicating that the droplet behaviors obtained (both the averaged values as well as the measurement uncertainties) are highly repeatable in the RBHT reflood tests. Nevertheless, it can be recognized that the level of precisions in the droplet size measurement between the two tests are relatively higher for the 200 lower limit as compared
to those for the 30 lower limit. The main disadvantage of a high in-focus count lower limit is that it tends to exclude more data points than the 30 lower limit, especially for the upstream location. As a result, droplet behaviors in the later stage of reflood are lost.

Figure C-23: Droplet Size Comparison between Exps. 7102 and 7112

The droplet count lower limit of 30 is found to retain adequate information during reflood and give a reasonable level of precision. A droplet in-focus count lower limit smaller than 30 tends to yield unacceptable uncertainties in the droplet measurements.
Appendix D

COBRA-TF Input Deck for RBHT Reflood Test

*******************************************************************************
* INPUT DECK
* RBHT Four-Subchannel Model
* Reflood 1 in/sec
* No Radiation H.T.
* This input models the rbht experiment 8009
* Pressure: 40 (psia); Reflood Temp: 1400 (F); Peak Power: 0.4 (kW/ft)
* Subcooling: 40 (F)
*
* YJ_9 subchannel_3 section
*******************************************************************************

* Main Problem Control Data
********************************************************

* ICOBRA
  0
* DTSTEP   TIMET
  0   0.0
* EPSO     OITMAX  IITMAX
  .001   10   40
* INIT     TEXT
  1     **** Reflood 1 in/sec Bundle Rod 7x7 ****

* Group 1 - Calculation Variables and Initial Conditions
********************************************************

*NGRP  NGAS
  1   1
  * PREF   HIN   GIN   AFLUX   GHIN   VFRAC1   VFRAC2   RSBF
     40.0  1696.7   0.0  0.267    124.  0.0   0.9999   1.0
* GTYPE   VFRAC
  air    .0001
          0  700.0
* Small Droplet Flag (0 off, 1 on)
  1
*

* Group 2 - Channel Description
********************************************************

*NGRP  NCHA
  2   9
  * I  AN  PW  ABOT  ATOP  NMGP  NRRD  DROD
     10.545  4.70   0.   0.   2   4.  .374
*INOD  KGPB  KGPA  INOD  KGPB  KGPA
  31   1   0   1   0   1
* I  AN  PW  ABOT  ATOP  NMGP  NRRD  DROD
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32.72323.50 0. 0. 4 20. .374
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*  I  AN  PW  ABOT  ATOP  NMGP  NRRD  DROD
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 3 1 0
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*INOD KGPB KGPA
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*  I  AN  PW  ABOT  ATOP  NMGP  NRRD  DROD
950.2471.77 0. 0. 0
*
**********************************************************************
*********
* Group 3 - Transverse Channel Connection (Gap) Data                          *
******************************************************************************
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*  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
 1 1  20.9760.496 0.5 0.0  0 4 1.0  0 2
*GMLT ETNR
 8.  0.0
  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
 2 2  31.952 .496 0.5 0.0  0 5 1.0  1 3
*GMLT ETNR
 16. 0.0
  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
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*GMLT ETNR
 24. 0.0
  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
 4 5  60.9760.496 0.5 0.0  1 0 1.0  0 5
*GMLT ETNR
 1.  0.0
  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
 5 6  71.952 .496 0.5 0.0  2 0 1.0  4 6
*GMLT ETNR
 1.  0.0
  K  IK  JK  GAPN  LNGT  WKR  FWAL  IGPB  IGPA  FCT  IGAP  JGAP
283

| 6 | 7 | 82.928 | 2.0 | 0.5 | 0.0 | 3 | 0 | 1.0 | 5 | -1 |

*GMLT ETNR
  1. 0.0

*NMGP
  0

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* Group 4 - Vertical Channel Connection Data
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*ISEC NCHN NONO DXS IVAR
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* CARD 4.3

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    3 7
    4 8

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  * I KCHA KCHB
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    6 9
    7 9
    8 9

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  * I KCHA KCHB
    9 9

  *IWDE
    4

  *MSIM MSIM MSIM MSIM MSIM MSIM MSIM MSIM MSIM
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*******************************************************************************
* Group 7 - Local loss coefficient and Grid spacer data
*******************************************************************************

*NGRP NCD NGT IFGQ IFSD IFES IFTP NFBS
  7 0 1 1 1 1 1 1 0 0 0 0

* nglcof
  * 1

  * CDL JICDM
  * 1.2 2 1 2 3 4
  * 1.2 5 1 2 3 4
  * 1.2 9 1 2 3 4
  * 1.2 14 1 2 3 4
  * 1.2 19 1 2 3 4
  * 1.2 25 1 2 3 4
  * 1.2 29 1 2 3 4
* ING NGAL NGCL IGMT GLOSS GABLOC GLONG GPERIM
  1 7 4 1 1.75 .3620 1.5 1.984
*NNGL
  2 5 9 14 19 25 29
*Initial Grid Temperatures
* TG TG TG TG TG TG TG
  800.894.1042.1172.1312.1354.979.
*NCGL GMLT NGRD NGSR NGRD NGSR NGRD NGSR NGRD NGSR NGRD NGSR
  1 4. 1 1 2 1 3 1
  2 12. 2 2 3 2 4 1 5 1
  3 20. 4 2 5 2 6 1 7 1
  4 13. 6 2 7 2
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*********************************************************************************************
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*********************************************************************************************
*NGRP NRRD NSRD NC NRTB NRAD NLYT NSTA NXF NCAN RADF
  8 7 1 3 2 0 0 0 1 0 0
* N IFTY IAXP NRND DAXMIN RMULT RADIAL HGAP ISEC HTMB TAMB
  1 1 1 2 0.05 1. 1.0 0. 1 0.
*NSCH PIE
  1 1.0
* N IFTY IAXP NRND DAXMIN RMULT RADIAL HGAP ISEC HTMB TAMB
  2 1 1 2 0.05 4. 4.0 0. 1 0.
*NSCH PIE NSCH PIE
  1 0.5 2 0.5
* N IFTY IAXP NRND DAXMIN RMULT RADIAL HGAP ISEC HTMB TAMB
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*NSCH PIE NSCH PIE
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*NSCH PIE NSCH PIE
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  6 1 1 2 0.05 20. 1.0 0. 1 0.
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* N IFTY IAXP NRND DAXMIN RMULT RADIAL HGAP ISEC HTMB TAMB
  7 1 0 2 0.05 4. 0.0 0. 1 0.
*NSCH PIE NSCH PIE
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* N ISTY HPERO HPERI RMULT NSLC NSLO HTAMB TAMBS
  1 3 14.20 14.20 1.0 4 0 0.0 75.0
* ***** Initializaiton Temp Table for rods*****
* I NRT1 NST1 NRX1
  1 7 0 13
*IRTB IRTB IRTB IRTB IRTB IRTB IRTB
  1 2 3 4 5 6 7
* AXIALT TRINIT AXIALT TRINIT AXIALT TRINIT AXIALT TRINIT
  0. 642.50 25.0 963.00 50.0 1105.0 75.0 1272.0
* 108. 1416.0 125.0 1109.0 144.5 698.60
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<td>14.77</td>
<td>1352</td>
<td>.1914</td>
<td>15.72</td>
</tr>
<tr>
<td>1500</td>
<td>.1935</td>
<td>15.86</td>
<td>1652</td>
<td>.2046</td>
<td>16.66</td>
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<tr>
<td>1832</td>
<td>.2166</td>
<td>17.61</td>
<td>2012</td>
<td>.2297</td>
<td>18.56</td>
</tr>
<tr>
<td>2192</td>
<td>.2417</td>
<td>19.50</td>
<td></td>
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<tr>
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<td>14</td>
<td>119</td>
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</tr>
</tbody>
</table>

| BN    |       |
| 212   | .16587| 67.37 | 392   | .22014| 63.827|
| 572   | .26263| 60.28 | 752   | .29590| 56.737|
| 932   | .32194| 53.19 | 1112  | .34233| 49.646|
1292. .35829 46.10 1472. .37078 42.555
1652. .38056 39.01 1832. .38822 35.464
2012. .39421 23.50 2192. .39891 23.375
2372. .40259 24.83 3 10 528.8 H coil
400. .11400 13.00 600. .117 14.833
800. .120 16.50 1000. .125 18.333
1200. .132 23.50 1800. .186 25.167
1600. .157 25.167

70.0 .10000 10.083 200. .107 11.333
400. .11400 13.00 600. .117 14.833
800. .120 16.50 1000. .125 18.333
1200. .132 23.50 1800. .186 25.167
1600. .157 25.167

* TPROP CPF1 THCF TPROP CPF1 THCF
650.0 0.2 .058 800. 0.2 .077

***********************************************************************************
* Group 11 - Axial Power Tables and Forcing Functions
***********************************************************************************
* CARD 11.1
*NGRP NAXP NQ NGPF
11 1 4 0
* CARD 11.2
* I NAXN
1 5
* CARD 11.3

* Y AXIAL Y AXIAL Y AXIAL Y AXIAL
0. 0.0 0.001 0.5 108. 1.5 143.9 0.5
144.1 0.5
* CARD 11.4

* YQ FQ YQ FQ YQ FQ
0.0 1.0 0.1 1.0 0.2 1.0 1000. 1.0

***********************************************************************************
* Group 13 - Boundary Conditions Data
***********************************************************************************
*NGRP NBND NKBD NFUN NGBD
13 5 0 1 0 0
*NPTS
4
*ABSC ORDI ABSC ORDI ABSC ORDI ABSC ORDI
0.0 0.0 1.0 0.0 2.0 1.01000. 1.0
*IBD1 IBD2 ISPC NPFF NNHF PVALUE HVALUE XVALUE
1 1 2 1 0 0.01875 195.61 40.
*HMGA GVAL GVAL GVAL
124. 1.0 0.9999 0.0001
*NHFN NGFN
0 0
*IBD1 IBD2 ISPC NPFF NNHF PVALUE HVALUE XVALUE
2 1 2 1 0 0.056206 195.61 40.
*HMGA GVAL GVAL GVAL
124. 1.0 0.9999 0.0001
*NHFN NGFN
0 0
*IBD1 IBD2 ISPC NPFF NNHF PVALUE HVALUE XVALUE
3 1 2 1 0 0.09367 195.61 40.
*HMGA GVAL GVAL GVAL
124.  1.0.9999.0001
*NHFN NGFN
 0 0
*IBD1 IBD2 ISPC NPFN NHFN  PVALUE    HVALUE    XVALUE
 4 1 2 1 0 0.07973   195.61   40.
*HMGA GVAL GVAL GVAL
124.  1.0.9999.0001
*NHFN NGFN
 0 0
*IBD1 IBD2 ISPC NPFN NHFN  PVALUE    HVALUE    XVALUE
 9 4 1 0 0 40.0   1170.0  40.0
*HMGA GVAL GVAL GVAL
124.  1.0.9999.0001
*NHFN NGFN
 0 0
*********************************************************************************************************************************************
* Group 14 - Output Options
*********************************************************************************************************************************************
*NGRP   N1 NOU1 NOU2 NOU3 NOU4 IPRP IOPT IRWR
 14  5  0  0  0  0  1  2  1
 0  0
*MAXDP
 9000
*        DTMN        DTMAX          TEND         RTWFP          TMAX
 99999999.00000001  0.01  750.0          1.0  99999999.0
*        EDINT         GFINT        SEDINT
 1.0  1.0  5.0
*        DTMN (if negative stop)
 999999999.00000001
Appendix E

Collapsed Liquid Level Variations at Different System Conditions
VITA

Yue Jin

Yue Jin was born on April 3, 1989 in China. He received his Bachelor’s degree in Nuclear Engineering from Xi’an Jiao Tong University in 2011 and Master’s degree in Nuclear Engineering from Shanghai Jiaotong University in 2014. He joined Penn State in August 2014 to further his graduate studies in Nuclear Engineering at the Pennsylvania State University. His research interests include two-phase flow mass and heat transport, liquid droplet dynamics, nuclear reactor thermal-hydraulics and safety analysis, numerical simulation, and uncertainty quantification. So far, he has published 12 refereed journal/conference papers with two additional journal papers under review. He has accepted an offer from MIT as a Postdoctoral Research Associate to further his research in Nuclear Engineering upon completing his dissertation work at Penn State.