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Critical Heat Flux Predictions for the Sandia Annular Core Research Reactor

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Prepared by Sandia National Laboratories Albuquerque, New Mexico 87185
and Livermore, California 94550 for the United States Department of Energy
under Contract DE-AC04-94AL85000

Printed August 1994

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Critical Heat Flux Predictions for the Sandia Annular Core Research Reactor*

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Abstract

This study provides best estimate predictions of the Critical Heat Flux (CHF) and the Critical Heat Flux Ratio (CHFR) to support the proposed upgrade of the Annual Core Research Reactor (ACRR) at Sandia National Laboratories (SNL) from its present value of 2 MWt to 4 MWt. These predictions are based on the University of New Mexico (UNM) - CHF correlation, originally developed for uniformly heated vertical annuli. The UNM-CHF correlation is applicable to low-flow and low-pressure conditions, which are typical of those in the ACRR. The three hypotheses that examined the effect of the nonuniform axial heat flux distribution in the ACRR core are (1) the local conditions hypotheses, (2) the total power hypothesis, and (3) the global conditions hypothesis. These hypotheses, in conjunction with the UNM-CHF correlation, are used to estimate the CHF and CHFR in the ACRR.

Because the total power hypothesis predictions of power per rod at CHF are approximately 15%-20% lower than those corresponding to saturation exit conditions, it can be concluded that the total power hypothesis considerably underestimates the CHF for nonuniformly heated geometries. This conclusion is in agreement with previous experimental results. The global conditions hypothesis, which is more conservative and more accurate of the other two, provides the most reliable predictions of CHF/CHFR for the ACRR. The global conditions hypothesis predictions of CHFR varied between 2.1 and 3.9, with the higher value corresponding to the lower water inlet temperature of 20°C.

MASTER

* The work described in this report was performed for Sandia National Laboratories under Contract No. 40-5800.

ACKNOWLEDGMENTS

To J.S. Philbin, F.M. Morris, B.F. Estes, and their co-workers at the Annular Core Research Reactor facility for the valuable high-power in-core data collected in two separate experiment campaigns in the mid-1980s, for their insights, constant encouragement, and funding support.

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NOMENCLATURE

A_c	flow area per fuel rod (m^2)
A_h	heated area per rod (m^2)
C_p	specific heat (J/kg)
D	fuel rod diameter (m)
D_e	equivalent hydraulic diameter (m)
D_h	equivalent heated diameter (m)
f	friction factor
f_m	Moody's friction factor
G	mass flux ($kg/m^2 \cdot s$)
G^*	dimensionless mass flux
g	acceleration due to gravity, (m/s^2)
H	height (m)
h	heat transfer coefficient ($W/m^2 \cdot K$)
h_{fg}	latent heat of evaporation (kJ/kg)
J_H	Colburn's J-Factor ($St Pr^{2/3}$)
K	coefficient of pressure loss
k	thermal conductivity ($W/m \cdot K$)
L	length of the fuel rod or heated rod (m)
L_{BC}	critical boiling length (m)
$\dot{m}$	mass flow rate per rod (kg/s)
N	total number of fuel rods in the core
Nu	Nusslet number (hD_e/k)
P	pressure (MPa)
P/D	pitch to diameter ratio
Pr	Prandtl number ($\mu C_p/k$)
Q	reactor power (MW)
q	power per fuel rod (W)
q^*	dimensionless heat flux
q'	local linear power (W/m)
q''	heat flux (W/m^2)
Ra_q	Rayleigh number
Re	Reynolds number ($\dot{m} D_e / A_c \mu$)
St	Stanton number (h/GC_p)

T	temperature (K)
x	equilibrium quality
z	axial distance from the bottom of the fuel region (m)

Greek Letters

α	void fraction
ε	annulus ratio
Δ	differential
Δh_i	inlet subcooled enthalpy (kJ/kg)
$\Delta \rho$	density differential (kg/m ³)
γ	radial peak-to-average power ratio in the ACRR
μ	dynamic viscosity (kg/m ² •s)
ρ	density (kg/m ³)
$\bar{\rho}$	average water density (kg/m ³)
σ	surface tension (N/m)

Subscripts

acc	acceleration
ave	averaged
b	at the water bulk temperature
buoy	buoyancy
dsb	developing subcooled boiling
c	cladding surface, critical core
cc	at the center of the core
ch	chimney
dc	downcomer
ent	at the channel entrance
ex	core exit
exit	at the channel exit
fr	friction
in	core inlet
l	liquid
r	radial
sat	saturation
sb	developed subcooled boiling

sp	single-phase convection
t	total
v	vapor
w	the cladding

Superscripts

f	active fuel region
i	index for the control volume
l	lower nonfuel region
u	upper nonfuel region

Critical Heat Flux Predictions for the Sandia Annular Core Research Reactor

EXECUTIVE SUMMARY

The purpose of the present study is to provide best estimate predictions of the Critical Heat Flux (CHF) and the Critical Heat Flux Ratio (CHFR) for the Sandia National Laboratories (SNL) Annular Core Research Reactor (ACRR). These predictions, obtained to support the proposed upgrade of the ACRR power level from its present value of 2 MWt to 4 MWt, are based on the University of New Mexico (UNM) - CHF correlation, originally developed for uniformly heated vertical annuli. The UNM-CHF correlation is applicable to low-flow and low-pressure conditions, which are typical of those in the ACRR.

The predictions of CHF and CHFR in the ACRR involved accomplishing the following tasks:

1. Developing a thermal-hydraulic model to estimate water flow rate through the ACRR core at CHF. Because the ACRR is cooled by natural circulation of water, direct measurement of the mass flow rate through the core is not possible. To indirectly determine the mass flow rate through the ACRR Core as a function of power level and inlet water temperature, a one-dimensional thermal hydraulic model of the ACRR was developed and verified. The model predictions of the temperature differential across the core (which is inversely proportional to the mass flow rate through the core) are compared with the in-core measurements at various power levels (2.0, 3.25, and 4.0 MWt). The good agreement between the model predictions and in-core temperature measurements demonstrated the soundness of using the model to calculate the water mass flow rate through the core for the purpose of estimating CHF and CHFR in the ACRR.

In addition, the model was employed to analyze the thermal-hydraulics performance of the ACRR at 2 MWt and 4 MWt power levels. Results indicate that subcooled boiling begins in the central and average channel of the ACRR core at reactor power levels of 0.5 MWt and 0.7 MWt, respectively. At higher power levels the extent of the subcooled boiling along the active fuel length increases, but the maximum cladding temperature increases very little. This confirms the observation of subcooled boiling in the ACRR core at 2 MWt. At this power level, the exit temperature increases from 336 to 358 K as the inlet core temperature increases from 295 to 328 K. But, because the maximum exit temperature (358 K) is still about 25 K below saturation, the vapor bubbles forming at the cladding surface apparently collapse as they detach from the cladding surface. Consequently, the presence of subcooled boiling at 2 MWt operation does not cause any power fluctuations. This explains absence of power fluctuations at 2 MWt, despite observed subcooled boiling. However, at a reactor power level of 4 MWt and water inlet temperature of 328 K, the exit temperature reached up to 349 K, which is only 9 K below the saturation temperature. These conditions are expected to enhance the vapor bubble survival upon detachment from the cladding surface and may result in net vapor void fraction at the exit of the core. Such void fraction at low-pressure and low-flow conditions induces flow chugging, which in turn, induces substantial power fluctuations. This may explain the observed reactor power

fluctuations at 4 MWt and inlet water temperature of 328 K. Extensive chugging of the coolant in the ACRR might trigger automatic reactor trip at power levels, which could be less than 40% of those corresponding to CHF. Therefore, onset of CHF is a concern if such a reactor scram does not occur.

2. Application of the UNM-CHF correlation, developed for uniformly heated geometries, to predict CHF/CHFR for nonuniformly heated geometry, such as the ACRR. There exist three distinct hypotheses to account for the effect of the nonuniform axial heat flux distribution. These hypothesis are (a) the local conditions hypothesis, (b) the total power hypothesis, and (c) the global conditions hypothesis. These hypothesis are examined and their effects on the predicted values of CHF and CHFR are quantified. All three hypotheses, in conjunction with the UNM-CHF correlation, are used to estimate the CHF and CHFR in ACRR.

The local conditions hypothesis states that CHF is only influenced by the local conditions such as vapor quality at CHF, and the upstream conditions have little influence on CHF. The total power hypothesis indicates that CHF will occur at the same exit quality regardless of the axial heat flux profile. The global conditions hypothesis takes into consideration the effects of both the upstream conditions (total power) and the local conditions on CHF. This hypothesis states that irrespective of the shape of the axial heat flux profile there is a unique relationship between the vapor quality at and the boiling length up to the location of CHF.

All three hypotheses predict that the total power per rod corresponding to CHF monotonically decrease with increasing inlet temperature (or decreasing inlet subcooling). The total power hypothesis predictions for the power per rod corresponding to CHF varied from a maximum of 57 kWt at an inlet temperature of 20°C to approximately 39 kWt at an inlet temperature of 55°C. Assuming that CHF occurs in the central hottest subchannel, the ACRR power corresponding to CHF was estimated to vary between 9.6 and 6.4 MWt for inlet temperature between 20° and 55°C. At 4.0 MWt, the CHFR in the ACRR is estimated as 2.4 at an inlet temperature of 20°C and 1.6 at an inlet temperature of 55°C. For the same range of inlet temperatures, the local conditions hypothesis predictions for CHFR varied between 4.25 and 2.6, while the global conditions hypothesis predictions varied between 3.9 and 2.1.

Because the total power hypothesis predictions of power per rod at CHF are approximately 15%-20% lower than those corresponding to saturation exit conditions, it can be concluded that the total power hypothesis considerably underestimates the CHF for nonuniformly heated geometries. This conclusion is in agreement with previous experimental results. The global conditions hypothesis, which is more conservative of the other two, is therefore preferred to provide most reliable predictions of CHF/CHFR for the ACRR. Additionally, previous experimental results, discussed further in the report, reveal that the global conditions hypothesis is the most accurate among these three hypotheses. Consequently, global conditions hypothesis predictions of CHF/CHFR were recommended to be used as the best estimate predictions. Table ES.1 lists the best estimate predictions of CHF and CHFR in the ACRR at 4 MWt.

**Table ES.1. Best estimate predictions of CHF and CHFR in the ACRR.
The CHFR is calculated for an operating power of 4 MWt.**

Parameter	Total Power Hypothesis		Local Conditions Hypothesis		Global Conditions Hypothesis	
	$T_{in}=20^{\circ}\text{C}$	$T_{in}=55^{\circ}\text{C}$	$T_{in}=20^{\circ}\text{C}$	$T_{in}=55^{\circ}\text{C}$	$T_{in}=20^{\circ}\text{C}$	$T_{in}=55^{\circ}\text{C}$
Reactor Power						
Corresponding CHF (MWt)	9.76	6.4	17.0	10.4	15.6	8.4
Critical Heat Flux Ratio	2.40	1.60	4.25	2.60	3.90	2.10

Chapter 1

INTRODUCTION

Critical heat flux (CHF) is a phenomenon that results in a build up of an insulating vapor layer on the surface of the heater. This vapor layer causes an abrupt reduction in the heat transfer rate and a simultaneous, precipitous rise in the surface temperature of the heater. This rise in the surface temperature could be enough to cause material failure of the heater.

The CHF (or the boiling crisis) phenomena can generally be classified into two broad categories depending on the vapor quality. The first, Departure from Nucleate Boiling (DNB), usually occurs at a low vapor quality typical of that encountered at high-pressure and/or high-coolant mass velocities. This type of CHF phenomenon, noted by a transition from nucleate boiling to film boiling, is associated with an abrupt increase in the surface temperature of the heater. Several conditions leading to the occurrence of the DNB type CHF have been proposed by Tong [1], and recently by Hewitt [2], Collier [3], and Katto [4]. The second category of the CHF phenomena is Dryout CHF, which is commonly referred to as Harwell's model because it was first postulated by Harwell scientists [5]. This type of CHF usually occurs at high vapor quality or void contents typical of those occurring in constrained geometries and at saturated or near saturated flow conditions. Prior to the boiling crisis, the surface of the heater is covered by a thin layer of liquid, while the bulk of the coolant flow consists of a mixture of vapor and entrained liquid droplets. The heat flow through the liquid film induces evaporation at the surface of the liquid layer, the thickness of which decreases as the heat flux increases. The thickness of this liquid film is also influenced by the phenomena of liquid droplet entrainment and deposition. When the heat flux is high enough to evaporate the liquid film, the surface dries out, causing an abrupt rise in the surface temperature of the heater. The values of the dryout type CHF as well as the surface temperature of the heater at CHF are lower than those for the DNB type CHF. The flow pattern observed in the dryout type CHF is generally an annular or an annular-mist type flow. Extensive reviews of the scenarios leading to the occurrence of the dryout type CHF have been presented by several authors [2-7]; however, there has been little direct experimental study of these scenarios for low-flow and low-pressure conditions similar to those in the Sandia ACRR.

Although many CHF correlations are available for multirod bundles and for annuli and tubes, these correlations were developed based on mostly high-flow rate and high-pressure data [8-11]. Consequently, using these correlations to predict CHF values at lower flow rates and pressures, as in the Sandia ACRR ($230\text{--}270\text{ kg m}^{-2}\text{ s}^{-1}$ and 0.143 MPa) would be inappropriate. Biasi et al. [8], have developed two CHF correlations; one was based on low-vapor exit quality data and the other was based on high-exit vapor quality data. These correlations covered a wide range of pressure ($0.27\text{--}14.2\text{ MPa}$) and mass fluxes ($100\text{--}600\text{ kg m}^{-2}\text{ s}^{-1}$) for various tube diameters and lengths ($D = 0.003\text{--}0.0375\text{ m}$ and $L = 0.2\text{--}0.6\text{ m}$). Similar to Biasi et al. [8], MacBeth [9,10] has developed two correlations, one for low-mass fluxes and the other for high-mass fluxes. The range of the water mass fluxes for these correlations varied from 13.6 to $12,232\text{ kg m}^{-2}\text{ s}^{-1}$. However, the low-mass flux correlation, which is of interest to the present work, employed only six CHF data points at 0.104 MPa ; the rest of the 372 data points were for a system pressure between 0.24 and 13.8 MPa . For subcooled flow conditions in tubes, annuli, and multirod bundles, Bernath [11] has developed a CHF correlation based on data taken at system pressures

ranging from 0.104 to 20.68 MPa and for coolant mass fluxes ranging from 750 to 5000 kg m⁻² s⁻¹. Because of their limited data base at low-flow, the applicability of the MacBeth and Bernath correlations to low-flow, low-pressure conditions is questionable.

Other research has shown that for a given inlet subcooling while the CHF increases precipitously with the coolant mass flow velocity at low mass flow rate, it increases at a much lower rate at higher coolant mass flow rates [3]. The coolant mass flux at the transition from the low- to the high-flow CHF regimes is independent of geometry. This transition was reported to occur in vertical tubes at about 200 kg m⁻² s⁻¹ [12], and in vertical annuli at about 180 kg m⁻² s⁻¹ [13]. The difference between these transition values of tubes and annuli is insignificant considering possible experimental errors and judgments involved in establishing the transition values. Another major difference between the low-flow and high-flow CHF regimes is that in the high-flow regime the CHF values strongly depend on the inlet subcooling; conversely in the low regime the CHF values are nearly independent of the inlet subcooling.

1.1 Low-Flow, Low-Pressure CHF Correlations

The only consistent CHF data for low-flow of water in vertical annuli at near atmospheric pressure were those reported by Rogers et al. [13], and Mishima and Ishii [14] and El-Genk et al. [15]. To the best of the author's knowledge, no CHF data are available for rod bundles at these operating conditions. Rogers et al. [13] collected CHF data for upward flow of water in vertical annuli (annulus ratios of 1.68, 1.91, and 2.31) for both subcooled and saturated conditions. The water mass fluxes ranged from 60 to 1200 kg m⁻² s⁻¹ with the majority of the data being collected at mass fluxes greater than 180 kg m⁻² s⁻¹; the CHF data were collected at water mass fluxes less than 180 kg m⁻² s⁻¹ in only one annulus, $\epsilon = 1.68$. In these experiments [13] the CHF occurred as a large vapor slug passed through the top of the heated section; however, the flow transition at CHF was not investigated. The CHF correlations developed by Rogers et al., based on the linearity exhibited by the data between the CHF and the inlet subcooling for mass fluxes higher than 180 kg m⁻² s⁻¹, were entirely empirical. Because the constants of the correlations were determined individually for each annulus, Rogers et al. correlations could not be applied to other annular geometries [13].

Mishima and Ishii [14] related their CHF data to the flow transition (churn-annular flow), which occurred at CHF. They identified the flow transition at CHF in two ways: by plotting the CHF values versus the equilibrium exit quality on a two-phase flow map and by visual observations. However, the data base obtained was limited since these experiments were confined to only one annulus, $\epsilon = 1.27$, and to very low water mass fluxes (from 0 to 40 kg m⁻² s⁻¹). Mishima and Ishii [14] correlated their CHF data using a drift flux model transition criterion from churn to annular flow and assuming that the void distribution coefficient in the annulus was the same as in a vertical tube. This assumption, which implies that the maximum void concentration occurs at the center of the tube, may not be applicable for flow in vertical annuli because of the unheated outer wall and the lack of geometrical symmetry of the void distribution in the annular gap.

Recently, El-Genk et al. [15,16] performed experiments using three vertical annuli (annulus ratio of 1.575, 1.72, and 2.0) to measure the critical heat flux for low-flow of water in vertical annuli near atmospheric pressure (0.118 MPa). CHF data are collected for water inlet subcooling ranging from 44 (or 182 kJ kg⁻¹) to 75 K (or 312 kJ kg⁻¹), and mass fluxes from 0 to 260 kg m⁻² s⁻¹. The CHF values varied from 155 kW m⁻² for zero inlet flow to a maximum of 1418 kW m⁻² for a water mass flux of 260 kg m⁻² s⁻¹. The flow transition at CHF varied with the annulus ratio and the water mass flux. While CHF in the smallest annulus ($\epsilon = 1.575$) always occurred at the transition from annular to annular mist flow, in the larger annuli ($\epsilon = 1.72$ and 2.0), the CHF occurred either at the transition from churn to annular flow or from slug to churn flow. The CHF data were classified and correlated according to the flow transition that occurred at CHF. Two CHF correlations were developed, one for the slug-churn and the churn-annular flow transitions and the other for the annular-annular mist flow transition.

In these experiments, the effects of the annulus ratio and the mass flux of the water and inlet subcooling on CHF as well as the flow transition at CHF values increased as the water mass flux or annulus ratio increased [15,16]. However, the effect of inlet subcooling on CHF was not distinguishable in the smaller annuli (annulus ratios of 1.575 and 1.72) and for the largest annulus ($\epsilon = 2.0$) at water mass fluxes less than 140 kg m⁻² s⁻¹. Conversely, in the largest annulus at higher mass fluxes, the data showed a distinct effect of inlet water subcooling on CHF; the CHF values increased as the inlet water subcooling increased. The results also showed that the CHF, which was of the dryout type, caused the liquid film at the top of the heated tube to evaporate, resulting in an abrupt rise in the surface temperature of the water. At CHF, the heater temperature varied within a narrow range and was generally independent of the annulus ratio; however, it increased as the water mass flux increased.

The heat flux at incipient flow chugging was also measured and found to vary from a minimum of about 40% of the CHF values in the smallest annulus (annulus ratio = 1.575) to a maximum of approximately 88% of CHF in the other larger annuli (annulus ratio = 1.72 and 2.0). Results also showed that the heat flux at incipient chugging increases as either the water mass flux or inlet subcooling increases [15,16]. The results are important to the operation and safety of the TRIGA type research reactors and the Sandia ACRR. Because the TRIGA and ACRR are cooled by low Reynolds number flow ($Re < 10,000$) and operate at near atmospheric pressure, hydrodynamic oscillations (or chugging) in the core before the occurrence of CHF are expected. These oscillations would induce possible power spikes as the vapor slugs formed in the core collapse and are replaced by cold water. Power spikes have been observed experimentally [J.S. Philbin, private communication, 1983] at ACRR at high inlet temperatures ($T > 328$ K) and powers greater than 4 MW. Negative power spikes corresponding to 10% of power occur about once in 10 s at the 4-MW power level and the frequency increases to about 8 per 10 s interval as the power is increased to 4.5 MW. The power initially goes sharply negative as bubbles form followed by an equally sharp positive increase as the bubbles collapse. In between the negative power spikes the power was steady. Such spikes may trigger the automatic shutdown system and scram the reactor well before the CHF condition is reached. Therefore, although determining the critical heat flux ratio is important to ensure the safe operation of these reactors, most likely CHF conditions will not be reached because flow chugging may induce positive power spikes and scram the reactor. According to the results of El-Genk et al. [15,16], such a reactor scram could occur at heat fluxes that are up to 40% less than the CHF value.

1.2 Applicability of UNM-CHF Correlation to the ACRR

Although the CHF correlation of El-Genk et al. [15,16], referred to as the UNM-CHF correlation hereafter, was specifically developed for operating conditions (low-water flow rate and near atmospheric pressure) similar to those in the ACRR, it cannot be directly applied to predict CHF/CHFR in the ACRR because of the differences in geometry, and the heating and operating conditions between the UNM-CHF experiments and the ACRR. The UNM-CHF data was obtained for forced circulation flow of water in uniformly heated vertical annuli, whereas the ACRR consists of a nonuniformly heated rod-bundle, which is cooled by natural circulation of water. In systems which are cooled by natural circulation, the buoyant force induced by the density differential across the core maintains the water circulation through the core. In such cores, increasing the power level increases the density differential, resulting in increased mass flow rate through the core [17]. Additionally, inlet subcooling has considerable effect on the mass flow rate through the core; the mass flow rate increases with decreasing inlet subcooling [18]. Thus the mass flow rate through the core is a strong function of both the reactor operating power and the inlet subcooling. Accurate knowledge of this mass flow rate through the core as a function of operating power level and inlet subcooling are important for accurate evaluation of ACRR thermal-hydraulics performance as well as the determination of CHF/CHFR. Attempts to directly measure the flow rate and coolant temperature in ACRR core were unsuccessful because of the bulky size of flow meters and the difficulty in centering the flow meter, the temperature probe, or both without either disturbing the flow or touching the fuel cladding. Also, the upward flow of the coolant caused the temperature probe to float freely in the measuring channel.

There is, therefore, a need to develop a thermal-hydraulics model for the ACRR that is capable of accurately predicting the mass flow rate through the core at various power levels as a function of inlet subcooling. The model should also predict the location of the maximum cladding temperature and the extent of subcooled boiling in the core at higher power levels. This latter requirement is especially important in light of the observed significant negative power fluctuations occurring at a nominal power level of 4 MWt which do not occur at the current operating power of 2 MWt [18]. Developing such a model and verifying its accuracy by comparing its predictions of coolant temperatures differential across the ACRR core with the in-core measurements is one of the objectives of this research.

Because of the differences in geometry and heating conditions between the ACRR and UNM-CHF experiments, an estimate of CHF in the ACRR using UNM-CHF correlation should take into consideration the effects of (a) bundle geometry, and (b) nonuniform heating. These effects are discussed in the following subsections.

1.2.1 The effect of bundle geometry on CHF has been thoroughly investigated, both experimentally and theoretically. MacBeth [19], Barnett [20] and Collier [21] provided a good review of these investigations. Initially MacBeth [19] and later Barnett [20] showed that for specified inlet and heating conditions, the total power that can be fed to an interior rod in a multirod bundle is very close (within 5%-15%) to that for an 'equivalent' annulus. An equivalent annulus is defined as an annulus that has the same equivalent diameter, D_h , and flow area per rod as those in the rod bundle (see Figure 1.1). In other words, the exit quality at CHF for an equivalent annulus would be expected to be approximately the same as that for the interior subchannel of a rod bundle.

Figure 1.1. Schematic of the equivalent annulus for an interior rod centered subchannel of the ACRR where the cross-sectional flow area per rod is the same [$D_h(\text{annulus}) = D_h(\text{bundle})$].

This observation was verified experimentally by Becker [22] for P/D less than 1.3 and pressures in the range of 0.3 and 0.64 MPa and mass fluxes ranging from 65 to 245 kg m⁻² s⁻¹. However, Becker pointed out that for P/D larger than 1.3 due to increased cross flow between subchannels, the bundle CHF data is usually larger than that corresponding to the equivalent annulus data. Additional confirmation of this trend is evident from the works of Silvestri [23] and Gaspari et al. [24]. In all cases the annulus CHF-correlations were found to provide reasonable agreement with the rod bundle data for $P/D < 1.2$ [19-24]. In view of these findings, it can be concluded that UNM-CHF correlation developed for annular geometries could be applicable to rod bundles with low P/D ratios (P/D of ACRR = 1.13). This conclusion is confirmed in Chapter 3 by comparing the UNM-CHF correlation predictions for CHF with the experimental data of Becker et al. [22] for a uniformly heated 7-rod bundle.

1.2.2 The effect of nonuniform heating on CHF is a topic under continuing investigation due to its application to power reactors. Numerous experimental and theoretical studies have been conducted in the past to devise consistent methodologies of applying CHF correlations developed for uniformly heated geometries to nonuniformly heated geometries [25-29]. Almost all of the suggested methodologies are based on one of the following hypotheses: (1) total power hypothesis, (2) local conditions hypothesis, and (3) global conditions hypothesis.

The total power hypothesis, proposed by Lee and Obertelli [25], suggests that the total power that can be fed to a geometry with nonuniform heating is the same as that for similar geometry with uniform heating. In other words, it states that the exit quality corresponding to the CHF conditions is independent of the axial heat flux distribution (see Figure 1.2). Mathematically, this hypothesis can be expressed as follows:

$$(q_{CHF}'')_{unif} = \frac{1}{L} \int_0^L \pi D (q_{CHF}'')_{non-unif} dZ \quad (1.1)$$

This hypothesis has no theoretical basis and was formulated solely based on Lee and Obertelli's experimental observation that the exit qualities at CHF are nearly the same regardless of the heat flux distributions. However, later investigation by Barnett [26] and Swenson et al. [27] have shown that the validity of total power hypothesis is limited to smoothly varying heat fluxes with very small nonuniformity ($q_{max}'' / q_{min}'' < 1.2$). For example, for skewed cosine heat flux profiles (similar to that in ACRR), Barnett [26] showed that the total power hypothesis predictions of CHF are up to 40% lower than measurements. Similar findings have led Lee [28] to suggest usage of the total power hypothesis to obtain a conservative lower bound for CHF. Because of these drawbacks, the total power hypothesis has become somewhat obsolete and rarely mentioned in the recent literature.

The total power hypothesis implicitly suggests that the local conditions (heat flux and steam quality at the location of CHF) have little effect on the onset of CHF, instead the CHF is governed only by the upstream and downstream conditions. Failure of the total power hypothesis has been interpreted by some researchers as the dominance of the local conditions at CHF over the upstream and downstream memory effects. This argument, with limited phenomenological understanding of the onset of CHF, led to the formulation of the local condition hypothesis. The local condition

Uniform Heat Flux

$$(\ddot{q}_{CHF})_{unif} = \frac{1}{L} \int_{-L/2}^{+L/2} (\ddot{q}_{CHF})_{non-unif} dz$$

Non-Uniform Heat Flux

A. FLUX PLANE

B. QUALITY PLANE

Figure 1.2. A schematic description of the total power hypothesis as postulated by Lee and Obertelli [25].

hypothesis states in essence that it is only the local heat flux and local quality at the location of CHF that control the onset of CHF; and the upstream history is not important (see Figure 1.3). It is immaterial whether the heat flux is distributed uniformly or nonuniformly and whether the inlet flow conditions are saturated or subcooled. This hypothesis has been the basis for numerous correlations that are still in use [29,30,31]. However, Barnett [32] reported that his correlation, developed based on the local conditions hypothesis, significantly overestimates CHF for nonuniform geometries (by up to 30%). A similar trend was also reported by several other investigators [21,33].

The limitation of the local conditions hypothesis has become more and more evident as the CHF data for different axial heat flux distributions became available, compiled, and analyzed. The failure of the local condition hypothesis to accurately predict the CHF can be clearly seen in Figure 1.4, which presents CHF data for Freon-114 flowing in inner heated vertical annuli with various heat flux profiles [34]. The fact that the CHF data in Figure 1.4, for various heat flux profiles, did not collapse to exhibit an unique functional relationship between CHF and the local quality that is independent of heat flux distribution, demonstrates the failure of the local conditions hypothesis. Several modifications have been suggested to improve this hypothesis, but with limited success.

The failure of the local conditions hypothesis to account for the effect of the axial flux profile on CHF has led to the realization that the 'upstream history,' also referred to as 'the upstream memory effect,' could have significant effect on CHF. Figure 1.4 illustrates the strong effect of upstream history on CHF. This realization that the CHF is governed not only by local conditions, but also by the upstream conditions was the basis of various integral concepts suggested thereafter. These integral concepts basically combine the total power hypothesis, which argues that only the upstream and downstream conditions are important, and the local conditions hypothesis, which assumes that only the local conditions control the onset of CHF. Of these integral concepts Tong's F-factor method [35] and CISE - Global conditions hypothesis [36] are the most widely used concepts. Although in most cases they both yield very similar results, evaluation of CHF based on the F-factor method is more complex. Owing to the complexity of the F-factor method, the global conditions hypothesis became more prevalent.

The global conditions hypothesis states that there exists a unique relationship between the local quality at CHF location, referred to hereafter as critical quality, x_c , and the boiling length up to the CHF location. The boiling length from the T_{sat} location in the column to the CHF location is referred to as L_{BC} (see Figure 1.5). Moreover, this relationship is independent of the heat flux profile, but depends on the inlet mass flux and the pressure. Inherent in the global conditions hypothesis is the assumption that the critical boiling length encompasses all the upstream effects. Figure 1.6 demonstrates the success of the global conditions hypothesis in allowing for the axial heat flux effects on CHF. Note that the data plotted in Figure 1.6 is identical to that presented previously in Figure 1.4. Although this result was first reported by investigators at CISE [36,37], and its success has been confirmed by several investigators. Figure 1.7 is an example of British CHF data [38] plotted according to global hypothesis in $x_c - L_{BC}$ plane. Similarly as shown in Figure 1.8, the axial heat flux effect on CHF for rod bundles is well accounted for by the global conditions hypothesis. In view of these findings, it is evident that the global conditions hypothesis best accounts for the effect of nonuniform axial heat flux profiles on CHF. However, in the

A. FLUX PLANE

B. QUALITY PLANE

Figure 1.3 A schematic description of the local conditions hypothesis.

Figure 1.4. The CHF data for Freon-114 flowing in inner heated vertical annuli plotted according to the local conditions hypothesis.

$$x_c = F(L_{Bc})$$

F is an unique functional relationship independent of heat flux distribution.

Figure 1.5. A schematic description of the global conditions hypothesis. Extracted from Lahey and Moody.

Figure 1.6. The CHF data for Freon-114 flowing in inner heated vertical annuli plotted in x_c - L_{BC} plane to show the effectiveness of the global conditions hypothesis.

Figure 1.7. The British CHF data for water flow in tubes plotted in the x_c - L_{BC} plane.

Figure 1.8. The GE CHF data for water flow in tubes plotted in the x_c - L_{BC} plane.

present study all the three hypotheses are employed to evaluate the CHF/CHFR in ACRR for comparison and completeness purposes.

1.3 Objectives of the Present Study

The primary objective of this study is to obtain a best estimate predictions of CHF/CHFR in the ACRR at 4 MWt. The UNM-CHF correlation, specifically developed for low-flow and near atmospheric pressure conditions in uniformly heated critical annuli, is employed to obtain these predictions. As pointed out earlier, calculating the CHF in the ACRR with the UNM-CHF correlations involved two subtasks:

1. The development and verification of a thermal-hydraulics model that is capable of predicting the mass flow rate through the ACRR core at various power levels. This model is also capable of predicting coolant and cladding temperatures, and the extent of subcooled boiling in the core at various operating power levels and inlet subcoolings.
2. The evaluation of CHF/CHFR in ACRR using the UNM-CHF correlation in conjunction with (a) the total power hypothesis, (b) the local conditions hypothesis, and (c) the global conditions hypothesis. In these calculations, the mass flow rate through the core was determined from the model developed in subtask 1.

The following chapter presents a brief summary of the results of the UNM-CHF experiments along with a list of the UNM-CHF correlations. Chapter 3 deals with the development and verification of the 1-D thermal-hydraulics model for the ACRR and its application to evaluate the ACRR thermal-hydraulics performance at 2 MWt and 4 MWt power levels. The penultimate chapter, Chapter 4, presents the methodology used to predict the CHF/CHFR in the ACRR at 4 MWt. The last section of Chapter 4 presents a summary of the results and makes a recommendation for the most appropriate CHF and CHFR values for the ACRR.

Chapter 2

THERMAL-HYDRAULIC MODEL FOR ACRR

The primary objective of this chapter is to develop a thermal-hydraulic model that is capable of accurately predicting the flow rate through the ACRR core as a function of the reactor operating power level and inlet subcooling. In addition, the model could be used to evaluate the thermal hydraulics performance of ACRR at the present operating power level of 2 MWt and the proposed higher power level of 4 MWt. The following sections describe the model development and verification, followed by a discussion of the model predictions of the coolant mass flow rate in the ACRR as a function of operating power and inlet subcooling. These predictions are utilized in Chapter 4 to obtain best estimate values of CHF and CHFR in the ACRR.

2.1 Model Formulation

As mentioned earlier, in the TRIGA type reactors and the Sandia ACRR, the buoyant force induced by the density differential across the core maintains the water circulation through the core (see Figure 2.1). Countering this buoyant force are the pressure losses due to the contraction and expansion at the entrance and the exit of the core as well as the acceleration and friction pressure losses in the flow channels. For simplicity, the reactor core is modeled using a single flow channel (or subchannel) approach. Such a simplification allows for determination of the flow rate and the coolant and cladding temperatures in any channel in the core while minimizing the amount of computations. A shortcoming of this simplification is that it neglects the cross flow between neighboring subchannels in the core that is being induced by the nonuniform radial power profile. However, various experiments [22-24] revealed that this cross flow is negligible for tightly packed geometries such as ACRR ($P/D = 1.12$). Furthermore, because the ultimate effect of cross flow mixing is to increase CHF, its neglect through the use of subchannel approach is expected to result in conservative estimate of CHF. In view of these discussions, the one-dimensional model used in the present study is justifiable; the validity of this proposition will be examined in the later parts of this chapter.

The geometric and operating parameters of a typical ACRR single flow channel at a reactor power of 2 MWt are listed in Table 2.1 and a schematic of the channel is presented in Figure 2.2. As shown in Figure 2.2, the total pressure losses in the downcomer and across the flow channel is equal to the sum of the pressure losses at the entrance, $(\Delta P)_{ent}$, and the exit, $(\Delta P)_{exit}$, of the flow channel, the friction pressure losses in the lower nonfuel region, $(\Delta P)_{frict}^l$, in the fuel region, $(\Delta P)_{frict}^f$, in the upper nonfuel region, $(\Delta P)_{frict}^u$, and in the downcomer, $(\Delta P)_{dc}$, as well as the pressure losses due to acceleration of the flow through the channel, $(\Delta P)_{acc}$. During steady-state operation the total pressure loss in the flow channel is balanced by the pressure gain due to buoyant forces, $(\Delta P)_{buoy}$. The momentum balance in the flow channel can therefore be written as follows:

$$\begin{aligned} (\Delta P)_{dc} + (\Delta P)_{ent} + (\Delta P)_{exit} + (\Delta P)_{frict}^l + (\Delta P)_{frict}^f + (\Delta P)_{frict}^u + (\Delta P)_{acc} \\ = \Delta P_{buoy} \end{aligned} \quad (2.1)$$

The individual terms in Equation (2.1) are defined and discussed in the following subsections.

Figure 2.1. A schematic of the ACRR core.

Table 2.1. Geometric and Operating Parameters of ACRR at a power level of 2 MWt.

Parameter	Value
P/D	1.113
Heated Length (m)	0.502
Heated Diameter (cm)	3.780
Power per Rod (kW)	10-16
Linear Power (kW/m)	20-32
Av. Heat Flux (W/cm ²)	16.8-26.9
Max. Rayleigh Number, Ra	8.0E+07
Reynolds Number, Re	3E+3 - 5E+3

Figure 2.2. A schematic of two adjacent flow channels in the ACRR core.

2.1.a Entrance and Downcomer Pressure Losses

This term, which accounts for pressure loss due to the contraction of the flow area at the core entrance as well as the pressure loss in the downcomer can be expressed in terms of the velocity head at the bottom end fitting as

$$(\Delta P)_{dc} + (\Delta P)_{ent} = K_{ent,dc} \frac{\dot{m}^2}{2\rho_{in}A_c^2g_c} \quad (2.2)$$

The water density, ρ_{in} , in this equation is evaluated at the inlet temperature and, therefore, is a function of inlet subcooling. Because there is no experimental data available for the pressure loss coefficient $K_{ent,dc}$, its value was taken to be equal to 3.58; this value provides the best agreement between model predictions and the in-core measurements of the temperature differential across the core at all power levels and inlet subcoolings.

2.1.b Exit Pressure Losses

This term, which takes into account pressure loss due to the expansion in the flow area as the water exits the core through the upper end plate, is also evaluated as a fraction of the velocity head at the top end fitting as

$$(\Delta P)_{exit} = K_{exit} \frac{\dot{m}^2}{2\rho_{ex}A_c^2g_c} \quad (2.3)$$

In Equation (2.3), the water density is evaluated at the bulk temperature of the water at the exit of the channel. A value of 3.0 for the exit loss factor, K_{exit} , was determined in a similar manner as K_{ent} .

2.1.c Friction Pressure Losses

For the purpose of accurately calculating the friction pressure losses in the ACRR core region, the flow channel is divided into three axial sections; a lower nonfuel region, an active fuel region and an upper nonfuel region (see Figure 2.2). Although the geometrical parameters in all three regions are identical, the flow condition would differ, and so would the relations for calculating the pressure loss in each region. In the friction pressure loss calculations, the water temperatures in the upper and lower nonfuel regions are assumed to be equal to the measured exit and inlet temperatures, respectively owing to the expected thorough mixing of the coolant in these regions. The friction pressure losses in the various core regions are determined as discussed in the following subsections.

2.1.d Lower and Upper Nonfuel Regions

The friction pressure losses in the upper and lower nonfuel regions of the core are given, respectively, by the following equations:

$$(\Delta P)_{\text{frict}}^l = f_m \frac{H^l}{D_e} \frac{\dot{m}^2}{2\rho_{\text{in}} A_c^2} \quad \text{and} \quad (2.4)$$

$$(\Delta P)_{\text{frict}}^u = f_m \frac{H^u}{D_e} \frac{\dot{m}^2}{2\rho_{\text{ex}} A_c^2}. \quad (2.5)$$

In Equations (2.4) and (2.5) the equivalent hydraulic diameter of the flow channel for an infinite triangular lattice, D_e is given as

$$D_e = D \left[\frac{2\sqrt{3}}{\pi} (P/D)^2 - 1 \right]. \quad (2.6)$$

The values of Moody's friction coefficient, f_m , in Equations (2.4) and (2.5) are calculated using the applicable correlation from Table 2.2.

Table 2.2. Correlation of Moody's friction factor

Range of Re	Correlation
< 2100	$f_m = 64/\text{Re}$
2100 - 15,000	$f_m = 2J_H$, and $J_h = \text{St Pr}^{0.66}$
>15,000	$f_m = 0.186/\text{Re}^{1/5}$

2.1.e Active Fuel Region

In a typical ACRR flow channel, the entire fuel element is cooled by single-phase convection as long as the maximum wall temperature is kept below that required to initiate boiling. However, at higher power levels the entrance and the exit regions of the core, where the heat fluxes are the lowest, are cooled by single-phase convection; in the central region, where the axial heat flux is the highest, the mode of heat transfer is predominantly subcooled boiling. In general, the friction losses in the active core region can be evaluated as

$$(\Delta P)_{\text{frict}}^f = \int_0^{H^f} f(z) \frac{\dot{m}^2}{2\rho(z) D_e A_c^2} dz, \quad (2.7)$$

where the value of the friction coefficient, $f(z)$, depends on the mode of heat transfer at the axial location z ; either single-phase convection or subcooled boiling. For single-phase convection, the value of $f(z)$ is obtained by multiplying Moody's friction factor, f_m , from Table 2.2 by the quantity $(\mu_w/\mu_b)^{0.6}$ to account for the change in the water's viscosity between the wall and the coolant bulk temperatures [44]. Therefore, for single-phase convection, the friction factor is expressed as

$$f(z) = f_m(z) \left[\frac{\mu_w}{\mu_b} \right]^{0.6} \quad (2.8a)$$

For subcooled boiling, the friction coefficient, $f(z)$, is calculated using the Reynolds analogy as suggested by Sebersky and Mulligan [45]. They reasoned that bubbles forming at the cladding surface during subcooled boiling are analogous to wall roughness in their effect on both the thermal and hydrodynamic boundary layers, and therefore, the Reynolds analogy between heat and momentum transfer should be applicable to the subcooled boiling. This hypothesis has been experimentally verified by Sebersky and Mulligan [45] for tube geometry and by Jordan and Leppert [46] for annular geometry at near atmospheric pressure. Therefore, $f(z)$ for subcooled boiling is given as

$$f(z) = \frac{8h_{sb}A_c}{C_p \dot{m}} \quad (2.8b)$$

Because the water properties in Equations (2.7), (2.8a), and (2.8b) are temperature dependent, and hence vary along the length of the channel, Equation (2.7) cannot be solved analytically; instead an explicit forward marching numerical technique was used. In this technique the active fuel region is divided into small axial control volumes of length Δz . The integrand in Equation (2.7) is then evaluated over each control volume and the results are added to give the total friction pressure losses along the entire active fuel region. To evaluate the integrand in Equation (2.7) over any control volume, i , a knowledge of the coolant bulk and the cladding surface temperatures in this volume is necessary. The coolant bulk temperature in the i -th control axial control volume of right Δz is evaluated from the heat balance equation:

$$T_{ib} = T_b^{i-1} + \frac{q'(z)\Delta z}{\dot{m}C_p^{i-1}}, \quad (2.9)$$

where T_{ib} and T_b^{i-1} are the water bulk temperatures in the axial control volumes i and $i-1$, respectively. The value of the linear heating rate in the i -th control volume, $q'(z)$, is determined from the ACRR axial power profile which was based on neutron flux measurements [17]. This axial power profile can be represented, with an accuracy of 99.5 %, by a set of polynomial equations as delineated in Figure 2.3.

2.1.f Acceleration Pressure Losses

Since the flow in the ACRR core is almost isothermal in both the upper and the lower nonfuel regions, the acceleration pressure loss occurs only in the active fuel region of the core. This pressure loss can be expressed as

$$(\Delta P)_{acc} = \frac{\dot{m}^2}{A_c^2} \left[\frac{1}{\rho_{ex}} - \frac{1}{\rho_{in}} \right] \quad (2.10)$$

Figure 2.3. Comparison of the ACRR axial power profile as determined from in-core measurements with the cosine profile.

Equations (2.2) through (2.10) were used to calculate the total pressure losses in the core and the downcomer, as indicated on the left hand side of Equation (2.1).

2.1.g Pressure Gain Due to Buoyancy

Because the water in the ACRR reactor tank outside the core region (downcomer) is subcooled, the density in the downcomer, ρ_{in} , is greater than the average water density, ρ_{ave} , in the core. The water temperature in the downcomer is approximately equal to that of the water entering the core. This density differential (buoyancy) between the water in the downcomer and that in the core induces a driving pressure $(\Delta P)_{buoy}$, which is responsible for the circulation of water through the core. At steady state, the pressure gain due to buoyant force equals the total pressure losses in the core and the downcomer. The driving pressure in the ACRR core can be expressed as

$$\begin{aligned} \Delta P_{buoy} = & \text{(Hydrostatic pressure in the downcomer)} \\ & - \text{(Hydrostatic pressure in the core flow channel)} \\ & - \text{(Hydrostatic pressure due to the chimney effect, if any).} \end{aligned} \quad (2.11)$$

This equation is expressed mathematically as follows:

$$(\Delta P)_{buoy} = [\rho_{in} H_c - \rho_{ave} H_c] g + \rho_{ex} H_{ch} g. \quad (2.12)$$

Recent temperature measurements above the ACRR core showed that thorough mixing of the water occurs within the first few centimeters above the top of the core, resulting in a uniform water temperature above the upper grid plate. As a result, the chimney term was eliminated from Equation (2.12) for the purpose of predicting the thermal hydraulic performance of the ACRR core. Equation (2.12) thus becomes

$$(\Delta P)_{buoy} = [\rho_{in} - \rho_{ave}] H_c g. \quad (2.13)$$

In this equation the average water density in the flow channel is obtained from the following relation:

$$\rho_{ave} H_c = \rho_{in} H^l + \rho_{ave}^f H^f + \rho_{ex} H^u, \quad (2.14)$$

where the value of average water density in the active fuel region, ρ_{ave}^f , is determined using the following equation:

$$\rho_{ave}^f = \frac{1}{H^f} \int_0^{H^f} \rho(z) dz. \quad (2.15)$$

After evaluating $\rho(z)$ in each control volume along the length of the active fuel region, ρ_{ave}^f , is determined by integrating Equation (2.15) numerically using Simpson's "1/3" rule. The value of

ρ_{ave}^f is then substituted into Equation (2.13) to determine the pressure gain due to buoyancy. These substitutions give the following expression for $(\Delta P)_{buoy}$:

$$(\Delta P)_{buoy} = [H^f(\rho_{in} - \rho_{ave}^f) + H^u(\rho_{in} - \rho_{ex})] g. \quad (2.16)$$

Substituting Equations (2.2)-(2.6), (2.10), and (2.16) into Equation (2.1) gives a complete expression of the momentum balance in the flow channel of choice in terms of the water mass flow rate and the inlet and exit temperatures. The mass flow rate in the flow channel can be expressed as

$$\dot{m} = q / \int_{T_{in}}^{T_{ex}} C_p dT. \quad (2.17)$$

The coupling of Equations (2.9) and (2.17) gives also the axial variation in the water bulk temperature in the flow channel as a function of the average fuel rod power in the ACRR core. This power and that for the fuel rod in the central channel can be given as

$$q'_{av} = Q / (N \cdot H^f) \quad \text{and} \quad (2.18a)$$

$$q'_{cc} = q'_{av} \cdot \gamma. \quad (2.18b)$$

The cladding temperature in any axial control volume, i , at an axial distance z , measured from the bottom of the active fuel region, can be expressed in terms of the rod power and the water's bulk temperature in this volume as follows:

$$T_c^i = T_b^i + \left(\frac{q'}{\pi D h^i} \right). \quad (2.19)$$

In Equation (3.19) the heat transfer coefficient, h^i , depends on the mode of heat transfer at the cladding surface; either single-phase convection, developing subcooled boiling, or fully developed subcooled boiling (see Figure 2.4). The heat transfer coefficient in each of these flow regimes is determined as discussed in the following section.

2.2 Evaluation of Heat Transfer Coefficient

Once the coolant bulk temperature is determined, the corresponding cladding temperature can be evaluated using the proper value of the heat transfer coefficient (see Table 2.3). The single-phase convective heat transfer correlations, h_{sp} , listed in Table 2.3 are developed from heat transfer data of a 7-rod bundle with pitch-to-diameter ratios of 1.25, 1.35, and 1.5 and for Reynolds number values between 80 and 40,000 [47,48]. In this research, which was conducted as a part of the UNM-SNL-GA joint research program [40], the forced flow heat transfer data in the rod-bundle was classified into two basic regimes: a turbulent flow regime, where $Re \geq Re_{tr}$, and a laminar flow regime, where $Re \leq Re_{tr}$. The values of Reynolds number at transition between two regimes, Re_{tr} , was found to vary linearly with P/D . Although the ACRR is cooled by natural

Figure 2.4. Schematic representation of the heat transfer regimes in the ACRR.

Table 2.3. Heat transfer correlations for single-phase convection and subcooled boiling used in the model.

Heat Transfer Regime	Correlation Used	Range of Parameters	Ref.
Single-Phase convection	$Nu_{sp}^* = 0.272 Raq^{0.25}$ (or) $Nu_{sp}^{\#} = (0.026(P/D) - 0.006) Re^{0.8} Pr^{1/3}$	$T_W^+ \leq 383 \text{ K}$ $Re_T/10^4 \geq 1.32(P/D) - 1.43$	[47-50]
Subcooled Boiling Heat Transfer	$h_{sb} = q / (T_W - T_b)$, where • $T_W = T_{sat} + (72/e^{P/1260})(q/10^6)$ • $q(\text{BTU}/\text{ft}^2\text{-hr})$, $T_W(^{\circ}\text{F})$, $T_{sat}(^{\circ}\text{F})$ and $P(\text{Psi})$	$T_W \geq 393 \text{ K}$:	[51]

* Natural convection correlation,

Turbulent forced convection correlation, and

+ Saturation temperature in the ACRR core in Albuquerque, NM.

convection, both turbulent flow and the natural convection correlations were found to give similar predictions of the heat transfer coefficient for the ACRR conditions [47,48]. Because the correlations listed in Table 2.3 were based on a large data base covering a wide range of P/D (1.25 to 1.50), they are expected to be applicable to the ACRR core ($P/D = 1.113$).

In the ACRR core, single-phase convection is assumed to be the heat transfer mechanism when the calculated cladding temperature is below the saturation water temperature (~ 383 K in the ACRR core) and that predicted using the subcooled boiling heat transfer correlation [51]. Once the cladding temperature is more than 10 K above saturation temperature ($T_w > T_{sat} + 10$ K), fully developed subcooled nucleate boiling is assumed to be the heat transfer mechanism; in Figure 2.4 this region extends beyond point C. Developing subcooled boiling, the region between points A and C in Figure 2.4, is assumed to occur when $T_{sat} < T_w < (T_{sat} + 10$ K). In this region, the heat transfer coefficient, h_{dsb} , is obtained by superimposing the single-phase convection and the fully developed subcooled boiling, h_{sb} , heat transfer coefficients, as suggested by Bergles and Rohsenow [51]:

$$h_{dsb} = \left[h_{sb}^2 + h_{sp}^2 \right]^{1/2} . \quad (2.20)$$

2.3 Numerical Scheme

Equations (2.1) through (2.20) are solved using an iterative numerical scheme. First, an arbitrary mass flow rate through the flow channel is assumed for a given inlet temperature and fuel rod power (or reactor power level). The friction pressure losses at the core entrance and the lower nonfuel region are determined using Equations (2.2) and (2.4). In order to evaluate the friction pressure losses in the active fuel region, this region is divided into many equal height control volumes (100 for most runs) and the bulk temperature in each control volume is determined using Equation (2.9), assuming that C_p is the same as that calculated for the control volume located directly upstream. The axial power distribution curve in the ACRR core (Figure 2.3) is used to determine the local power in each control volume, q_i . Once the water bulk temperature is calculated, the thermophysical properties in the control volume are updated and stored for later use. Finally, the appropriate heat transfer correlation from Table 2.3 and/or Equation (2.20) is used to determine the heat transfer coefficient. The value of the heat transfer coefficient is substituted in Equation (2.19) to calculate the local cladding temperature. If the local cladding temperature is greater than the saturation temperature (i.e., $T_c^i > T_{sat}$), Equation (2.8b) is used to evaluate Moody's friction factor for the control volume; otherwise, Equation (2.8a) is used. This process continues in the other control volumes in the core until the top of the active fuel region is reached. Simpson's "1/3" rule is then used to calculate the total frictional losses over the entire active fuel region using Equation (2.7).

The average water density in the active fuel region of the flow channel is determined using Equation (2.15). Subsequently, the values of the pressure loss and gain terms in Equation (2.1) are evaluated using Equations (2.3) - (2.5), (2.10), and (2.16), respectively. The final step of this iteration is to check the equality of Equation (2.1). If this equality is satisfied within a preset error margin (10^{-3}), then the calculated mass flow rate is taken as the actual flow rate through the channel. Otherwise, the value of the mass flow rate is decreased or increased depending on

whether the left hand side of the Equation (2.1) is larger or smaller than the right hand side, respectively. When the solution converges the calculated results include the axial distributions of the water and the cladding temperatures and the water mass flow rate in the flow channel. Generally the numerical solutions require less than 10 iterations to converge.

To verify the accuracy of the thermal-hydraulic model, as well as validate the assumptions introduced during the model development, the model predictions are compared in the next section with in-core temperature measurements in the ACRR, at different reactor power levels.

2.4 Model Verification

As pointed out earlier, in-core measurements of the water flow rate and temperature in the ACRR core are difficult to perform. However, the water inlet and outlet temperatures in the core can easily be measured at various operating power levels. The measured temperature differentials, ΔT , in the ACRR are compared with the model's predictions at the same-steady state power levels. As Figures 2.5 through 2.7 indicate, there is a good agreement ($\pm 10\%$) between the model predictions and the measured values of ΔT in the ACRR core at 2.0, 3.25, and 4.0 MWt, respectively. These figures also show that for a given water inlet temperature, ΔT increases as the reactor power level increases. Conversely, at a given reactor power level, increasing the water inlet temperature causes ΔT to decrease, but increases the mass flow rate (see Figure 2.8). This increase in the water flow rate occurs because an increase in the inlet temperature reduces the average water density in the core, increasing the buoyant forces and consequently the water circulation rate. The excellent agreement between the model predictions and the in-core temperature measurements demonstrates the soundness of the modeling approach as well as of the validity of the assumptions introduced during the model development.

2.5 Thermal-Hydraulics Performance of ACRR at 2 and 4 MWt

Figure 2.8 presents the calculated water mass flow rate in the ACRR as a function of the core inlet temperature for operating power levels of 2.0 and 4.0 MWt. While the mass flow rate increases with both operating power and inlet water temperature, it is higher in the hottest channel than in the average channel of the core. As indicated earlier, this increase in the mass flow rate through the core while increasing the inlet temperature can be attributed to the lower average water density in the core and thus the higher driving pressure due to buoyancy.

Figure 2.9 presents the calculated cladding temperature in the average channel in the ACRR as a function of the axial distance from the bottom of the active fuel region and the reactor operating power. As shown in this figure, at a reactor power of 0.5 MWt, where the heat transfer in the core is due solely to single-phase convection, the axial distribution of the cladding temperature resembles that of the axial power profile (see Figure 2.3). The peak cladding temperature, which is below the water saturation temperature, occurs at 0.22 m from the bottom of the active fuel region. However, at an operating power of 1 MWt, the cladding temperature in the bottom segment ($z = 0.0$ to 0.12 m) and the top segment ($z = 0.32$ to 0.52 m) of the active fuel rod length increased and decreased, respectively, as demanded by the power profile. In these regions, where

Figure 2.5. Comparison of the model predictions for temperature differential across the core with in-core measurements at 2 MWt.

Figure 2.6. Comparison of the model predictions for temperature differential across the core with in-core measurements at 3.25 MWt.

Figure 2.7. Comparison of the model predictions for temperature differential across the core with in-core measurements at 4 MWt.

Figure 2.8. Model predictions of the water flow rates through the core as functions of inlet water temperature at 2 and 4 MWt.

Figure 2.9. Axial distribution of the cladding temperature in the average flow channel of ACRR at 0.5, 1.0, 2.0, and 4.0 MWt.

the heat flux is relatively low, the heat transfer from the fuel rod cladding to the coolant was due either to single-phase convection or developing subcooled boiling, while in the middle region ($z = 0.12$ to 0.32 m), where the heat flux is relatively high, the heat transfer was due solely to subcooled boiling. Because the heat transfer coefficient for subcooled boiling is much higher than that for single-phase convection, the cladding temperature in the middle region of the active fuel length ($z = 0.12$ to 0.32 m) remained relatively flat with axial location (see Figure 2.9).

Figure 2.10 presents a heat transfer regime map of the ACRR in terms of the extent of the subcooled boiling region along the active fuel rod length and the operating power for water inlet temperatures of 293 and 333 K. As shown in this figure, subcooled boiling in the average flow channel ($\gamma = 1$) in the ACRR begins at about 0.7 MWt and the extent of subcooled boiling along the fuel rod increases with reactor power. Similar heat transfer regime map for central channel can be obtained by dividing the y-ordinate (rod power) by the radial peaking factor ($\gamma = 1.4$). At reactor powers above 1.9 MW, the entire length of the active fuel region is cooled by subcooled boiling, but the maximum cladding temperature remains below 400 K, which is only 20 K above the saturation temperature of water in the ACRR (383 K).

These results confirm the observation of subcooled boiling in the ACRR core at 2.0 MWt. At this power level, the exit core temperature increases from 336 to 358 K as the inlet core temperature increases from 295 to 328 K (see Figure 2.5). Because the highest exit core temperature of 358 K is only about 25 K below saturation, the vapor bubbles forming at the cladding surface at 2 MWt power level are expected to collapse shortly after they detach from the cladding. Consequently, subcooled boiling in the ACRR at 2.0 MWt does not contribute to the formation of vapor slugs nor introduce negative reactivity feedback causing oscillations in the reactor power. However, at a power level of 4.0 MW and water inlet temperature of 328 K, the subcooled boiling in the ACRR core introduces negative power overshoots causing noticeable oscillations in the reactor power [18]. Such oscillations in the reactor power may be attributed to the presence of vapor slugs at the exit of the core. In this case, the formation and release of the vapor slugs at the exit of the core cause hydrodynamic instability of the coolant in the core and consequently oscillation of the reactor power. While the presence of vapor in the core due to boiling reduces the reactor power due to the negative void coefficient of reactivity, the release of the vapor bubbles through the core top support plate causes a flow incursion of the cold water through the core inlet. This incursion of cold water introduces a positive reactivity feedback, causing the reactor power to overshoot. This may qualitatively explain the observed power and reactivity oscillations in the ACRR at a power level of 4.0 MWt. These oscillations were effectively reduced as the core inlet temperature was decreased, and they disappeared at a core inlet temperature of 298 K. At this inlet temperature, the water exit core temperature was almost 22 K below saturation, causing the vapor bubbles to collapse as they detached from the cladding surface, hence resulting in no oscillations of the reactor power.

Because the power oscillations in the ACRR were observed when the exit water temperature was slightly subcooled or saturated, the reactor power levels causing the exit water temperature to reach saturation were calculated. The results are plotted in Figure 2.11 as a function of the core inlet temperature at reactor operating power of 2.0 and 4.0 MWt. As indicated in Figure 2.11, at 293 K inlet temperature (~ 90 K subcooling) the rod power for saturation exit temperature is about

Figure 2.10. Heat transfer regime for the average flow channel of the Sandia ACRR.

Figure 2.11. Model prediction of power per rod corresponding to saturation exit conditions in the ACRR.

3 times higher than that in the hottest channel of the core at 4 MWt, while it is only about 60% higher at the 328 K inlet temperature (55 K subcooling). These estimates of the fuel rod power in the ACRR for a saturation exit temperature demonstrate the strong effect of the inlet temperature on potential power oscillations in the ACRR core.

At power levels higher than those corresponding to the saturation exit conditions in the ACRR, the thermodynamic equilibrium vapor exit quality becomes positive. Because of low-flow and low-pressure conditions in the ACRR, even a small increase in the quality results in a substantial increase in the vapor void fraction at the exit of the core. As reported by several investigators, voiding the channel at low-pressures leads to hydrodynamic instabilities and induces coolant excursions through the inlet and exit of the flow channel [14,50]. In the UNM-CHF annuli experiments, the incipience of such instabilities has been found to occur at a heat flux that is as low as 40% of CHF. In this context, it is important to note that potential power oscillations and negative power overshoots may activate the reactor scram well before reaching the critical heat flux.

2.6 Mass Flow Rate at CHF

The presence of the hydrodynamic instabilities introduces uncertainties in the frictional pressure drop estimations and, hence, in the calculated mass flow rate in the ACRR core at power levels higher than those corresponding to the saturation exit conditions. As discussed earlier, the mass flow rate through the core is determined from the pressure balance expressed by Equation (2.1). Formation of large voids in the core decreases the average density of the coolant in the channel and thus enhances the buoyant forces. As a result the mass flow rate through the core is expected to increase with increasing power levels. But, countering this increase in buoyant forces is the frictional pressure drop introduced by two-phase flow in the channel. Evaluating the frictional pressure drops in two-phase flow channel experiencing flow instabilities is difficult because of the lack of applicable correlations. Although the frictional pressure losses in the flow channel are expected to increase, the experimental results of Mishima and Ishii [14] for natural circulation conditions have indicated that the pressure gain due to the increase in buoyant forces dominates, resulting in a net increase in the mass flow rate through the core. The measured mass flow rate through the core in their experiments [14] increased with power until the onset of CHF.

It is not possible to accurately predict the water mass flow rate in the ACRR for power levels beyond those for incipient chugging. Therefore, this research made a conservative assumption to use the mass flow corresponding to the saturation exit conditions to be the same through the core at CHF. This assumption then neglects the increase in mass flow rate at powers higher than those corresponding to the saturation exit conditions. This assumption is expected to provide a conservative estimate of CHF in ACRR, which is preferable for safety considerations.

Figure 2.12 presents the model predictions of the mass flow rate through the ACRR core for saturation exit conditions, $\dot{m}_{\text{sat}}$. As shown in Figure 2.12, $\dot{m}_{\text{sat}}$ increases with increasing inlet subcooling, and it is significantly higher than core mass flow rate, $\dot{m}$, corresponding to operating power of 2.0 and 4.0 MWt. For example, at inlet temperature of 303 K, $\dot{m}_{\text{sat}}$ is approximately 0.105 kg/s, while $\dot{m}$ in the average and hottest channels in the core at 4 MWt is 0.06 kg/s and

Figure 2.12. Model predictions of the mass flow rate through the ACRR core corresponding to the saturation exit conditions.

0.68 kg/s, respectively. In Chapter 4 these $\dot{m}_{\text{sat}}$ predictions are used in conjunction with UNM-CHF correlations to obtain best estimates of the CHF and CHFR in the ACRR.

2.7 Conclusions

A thermal-hydraulic model for the Sandia ACRR was developed and the model predictions were compared with the in-core measurements. The model predictions of the temperature differential across the core were in excellent agreement with the in-core measurements at 2.0, 3.25, and 4.0 MW. Results from the model indicated that subcooled boiling in the central and average channel of ACRR will begin at reactor power levels of 0.5 and 0.7 MW, respectively. At higher power levels the extent of the boiling region along the active fuel length increases with the reactor power, causing the maximum cladding temperature to increase very little.

Chapter 3

UNM - CHF EXPERIMENTS

A multiyear UNM-SNL-GA joint research program was initiated to develop CHF correlations that are applicable to low-flow and near-atmospheric pressure conditions [40]; conditions of particular relevance to the ACRR. As part of the program, experiments were conducted to obtain CHF data in vertical annuli having a uniformly heated inner wall and an insulated outer wall. Details on the design and instrumentation of the experiments as well as the experimental results, which include point-by-point listings of the data, are presented elsewhere [16]. The following sections summarize the experimental results, which are important to obtaining the best estimate values of CHF and CHFR in the ACRR.

3.1 Experimental Results

In UNM-CHF experiments, a total of 383 data points were collected using three annuli for inlet subcoolings ranging from 182 to 312 kJ/kg and for mass flow rates from 0 to 0.0841 kg/s. The CHF data for the three annuli ($\epsilon = 1.575, 1.72$ and 2.0) are plotted in Figure 3.1 as functions of the inlet water mass flux along with the zero flow data. The values of CHF varied from 155 kW/m² for zero inlet flow to a maximum of 1418 kW/m² for a water mass flow rate of 0.0841 kg/s. The values of CHF for zero flow were generally about 30% lower than the CHF at the lowest mass flux values. This figure can be used to demonstrate the combined effects of mass flux, annulus ratio, and inlet subcooling on CHF. Increasing the mass flux in a given annulus (or the mass flow rate) increased the CHF. Although the difference in the effect of annulus ratio on CHF between the smallest annulus ratios 1.575 and 1.72 is not distinguishable, the effect of the largest annulus is quite pronounced. Also, Figure 3.1 shows that the effect of inlet subcooling on CHF was negligible in the smaller annuli ($\epsilon = 1.575$ and 1.72) and in the largest annulus ($\epsilon = 2.0$) for mass fluxes less than 140 kg/m²s. However at mass fluxes greater than 140 kg/m²s, the effects of inlet subcooling were distinct in the largest annulus; thus increasing the water inlet subcooling resulted in higher CHF values.

3.2 Incipience of Chugging

Other data of interest to this study are those for incipient chugging, which occurred at a much lower heat flux than CHF. In Figure 3.2 the surface heat flux corresponding to the onset of chugging is plotted versus the water's inlet mass flux. These data exhibit same dependence on the operating parameters as the CHF data, except that the effect of inlet subcooling on the heat flux at incipient chugging is present in all three annuli. As shown in Figure 3.2 the effect of inlet subcooling becomes particularly distinct at water mass fluxes that are greater than 140 kg/m²s. A comparison of these data with the CHF data in Figure 3.1 shows that the heat flux for incipient chugging vary from a minimum of about 40% of CHF in the smallest annulus ($\epsilon = 1.575$) to a maximum of approximately 88% of CHF for larger annuli ($\epsilon = 1.72$ and 2.0). These results are important to the operation and safety of the TRIGA type research reactors and the ACRR because, as discussed in Chapter 1, flow chugging in the core could trigger automatic shutdown of the reactor before reaching CHF.

Figure 3.1. The UNM-CHF data for low-flow and low-pressure operating conditions for three vertical annuli having a uniformly heated inner wall and insulated outer wall.

Figure 3.2. The UNM data for incipient chugging in the annular test sections as a function of water mass flux and inlet subcooling.

3.3 Flow Transition at CHF

The UNM-CHF data for the three annulus ratios, excluding those with zero inlet flow, were classified into three categories according to the flow transition occurring in the test section at CHF. These flow transitions are the slug-churn flow transition, the churn-annular flow transition, and the annular-annular mist flow transition (see Figure 3.3). The classification of the CHF data was made based on the recorded video images and still photographs of the flow fields as well as the two-phase flow maps (see Figures 3.3-3.5) [16]. It is important to note that the results of the flow maps are only qualitative because they are developed based on equilibrium exit quality values. However, the actual values of the exit quality in the present CHF experiments at the low-water flow rate are expected to be higher than the equilibrium values (see Figure 3.6) [3].

Figure 3.3 presents the CHF data for the smallest annulus ($\epsilon = 1.575$) plotted on a two-phase flow map obtained from using the drift-flux model [41,42]. As this figure indicates, the CHF data for this annulus falls on the transition boundary between annular and annular mist flow. Figures 3.4 and 3.5 present the CHF data for the larger annulus ratios ($\epsilon = 1.72$ and 2.0) plotted onto the corresponding two-phase flow maps. As these figures suggest most of the data for these two annuli fall on the transition boundary of either the slug-churn flow or the churn-annular flows. The scattering in the data between the various flow regimes, however, occurred because of the inherent uncertainties associated with the two-phase flow maps and the use of equilibrium exit quality rather than the nonequilibrium exit quality. Because estimating the nonequilibrium (or actual) quality is beyond the scope of this work, the two-phase flow maps (Figures 3.3-3.5) were plotted in terms of the equilibrium exit quality, which can be easily calculated from the heat balance in the test section. In the range of equilibrium qualities obtained in El-Genk et al.'s experiments (-0.01 to 0.40), the actual quality was expected to be higher than the equilibrium quality. This may explain the scattering in the experimental data points presented in Figure 3.5.

3.4 Development of UNM-CHF Correlations

The UNM-CHF data were classified and correlated in two separate groups. The first group includes CHF data for the largest two annuli ($\epsilon = 1.72$ and 2.0), which occurred in the slug-churn and churn-annular flow transitions. The following general correlating equation was derived [14] starting from the heat balance equation:

$$q^* \left(\frac{A_h}{A} \right) = A_1 [L/D_h]^{B_1} \left[C_1 + D_1 \frac{G^* \Delta h_i}{h_{fg}} \right], \quad (3.1)$$

where

$$q^* = Q / (h_{fg} A_h \sqrt{\lambda \rho_v g \Delta \rho}),$$

$$G^* = G / \sqrt{\lambda \rho_v g \Delta \rho},$$

$$\lambda = \sqrt{\sigma / g \Delta \rho}.$$

Figure 3.3. The flow regime mapping of the UNM-CHF data for $\varepsilon = 1.575$.

Figure 3.4. The flow regime mapping of the UNM-CHF data for $\varepsilon = 1.72$.

Figure 3.5. The flow regime mapping of the UNM-CHF data for $\epsilon = 2.0$.

Figure 3.6. Schematic of the relationship between equilibrium and nonequilibrium qualities.

The coefficient A_1 , B_1 , C_1 , and D_1 were determined from the least square fit of Equation (3.1) to the CHF data of both the slug-churn and the churn-annular flow transitions in the two largest annuli ($\epsilon = 1.72$ and 2.0); $A_1 = 1.65$, $B_1 = 0.2$, $C_1 = 0.933$ and $D_1 = 0.212$. Equation (3.1) is compared with the experimental data in Figure 3.7; this figure shows the good agreement between the data and the correlation.

A different correlation was developed for the second group of CHF data for the smallest annuli ($\epsilon = 1.575$), which occurred at the annular-annular mist flow transition. The basic heat balance equation in conjunction with the drift flux model for annular flow [43] was used to derive the following general correlating equation.

$$q * \left(\frac{A_h}{A} \right) = C_2 N_{\mu f}^{-0.2} + D_2 G * \frac{\Delta h_i}{h_{fg}}, \quad (3.2)$$

where,

$$N_{\mu f} = \left[\mu_f / \sqrt{\sigma \rho_l (\sigma / g \Delta \rho)^{1/2}} \right].$$

The coefficients C_2 , D_2 were determined from the least squares fit of Equation (3.2) with the CHF data for annulus ratio 1.575 to be 0.852 and 1.045, respectively. Equation (3.2) was plotted with the experimental data in Figure 3.8. Once again good agreement between the experiment and the correlation is evident.

Figure 3.7. Comparison of the UNM-CHF correlation for slug-churn flows with the data for the larger annuli.

Figure 3.8. Comparison of the UNM-CHF correlation for annular-annular mist flows with the data for the smallest annulus.

Before applying the UNM-CHF correlations [Equations (3.1) and (3.2)] to predict CHF and CHFR in the ACRR, their predictions were compared with the experimental data of previous investigations obtained for low-flow of water at near atmospheric pressures in vertical annuli [13,14]. Table 3.1 summarizes the range of geometrical and operating conditions over which the comparison was carried out and also lists the geometrical and operating conditions in the ACRR.

As shown in Table 3.1, the comparison between the experimental data of Rogers et al. [13] and the UNM-CHF correlation is reasonable; in most cases the deviation was less than the reported experimental uncertainties (15%). This validates the UNM-CHF correlation in the range of mass flux (75-225 kg/m²s), which is important for the ACRR application (see Table 3.1). By contrast the experimental data of Mishima and Ishii [14], obtained at very low mass fluxes ($G < 50$ kg/m²s) using natural convection, were as much as 30% lower. These low CHF values can be attributed to two factors. First, according to Mishima and Ishii, at low mass fluxes presence of large hydrodynamic instabilities resulted in premature occurrence of CHF; in quite a few cases the CHF occurred at bubbly slug flow transition, where the measured CHF values were up to 30% lower than those predicted using UNM-CHF correlations. The rest of their data, where CHF occurred at churn-annular flow transition, were in reasonable agreement (deviation < 15%) with the UNM-CHF correlations. Secondly, as pointed out by Mishima and Ishii, the large-scale hydrodynamic instabilities in their experiments introduced large uncertainty in the flow measurements (up to 20%), which might explain the large discrepancies between the CHF data and UNM correlation at low mass fluxes. As pointed out earlier in this range of mass fluxes of interest to the ACRR (250-285 kg/m²s), the agreement between the UNM-CHF correlation and the experimental data (UNM experimental data as well as the data of Rogers et al., [13]) is good.

Table 3.1. Comparison of the ACRR geometrical and operating conditions with the experimental range of operating conditions encountered in UNM-CHF experiments.

Ref	No Data	ϵ	D_h (cm)	G (kg/m ² s)	T_{in} (K)	CHF (kW/m ²)	X_{exit} (%)	Comp with UNM-Correlation		
								Max. Diff (%)	Ave. Diff (%)	No. of data >15%
[15]	173	2.0	3.81	25-225	297-320	400-1400	-0.2-45.3	14.54	9.25	1
[15]	107	1.72	2.5	40-240	297-320	350-958	-0.4-27.45	15.10	9.25	1
[15]	92	1.575	1.875	40-250	297-320	350-950	4.26-30.35	12.0	6.75	None
[13]	37	1.68	2.384	50-700	293-320	150-2800	-3.0-22	21.42	12.34	9
[14]	52	1.269	1.250	5-50	307-353	100-400	5.0-45.0	29.33	12.34	16
[17]	ACRR	1.12 (P/D)	1.385	230-270	293-333	See Analysis in this report	See Analysis in this report	NA	NA	NA

Figure 3.9. Flow regime map for the bundle data of Becker et al. [22].

The discussion presented above confirms the applicability of UNM-CHF correlation to annular geometries. However, uncertainties still remain regarding the applicability of annular data to the ACRR core which consists of a multirod bundles like geometry. Therefore, before making a prediction of CHF in the ACRR core, it is imperative to first examine the applicability of the UNM-CHF correlation to bundle geometry.

3.5 Applicability of UNM-CHF Correlation to Bundle Geometry

MacBeth [19] and Barnett [26] have shown that annulus CHF correlations predict a wide range of bundle data with remarkable accuracy. However, their investigations were limited to high-flow and high-pressure operating conditions. To the best of the authors' knowledge, no investigations have been conducted to examine the applicability of annulus-correlations to bundle geometry at low-flow and low-pressure conditions, typical of those in the ACRR. In order to examine its applicability to bundle geometry, the UNM-CHF correlation is compared with the experimental data of Becker et al. [22] for low-flow and low-pressure conditions in a 7-rod bundle with $P/D = 1.23$. Their data were obtained for mass fluxes ranging between 65 and 245 $\text{kg/m}^2\cdot\text{s}$, and pressure in the range from 0.3 to 0.64 MPa.

The experimental data of Becker et al. [22] is classified according to the flow transition at CHF and compared with the respective UNM-CHF correlations. As shown in Figure 3.9, in most cases the CHF in Becker et al.'s bundles occurred at the annular-annular mist flow transition. However, there were a limited number of runs (eight) where CHF occurred at the slug-churn/churn-annular flow transitions. Figures 3.10 and 3.11 compare of the bundle data of Becker et al. with the UNM-CHF correlations for both annular-annular mist flow transition and slug-churn/churn-annular flow transition. As evident from these figures, the UNM-CHF correlations for the annulus geometry are in good agreement with the bundle data; the average deviation is 7% while the maximum deviation is 15%. This good agreement confirms the applicability of annulus CHF correlation to bundle geometry at low-flow and low-pressure operating conditions. In the following chapter UNM-CHF correlations for annulus geometry are applied to predict CHF/CHFR in the ACRR.

Figure 3.10. Comparison of the UNM-CHF correlation predictions for CHF with the bundle data of Becker et al. [22].

Figure 3.11. Point-by-point comparison of the UNM-CHF correlation with the bundle data of Becker et al. [22].

Chapter 4

PREDICTION OF CHF AND CHFR IN THE ACRR

4.1 The Flow Transition at CHF in the ACRR

Experimental investigations of El-Genk et al. [15] for annular geometries and that of Becker et al. [22] for bundle geometry indicate that in constrained geometries (such as in the ACRR with $P/D = 1.12$) at low-pressure conditions, the CHF phenomena is expected to occur during the annular-annular mist flow transition. When Becker et al. [22] CHF data for rod bundles are plotted on a two-phase flow regime map (see Figure 3.10), it indicated that for P/D ratios less than 1.2, the CHF in almost all cases occurred during the annular-annular mist flow transition. Therefore, it can be argued that in the ACRR the CHF also occur during the transition from annular to annular mist flow. However, there are several uncertainties associated with this argument.

- (1) The investigations of Mishima et al. [12] and Mishima and Ishii [14] indicated that for soft systems, CHF occurs during the churn-annular flow transition. A system is defined as soft if the pressure losses in the heated section are substantially larger than those incurred in the rest of the loop. During single-phase operation, the ACRR is not a soft system because the pressure losses in the lower and upper grid plates and in the downcomer are comparable to the net pressure loss in the heated section alone; they are approximately 10%-20% lower than the later. However, it is conceivable that at high powers the system may become soft due to the increased two-phase flow pressure drop in the reactor core, causing CHF to occur during the slug-churn/churn annular flow transition.*
- (2) At high power, beyond that to initiate boiling, two-phase pressure losses in the hottest channels, near the central cavity in the ACRR, will be higher than those in the outer channels, where operating power and void fraction are lower. Consequently, a pressure differential will develop causing net radial flow from the cooler outer subchannels to the hotter channels, thus reducing their exit void fraction and causing CHF to occur at the churn-annular flow transition instead of the annular-annular flow transition. The latter would have been the expected flow transition at CHF in the ACRR, if cross flow between neighboring subchannels is negligible, which was the case in CHF annulus experiments of Becker et al. [22] and El-Genk et al. [15].

In conclusion, CHF in the ACRR most probably will occur during either the annular-annular mist flow transition or the churn-annular flow transition. However, since the slug-churn/churn-annular flow transition constitutes the worst case scenario, CHF predictions in the ACRR are made using the slug-churn/churn-annular flow transition; correlation [15] [Equation (3.1)]. The following sections describes the methodology used to evaluate the CHF and the CHFR in the ACRR using this correlation.

* As discussed in the previous sections, soft systems are associated with potential for large flow instabilities that would probably induce unacceptably large negative power fluctuations. In such a case it is likely that the automatic reactor trip will prevent occurrence of CHF.

4.2 Methodology Used to Calculate CHF/CHFR in the ACRR

As pointed out earlier, the CHF in the nonuniformly heated ACRR core can be estimated using the UNM-CHF correlation in conjunction with either (a) the total power hypothesis, (b) the local conditions hypothesis, or (c) the global conditions hypothesis. Because of the uncertainties associated with the first two hypotheses (see Section 1.2) and the success of the latter to correlate the CHF data, regardless of the shape of the axial flux distribution, the predictions of CHF, based on the global conditions hypothesis, are considered the most accurate. In the following subsections, the calculation used to evaluate CHF and the CHFR using each of the above hypotheses is presented and discussed.

4.2.1 The Total Power Hypothesis proposed by Lee and Obertelli [25], the total power hypothesis suggests that the total power input at CHF in nonuniformly heated geometry is equal to that for uniformly heated geometry for the same flow rate and inlet subcooling. Under such an assumption, the power per rod corresponding to the CHF conditions in ACRR is calculated in four successive steps.

1. The mass flow rate in the ACRR at CHF is calculated as a function of inlet temperature using the one-dimensional thermal-hydraulics model developed in Chapter 2 (see Figure 2.11) and assuming exit saturation temperature.
2. This mass flow rate is then used in Equation (3.1) to calculate the CHF value at the churn-annular flow transition. Multiplication of CHF with the rod surface area provides an estimate of the total power per rod at CHF.
3. According to the total power hypothesis, the power per rod calculated in Step 2 is equal to that corresponding to CHF in the ACRR.
4. Divide this value of CHF by the maximum (radial peak) heat flux in the ACRR.

Figures 4.1 and 4.2 present the calculated values of power per rod at CHF and the CHFR, respectively, as functions of inlet temperature. As illustrated in Figure 4.1, the power per rod corresponding to the CHF ranges from 57 kWt for T_{in} of 20°C to approximately 39 kWt for T_{in} of 55°C. Assuming that CHF occurs in the central hottest subchannel, the reactor power corresponding to CHF conditions is 9.6 MWt and 6.4 MWt at inlet temperatures of 20° and 55°C, respectively. As shown in Figure 4.2 the Critical Heat Power Ratio (CHFR), in the ACRR is equal to 2.4 at T_{in} of 20°C and 1.6 at T_{in} of 55°C.

Careful examination of Figure 4.1 reveals that the total power hypothesis prediction of rod power corresponding to CHF is approximately 2%-15% lower than that corresponding to the saturation exit conditions. The conservative estimates of CHF that the total power hypothesis provides are consistent with those reported by previous investigators [26,27]. For example, Barnett [32] pointed out that for skewed axial heat flux distribution, the total power hypothesis underpredicts CHF by up to 30%. Consequently, Lee [28] suggested using total power hypothesis to obtain a lower bound estimate for CHF.

Figure 4.1. Total and local power hypotheses predictions for power per rod corresponding to CHF as functions of inlet temperature.

Figure 4.2. Total and local power hypotheses predictions for CHF as functions of inlet temperature.

4.2.2 The Local Conditions Hypothesis assumes that there exists an unique relationship between the steam quality at the location of CHF and the CHF. The calculation procedure in this case is as follows:

- 1&2. Carry out Steps 1 and 2 of the total power hypothesis.
3. Calculate the equilibrium exit quality corresponding to the predicted CHF in Step 2.
4. Calculate the axial location and power per rod in the ACRR at which steam quality and the local heat flux are the same as Step 3. This local heat flux is taken as the CHF in the ACRR core.
5. Divide the CHF value determined from Step 4 by the heat flux at the same axial location in the ACRR at 4 MWt to determine the CHFR.

The CHF and CHFR values determined based on the local conditions hypothesis are also plotted in Figures 4.1 and 4.2, along with those obtained using the total power hypothesis. As shown in these figures, predictions for the power per rod at CHF using the local conditions hypothesis varied between 100 kWt at T_{in} of 20°C and 70 kWt at T_{in} of 55°C. These values correspond to CHFR of 4.25 and 2.6, respectively.

Although the CHFR predictions based on the local conditions hypothesis are highly favorable to the proposed upgrade of ACRR, the global hypothesis is the most reliable primarily because it has been validated for low-flow and low-pressure conditions. As discussed in Chapter 1, (see Figure 1.4), the local conditions do not accurately account for the effect of nonuniform axial heat flux distribution resulting in the substantial overpredictions (up to 40%) of CHF Barnett [26], and Collier [21].

As pointed out in Chapter 1, the main drawback of the local conditions hypothesis is that CHF is entirely a local phenomena. On the other extreme, the assumption that the local conditions have little or no effect on CHF is the drawback of the total power hypothesis, but rather the upstream condition leading to CHF. A combination of these two hypotheses forms the basis for the global conditions approach. Several articles in the recent literature exemplify the distinct merits of the global conditions hypothesis. The following section is devoted to CHF/CHFR calculations using the global conditions hypothesis.

4.2.3 The Global Conditions Hypothesis suggests that the local steam quality at the location of CHF, x_c , is uniquely related to the boiling length up to that location, L_{BC} ; this relation is independent of the axial heat distribution or flow regime at CHF. Numerous experimental investigations have confirmed the validity of global conditions hypothesis for various geometries at high-flow and high-pressure operating conditions (see Figures 1.6-1-8). However, until now no confirmation was provided for low-flow conditions. To verify the validity of global conditions hypothesis to low-flow and low-pressure conditions, the experimental data of El-Genk et al. [15,16] are plotted in the $x_c - L_{BC}$ plane (see Figure 4.3). As illustrated in Figure 4.3 the data obtained from all three annuli collapsed together to form a single line for each inlet subcooling.

Figure 4.3. A plot of UNM-CHF data in the $x_c - L_{BC}$ plane to verify validity of global conditions hypothesis to low-flow and low-pressure operating conditions.

Based on such a behavior, it was possible to correlate the data obtained from all three annuli using the following equation:

$$x_c = \frac{(\Delta h)_i}{h_{fg}} \sum_{i=0}^7 a_i (L_{BC})^i, \quad (4.1)$$

where

$$\begin{aligned} a_1 &= 196.25 & a_4 &= -263438.96 \\ a_2 &= -1094.64 & a_5 &= 1203957.29 \\ a_3 &= 29908.79 & a_6 &= -2680872.71 \\ a_7 &= 2407232.29 \end{aligned}$$

It should be noted that Equation (4.1) adequately correlates the CHF experimental data of El-Genk et al. [15,16] for all three flow transitions at CHF (slug-churn, churn-annular, and annular-annular mist). As shown in Figure 4.4, this equation is within $\pm 4\%$ of the experimental data. This correlation developed based on the global conditions hypothesis was employed to determine CHF/CHFR in ACRR as specified below:

1. For a given inlet temperature, calculate the mass flow rate through the core using the thermal hydraulic model for the ACRR as a function of inlet subcooling.
2. Use this value of mass flow rate to predict the critical boiling length that satisfies not only Equation (4.1) but also the following energy conservation equation:

$$\int_{L_{NB}}^{L_{BC}} \pi D q'' dz = \dot{m} x_c h_{fg}, \quad (4.2)$$

where the nonboiling length, L_{NB} , is determined from the following equation:

$$\int_0^{L_{BC}} \pi D q'' dz = \dot{m} (h_{sat} - h_{in}). \quad (4.3)$$

3. The heat flux at the location where both Equations (4.1) and (4.2) are satisfied is equal to CHF.
4. Divide this CHF value by the local value of heat flux at 4 MWt operation to obtain CHFR.

Figures 4.5 and 4.6 present global conditions hypothesis predictions for the power per rod corresponding to CHF and CHFR, respectively. As shown in Figure 4.5 the power per rod corresponding to CHF varies from 95 KWth at an inlet temperature of 20°C to 60 KWth at an inlet temperature of 55°C. As illustrated in Figure 4.6 this represents a CHFR of 3.9 at 20°C and 2.1 at 55°C.

Figure 4.4. Comparison of Equation (4.1) with the experimental data from all three annuli.

Figure 4.5. Total power hypothesis predictions of power per rod corresponding to CHF as a function of inlet temperature.

Figure 4.6. Total power hypothesis predictions of CHFR as function of inlet temperature.

4.3 Discussions

As shown in Figure 4.6, the CHF estimate for ACRR is highly dependent on the hypothesis used. In the temperatures ranging between 20° to 55°C, the total power hypothesis predictions varied between 2.4 and 1.6, the local conditions hypothesis predictions varied between 4.2 and 2.8, and the global conditions hypothesis predictions varied between 3.9 and 2.1 when the higher values correspond to the lower inlet temperature. Due to this large dependence of CHF predictions on the hypothesis used for evaluation, it is desirable to comment on which of these predictions are the most reliable.

As discussed earlier, several investigators have shown that the total power hypothesis significantly underestimates CHF in nonuniform geometries. The fact that total power hypothesis predictions of the power per rod corresponding to CHF are lower than those corresponding to saturation exit conditions (see Figure 4.1) confirms that trend. Therefore, it is recommended to the total power hypothesis CHF predictions be treated as a lower bound. Among the other two hypotheses, the global hypothesis is the most reliable primarily because of its validity the low-flow and low-pressure conditions has been confirmed (see Figures 4.3 and 4.4). To the contrary the local conditions hypothesis has never been experimentally verified for low-flow and low-pressure conditions, and very scarcely verified at high-pressure conditions. Moreover, the global conditions hypothesis provided more conservative estimate of CHF/CHFR in the ACRR as compared to the local conditions hypothesis. Consequently, it is recommended that the global conditions hypothesis predictions be regarded as the best estimate predictions for CHF/CHFR in the ACRR.

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