Experimental Investigations on Boiling Heat Transfer Characteristics of Accident-Tolerant-Fuel and Traditional Claddings

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by

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B.S., Nuclear Engineering, Chengdu University of Technology, 2016
M.S., Nuclear Engineering, The University of New Mexico, 2019

DISSERTATION

Submitted in Partial Fulfillment of the Requirements for the Degree of

Doctor of Philosophy

Engineering

The University of New Mexico
Albuquerque, New Mexico

May, 2022
Acknowledgments

First and foremost, I would like to express my much gratitude to my graduate supervisor, Professor Minghui Chen. Without his careful and insightful instructions and guidance, this dissertation work would not be accomplished. I am very grateful to Professor Anil K. Prinja for his kind help and advice during the miserable adaptation stage of my first arrival in USA. I really appreciate Professor Amir Ali's help, instruction and suggestion with my experimentations, researches and studies. These three professors provide constructive suggestions and committee services to this dissertation work. Moreover, I am thankful to Professor Osman Anderoglu and Professor Nima Fathi for serving my dissertation defense committee. These professors are sincerely appreciated for their help with my milestone move-forward.

This dissertation work is based upon work supported by the U.S. Department of Energy Office of Nuclear Energy’s Nuclear Energy University Program under award number DE-NE0008687 and Integrated Research Program under award number DE-NE-0008531. During the completion of this dissertation, I received the financial support of excellence fellowship from the UNM School of Engineering. Without their generous support, I could not survive the tough period of COVID-19 pandemic. I am also grateful to UNM GPSA council for their New Mexico Research Grant-General Priority on various cladding materials. The parts of this dissertation is also funded by Office of Graduate Studies UNM under the research supplement fund and the Dean’s Dissertation Fellowship.

Special thanks are credited to my significant other, Lanlan Wang, and my parents for their long-standing support and encourage to his doctoral study.
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ABSTRACT

To make it more clear that how the materials’ thermal-physical properties and wall thickness have influential impacts on boiling heat transfer characteristics of cladding, this dissertation looks into the potential impacts of cladding materials on critical heat flux and heat transfer coefficients by a systematic experimental investigation across a wide range of pool/flow boiling conditions under the steady-state and power-transient heat inputs.

Recent thermal-hydraulics studies have demonstrated that iron-chromium-aluminium (FeCrAl) alloys have the thermal priorities over zircalloys and other commercial alloys including critical heat flux and heat transfer coefficient. However, it is found in our experimental results that FeCrAl-C26M, and FeCrAl-C36M have higher critical heat fluxes and heat transfer coefficients than zircalloys and stainless steels while FeCrAl-B126Y and FeCrAl-B136Y have lower critical heat flux than zircalloys. This speaks to the possibility that the superiority of accident tolerant fuel claddings depends on which alloys they belong to. However, the difference gap of critical heat flux and of heat transfer coefficient between various cladding materials can be suppressed by high mass flux and/or high inlet subcooling. The one possible mechanistic rationale behind this is that the material-side factors related with near-field mechanisms such as surface wettability/roughness, and thermal-physical properties, compete with the far-field heat convection mechanisms dominated by mass flux and inlet subcooling.
Besides the steady-state boiling experiments, the FeCrAl and zircaloy claddings are subjected to the power transient heat inputs of linear ramp and Fuchs-RIA. In comparison with the steady-state flow boiling, the power transient maximum heat flux is larger than the steady-state critical heat flux and so is the power-transient heat transfer coefficient. As a result, it implies that the power transient state of light water reactors gives higher thermal safety margin than their nominal steady states. The experimental results of Fuchs-reactivity initiated accident power transient across a wide range of inlet subcoolings and mass fluxes give a solid confirmation to that the difference gap of power transient maximum heat flux between various transient timescales and cladding materials can be suppressed by the increasing of mass flux and inlet subcooling. This connotes that the power-transient boiling heat transfer may be dominated by two completely distinct mechanisms, heat conduction between cladding solid and water coolant competing with heat convection that are contributed by mass flux and inlet subcooling.
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Nomenclatures

Nomenclatures throughout all chapters are hereby defined including acronyms and symbols.

**Acronyms**

<table>
<thead>
<tr>
<th>Acronym</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>AFM</td>
<td>Atomic Force Microscopy</td>
</tr>
<tr>
<td>ATF</td>
<td>Accident Tolerant Fuel</td>
</tr>
<tr>
<td>BHT</td>
<td>Boiling Heat Transfer</td>
</tr>
<tr>
<td>CFD</td>
<td>Computational Fluid Dynamics</td>
</tr>
<tr>
<td>CHF</td>
<td>Critical Heat Flux $\text{kW/m}^2$</td>
</tr>
<tr>
<td>DC</td>
<td>Direct Current</td>
</tr>
<tr>
<td>HVAC</td>
<td>Heating, Ventilation, and Air Conditioning</td>
</tr>
<tr>
<td>DNB</td>
<td>Departure from Nucleate Boiling $\text{kW/m}^2$</td>
</tr>
<tr>
<td>DBAs</td>
<td>Design Basis Accidents</td>
</tr>
<tr>
<td>DOE</td>
<td>Department of Energy</td>
</tr>
<tr>
<td>EPRI</td>
<td>Electric Power Research Institute</td>
</tr>
<tr>
<td>FeCrAl</td>
<td>iron(Fe)-chromium(Cr)-aluminium(Al)</td>
</tr>
<tr>
<td>LUT</td>
<td>Look-Up Table</td>
</tr>
<tr>
<td>PID</td>
<td>Proportional-Integral-Derivative</td>
</tr>
<tr>
<td>PVD</td>
<td>Physical Vapor Deposition</td>
</tr>
<tr>
<td>MC-BHT</td>
<td>Material Conjugated Boiling Heat Transfer</td>
</tr>
<tr>
<td>MFB</td>
<td>Minimum Film Boiling</td>
</tr>
</tbody>
</table>
NRC Nuclear Regulatory Commission
NB-HTC Nucleate Boiling Heat Transfer Coefficient kW/m²K
GE General Electric
JAEA Japan Atomic Energy Agency
KAERI Korea Atomic Energy Research Institute
PCT Peak Cladding Temperature °C
PWR Pressurized Water Reactor
BWR Boiling Water Reactor
UNM University of New Mexico
LWR Light Water Reactor
RIA Reactivity Initiated Accident
LOCA Loss Of Coolant Accident
LOFA Loss Of Flow Accident
HTC Heat Transfer Coefficient kW/m²K
ONB Onset of Nucleate Boiling kW/m²
SBO Station Black-Out
SEM Scanning Electron Microscope
T-H Thermal-Hydraulics
UQ Uncertainty Quantification
SP-HTC Single Phase Heat Transfer Coefficient kW/m²K
SS Stainless Steels
HL Effective Heated Length m

Symbols

\( q'' \) Surface Heat Flux kW/m²
\( C_p \) Isobaric Specific Heat Capacity J/(kg·K)
\( k \) Thermal Conductivity W/(m·K)
\( \rho \) Mass Density kg/m³
\( T \) Temperature K

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\( R_e \) Ohmic Resistance \( \Omega \)
\( P \) Power \( \text{W} \)
\( U \) Voltage \( \text{Volt} \)
\( I \) Current \( \text{Amp} \)
\( D \) Outer Diameter \( \text{m} \)
\( \delta_{th} \) Wall Thickness \( \text{m} \)
\( \varepsilon \) Thermal Emissivity
\( \sigma \) Surface Tension \( \text{N/m} \)
\( g \) Gravitational Acceleration Constant \( \text{m/s}^2 \)
\( \phi \) Surface Inclination Angle \( ^\circ \)
\( \theta \) Surface Contact Angle \( ^\circ \)
\( \Delta h \) Specific Enthalpy Difference \( \text{J/kg} \)
\( p \) System Pressure \( \text{kPa} \)
\( G \) Mass Flux \( \text{kg/m}^2\text{s} \)
\( P/D \) Pitch-to-Diameter Ratio \([-]\)
\( \tau \) Power Transient Period \( \text{s} \)
\( \tau_e \) Exponential Reactor Period \( \text{s} \)
\( \Delta T_{sub} \) Liquid Subcooling \( \text{K} \)

**Dimensionless Quantity**

\( Re \) Reynolds Number
\( Pr \) Prandtl Number
\( Nu \) Nusselt Number
\( Bo \) Boiling Number
\( Ja \) Jacob Number
\( We \) Weber Number
\( Fo \) Fourier Number
\( X_e \) Equilibrium Thermodynamic Quality
\( Gr \) Grashof Number

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Chapter 1

Introduction

A growing number of countries and energy vendors take interest in expanding the market of nuclear energy or introducing nuclear energy as new power sources despite the nuclear accident occurred at the Fukushima Daiichi plant in Japan (Wittneben, 2012). However, the safe production of nuclear energy presses the rise of the engineering questions that nuclear community needs to face. To mitigate the accident impacts on reactor core and improve the safety margin of fuel-cladding structure, the U.S. Department of Energy (DOE) is advancing the campaign of accident tolerant fuel (ATF) concepts (S. Bragg-Sitton, 2014).

1.1 Prologue

Nuclear Regulatory Commission (NRC) renders a brief definition of ATF to nuclear community and industry, as that ATF is a complete set of novel technologies exhibiting the promising potentials of safety enhancement of reactor cores and their auxiliary components. Such new technologies are centered by offering better performances and resistances to reactor cores during normal operation, transient conditions, and accident scenarios. Introduction of ATF concepts targets to enhance the safety-margin, fault-tolerance and accident-resistance by replacing more advanced new fuel-cladding units in current light water reactors (LWRs). The ultimate purpose of ATF core load is to maintain the structural integrity and prevent the large-scale leakage of radionu-
clides during the accident scenarios. This needs new materials of fuel-cladding to be loaded in LWRs and these new materials should have compatibilities with existing LWRs from the perspective of chemistry, neutronics and thermal hydraulics (Charit, 2018).

The primary safety concerns of LWR cores are often determined by various limits of cladding material properties of fuel pins and assemblies under the extreme environment. Thus, studying the material properties and behaviors of ATF cladding is of utmost importance to decide if the potential candidates are applicable to the current designs of LWRs. One of important limits of ATF cladding materials is concerned with their corresponding thermal hydraulics performances under the operation protocols of LWRs including Critical Heat Flux (CHF), Nucleate Boiling Heat Transfer coefficient (NBHTC), Onset of Nucleate Boiling (ONB) and etc. Rebak (2015) provided a brief guideline of what aspects of alloy selections should be carefully assessed for ATF cladding in commercial LWRs. Also Pint et al. (2015) presented a comprehensive assessment example for the reference to material selection of ATF cladding. Although the advance fuel campaign of DOE has considered a large variety of candidate fuel-cladding materials, up to now there are a few most promising candidates of ATF cladding materials in the listings of NRC, DOE, and USA nuclear industry: Chromium-coated Zircalloy-4 (Brachet et al., 2019), FeCrAl series (Field et al., 2018), SiC/ SiC (Snead et al., 2017).

It is notable that research and development of ATF concepts to date has succeeded in grasping a progressively better understanding of material-mechanics performances of listed fuel-cladding candidates under the extreme environments of LWRs (Terrani, 2018), nuclear power community has little assessment of ATF concepts from the standpoint of thermal hydraulics so far. Untouched thermal hydraulics behaviors of proposed ATF cladding candidates include but not limited to these following imperative metrics: CHF, ONB, HTCs at single-phase and two phase regimes, friction factors, responses to emergency cooling and design basis accidents (DBAs). This is vital to the operation licensing of future ATF-based LWRs, the economy of nuclear power, the safety evaluation, progression of DBAs, and etc.
1.2 Milestones and Demands of ATF Core Loading

Following after nuclear reactor disasters occurring at the Fukushima Daiichi nuclear power plant in Japan, the senate of USA initiated the development of fuels with enhanced accident tolerances and DOE has been promoting the ATF technology to improve the economy of nuclear power and enhance the safety of current-design of LWRs. The strategic road-map of development, demonstration and deployment of ATF technologies is composed of three phases of implementation (Lyons, 2015).

(i) Feasibility Studies and Down-Selection Assessment

(ii) Development and Qualification

(iii) Commercialization and Core In-pile Tests

The timeline of implementation steps is shown in Fig.1-1. It has been almost ten years since the Fukushima nuclear avalanche that progresses of ATF conceptualization, validation and verification are remarkable under national programs boosted by DOE. The out-of-pile systematical experiments of ATF candidates make them technically ready and mature (Field et al., 2019; Duan et al., 2017), which moves the experimental focuses of ATF candidates to the stage of in-pile experiments. Up to date, several main vendors and their associates of nuclear power have implemented the in-pile tests of ATF-core loading in some current LWRs such as the Clinton GE-BWR unit (Peachey, 2020) and the Byron Westinghouse-PWR unit 2 (Isted, 2019) and the marching journey of ATF core load towards commercialization is briefly divided into three steps: individual in-pile tests, batch loading and full-core loading (See key ATF fuel milestones in Fig.1-2). Safety concerns of nuclear reactor are perhaps the top-tier driver of fuel design and optimization while it is a must that introduction of ATF-technology to cores of LWRs should move cautiously. The comprehensive assessment of ATF innovation should be performed systematically during the in-pile and out-of-pile tests. S. M. Bragg-Sitton et al. (2016) summarizes the technical assessment methodologies to assist the prioritization and optimization of ATF designs including
Figure 1-1: R&D roadmap of DOE ATF campaign (Lyons, 2015; S. M. Bragg-Sitton et al., 2016)

the attributes and constraints of ATF candidates, and the evaluation metrics. Actually, apart from the top priority of safe and reliable operation of LWRs, their power efficiencies and economies are deciding factors in the implementation plan of ATF core load. However, such relevant studies are insufficient.

It is noted by DOE that the urgency of nuclear power is underscored by many economic reasons (Morgan et al., 2018). Because nuclear power cannot compete with cheap shale gas in the energy market of North America and Europe (Brown, 2013). Patel (2020) addressed that nuclear power fleets of USA already have sixty-year operation licenses that will expire in the 2030s. This will exacerbate the shrinking of the existing fleet of USA nuclear power. Although the advanced reactors of US design such as molten salt reactor, liquid metal-cooled reactor and etc have been investigated for many years even since the early stage of atomic age, it is quite indisputable that these advanced nuclear reactors of Generation IV will not be available in the
commercial market of nuclear energy in the next few decades. To compete with natural gas and/or decarbonize the current energy system, the most engineering-feasible strategy is to revitalize LWRs by loading ATF candidates in current LWR cores and prolonging the lifespan of existing nuclear power units, and to improve the nominal power level of ATF-loaded LWRs. This concept needs a broad-ranging assessment of experimental and computational verification and validation. The lessons learned from nuclear reactor accidents have facilitated fuel researches and developments of different suppliers including Westinghouse (Ray et al., 2014; Lahoda & Boylan, 2019), GE (Stachowski et al., 2016), KAERI (Koo et al., 2014), and etc. From the current status of LWR deployment and development in 7 countries (See Tab.1.1), the PWR nuclear power will be the mainstream energy source in next several decades. However, BWR was the primary reactor type in the nuclear energy market of Japan and the number of PWR gradually increases recent years in Japan. It can be anticipated that the ATF technology will have a promising need in the LWR market of USA and China. To successfully load ATF into PWR cores that are designed based on different
Table 1.1: Statistic Information of LWR Units in Several Countries With a Significant Amount of Nuclear Power (Data Source from World Nuclear Association)

<table>
<thead>
<tr>
<th>Country</th>
<th>Type</th>
<th>Operational</th>
<th>Planned</th>
<th>Under Construction</th>
<th>Others</th>
</tr>
</thead>
<tbody>
<tr>
<td>USA</td>
<td>PWR</td>
<td>63</td>
<td>16</td>
<td>2</td>
<td>27</td>
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<td></td>
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<td>17</td>
</tr>
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<td>Russia</td>
<td>PWR</td>
<td>23</td>
<td>19</td>
<td>4</td>
<td>21</td>
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<td>2</td>
<td>3</td>
<td>2</td>
<td>30</td>
</tr>
<tr>
<td>France</td>
<td>PWR</td>
<td>55</td>
<td>0</td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>BWR</td>
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<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>UK</td>
<td>PWR</td>
<td>1</td>
<td>6</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>BWR</td>
<td>0</td>
<td>4</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>South Korean</td>
<td>PWR</td>
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<td>0</td>
<td>4</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>BWR</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>China*</td>
<td>PWR</td>
<td>47</td>
<td>80</td>
<td>14</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>BWR</td>
<td>2</td>
<td>0</td>
<td>0</td>
<td>4</td>
</tr>
</tbody>
</table>

Others: Unfinished, Shutdown, Dismantled, Operation Suspended, and etc

Noting that other countries compared with the listed nations above have less amount of nuclear power but they still matter in nuclear energy market such as India and Brazil. For Statistic Information in China, two parts are included both People's Republic of China and Republic of China (Claim no politics views are involved).

protocols, assessments of ATF loaded cores of design specs and protocols should be performed with the ATF-related tabulated data of out-of-pile tests under the assist of emergent computation power.

1.3 Overall Framework of This Study

1.3.1 Objectives

Recent studies of ATF candidates (Duan et al., 2017; Terrani, 2018; Charit, 2018; Field et al., 2019; Braun et al., 2017) have made great progresses in material attributes, material mechanics, irradiation behaviors and chemical compatibilities. However the exploring thermal hydraulics studies of ATF materials are inadequate to support the full core load of ATF candidates in the midst of the 2020s even if their thermal mechanical properties are well understood (Snead et al., 2015, 2017; Field et al.,
2018; Lahoda & Boylan, 2019; Koyanagi et al., 2018; Gamble et al., 2017). Up to date, some thermal hydraulic studies of ATF-candidates, such as the ATF quenching studies (J. Y. Kang et al., 2018), are partially touched in the board-ranging coverage of thermal hydraulic phenomena (Aksan, 2019). To fill up the missing gaps in thermal hydraulic studies of ATF-candidates, this study covers these following areas to support the future core load of those proposed ATF materials:

a) Material Dependent Pool Boiling Studies

- To characterize the pool boiling CHF/HTC values of FeCrAl alloys, Zircalloys and other traditional cladding materials;
- To understand how the material thermal-physical properties and cladding wall thickness affect pool boiling CHF;
- To probe how the pool boiling CHF occurrence exert corrosive effects on the cladding surfaces;

b) Material Dependent Flow Boiling Regimes of Steady-State

- To study how heat transfer coefficients of single phase and two phase are dependent of cladding tube materials;
- To study the material dependent behaviors of two deciding points of nucleate boiling, i.e, HTCs and CHF;
- To systematically study the footprints of thermal hydraulic on flow boiling curves, i.e, the effects of near- and post- CHF on cladding surfaces;
- To translate the experimental CHF datasets of uniform heated tube to the practical CHF values of Cosine heated rods in fuel assembly structures of LWRs;

c) Material Conjugate Heat Transfer of Reactor Kinetics

- To identify the primary characteristics of transient flow boiling curve;
- To study the thermal responses of cladding materials to different transient heat input modes;
To investigate how thermal responses of cladding materials to different rates of individual transient heat input;

To understand the progression of DBAs via using experimental data of transient flow boiling;

Fig.1-3 presents a general scheme on how the heat convection is coupled with quenching and evaporation. According to the heat flux partitioning model, the total heat flux released from the cladding surface can be divided into three parts, convective heat $q'^{c}$, quenching heat $q'^{q}$, and evaporative heat $q'^{e}$. The convective heat flux is only governed by the far-field convective factors including mass flux, inlet subcooling and system layouts. However, the quenching and evaporative heat fluxes are partially subjected to near-field material-side factors including thermal-physical properties, surface hydrodynamics, and surface morphologies. Especially, the quenching heat flux is more likely dominated by the cladding material thermal-physical properties because of bubble-induced transient heat conduction. The mechanistic contributions to the material-related heat evaporation also include the cladding surface hydrody-

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Figure 1-3: General coupling scheme of heat convection with quenching and evaporation
namics (surface wettability, wicking spreadability) and surface morphologies (surface roughness, macro/micro surface structures) by the bubble interactive behaviors.

1.3.2 Outlines of This Dissertation

In this study, the primary goal is to study the thermal hydraulics performances of ATF candidates under different operation protocols. The outline of this dissertation is schemed as follows:

- Chapter 1 gives the introductory description to the experimental and numerical studies of this dissertation and provides a general elaboration to the research background of ATF concepts;

- Chapter 2 lists out the experimental facilities of ATF claddings T-H performances and tabulates their important system components, also elucidates the operational procedures of ATF claddings T-H experiments;

- Chapter 3 briefly reviews the recent progresses on ATF cladding T-H evaluations of ATF cladding materials and discusses the implications of ATF T-H experimental results to the thermal safety margins of LWRs;

- Chapter 5 experimentally investigates the T-H characteristics of ATF cladding candidates and explores the T-H performances between various cladding materials contributed by the material thermal-physical properties under various flow boiling conditions;

- Chapter 6 presents the state-of-art review studies on the recent power transient flow boiling experiments, discusses their potential implications to thermal safety evaluations of LWRs, then experimentally investigates how the power transient modes and material thermal properties affect the transient flow boiling heat transfers.

- Chapter 7 experimentally characterizes how the cladding materials respond to the linear ramp/Fuchs-RIA power transients in terms of MHF and cladding
surface temperature and studies the potential impacts of inlet subcooling and mass flux on the power-transient flow boiling.

• Chapter 8 extracts key experimental findings and present primary implications of those experimental results to the ATF-loaded LWRs.

1.4 Conclusions

This chapter renders a brief historic background of ATF concepts and rationalizes why the ATF concepts are much needed in the LWRs-based energy market. Besides, the analytical and experimental study scopes of ATF cladding candidates are elaborated in details and the outlines and content structures of this dissertation are generally described to provide an overall view of the T-H characteristics of ATF claddings.
Chapter 2

Experimental Facilities of Boiling Heat Transfer

2.1 Prologue

Although mechanistic and phenomenological understanding of thermal fluid and theoretical approaches of flow boiling have been gaining substantial progresses in community of boiling heat transfer for many years, predictive models of two primary points are not still technically ready to practical applications of engineering fields, i.e. CHF and ONB. Also NBHTC can not be obtained by theoretical methods. These important information of flow boiling curve are procured by experiments.

Usually, series of flow boiling experiments are performed to establish large datasets for developing empirical/semi-empirical correlations of CHF, ONB and NBHTC or constructing their appropriate lookup tables (LUTs) such as the 2006 CHF LUTs of Groeneveld et al. (1996, 2007). In light of experiment complexity, most experiments of flow boiling employed uniformly-joule-heated tubes to serve as heat sources of internal flow. To obtain the intrinsic features of external flow boiling, annulus flow heated by insertion heater rods are adopted in some studies (Rogers et al., 1982; Haas et al., 2013). Besides the circular tubes, channels of different geometries were also studied including rectangular channel (Oh & Englert, 1993) and semi-circular
tube (See & Leong, 2020). The most common fluid of flow boiling is deionized water in 75% of flow boiling experiments. However, it is tough and expensive to perform flow boiling experiments of water under the static pressure of water beyond 3 MPa. Other simulant fluids are used to replace water in some flow boiling experiments, to reduce the facilities’ requirements of experiments and investigate the triggering mechanisms of CHF by scaling laws of fluid similarity for example H. Zhang et al. (2007) investigated the dynamics behaviors of bubbles and vapors that are upon the step-wise increase of heat flux up to CHF in the flow boiling of FC-72 under the highly subcooled conditions. To support the probing study of the vertical-upward thermal flow in LWRs, most experimental schemes also adopted the vertical flow boiling heated by uniform tube heater. But experimental studies of horizontal flow boiling are needed by CANDU-type reactors for the effect of orientation on CHF (Merilo, 1977). The material-dependent flow boiling is not systematically studied in an experimental manner even though many flow boiling experiments have been performed for particular topic-oriented goals.

UNM experimental facilities of boiling heat transfer are constructed to support the systematic studies of material-dependent flow boiling for performance evaluations of ATF thermal hydraulic metrics. In our flow boiling experiments, the deionized water that gains heat by either direct joule heating or indirect joule heating flows up-vertically throughout the tested tube under various flow boiling. It is also noted that the inner diameter of tested tube is greater than 3 mm in this study. Because Kandlikar et al. (2013) proposed that the smallest channel dimension of conventional channel is beyond 3 mm.

2.2 Experimental Boiling Heat Transfer Facilities

2.2.1 UNM Pool Boiling Chamber

In the pool boiling experiments of ATF claddings and other commercial alloys, the tested specimens of cladding materials are placed vertically and submerged in the
deionized water to simulate the pool boiling heat transfer of single fuel rod. The pool boiling experimental study of this dissertation only focuses on the pool boiling experiments on FeCrAl alloys and other commercial alloys under the atmospheric pressure of Albuquerque (~ 84 kPa and its saturation boiling temperature of water ~ 94.5 °C).

To reduce the experimental costs and efforts, the author of this dissertation updates and modifies the test section on the basis of pool boiling chamber designed by Dr. Amir Ali (see more details in Ref. (Ali et al., 2018, 2020)). The pool boiling chamber was manufactured and fabricated by the UNM machining shop. As shown in Fig.2-1, the pool boiling consists of two thermocouple feedthroughs, a surface condenser, two sight glass windows, two immersion cartridge heaters, two power feedthroughs. Also, two pad heaters are attached and pasted on side walls of boiling chamber to maintain the bulk temperature of working liquid and balance thermal loss to ambient environment.

Figure 2-1: Pool boiling chamber design: (a) bottom view of CAD design, (b) top view of CAD design and (c) physical view

The test section configuration is upgraded to support the indirect joule heating. The CAD design scheme is shown in Fig.2-2. Two power feedthroughs transmit the power via two copper clamping terminals to heat up the tested section with effective heated length of 2" and outer diameter of 0.375".
2.2.2 UNM Flow Boiling Facilities

The system layout of UNM flow boiling loop is scheduled as shown in Fig.2-3 and the physical view of UNM flow boiling loop is shown in Fig. the steady-state and transient experiments of flow boiling are performed in the flow loop. This flow boiling facility is composed of these following components:

- a pressurized water tank isolating steam from the water flow;
- a circulation pump driving flow and controlling mass flow rate;
- a preheater adjusting the inlet temperature of working fluid;
- a chiller cooling down the system temperature;
- a test section for testing different cladding materials;
- a DC programming power supply providing heat to tested materials by joule heating;
- different instrumentations measuring pressures, temperatures and mass fluxes, transducers for obtaining currents and voltage drop in the test section.

The primary pipes of the loop are made of SS316 and the working fluid is either deionized water or R134a.
In order to protect the DC programming power supply from being wetted, it is encased by a air-cooled plastic box. Several safety shields of tempered glass are placed surrounding the entire test loop of flow boiling to prevent hot-water splashing or steam explosion under high pressure conditions. The unistrut frame holding key components of flow boiling loop stands in a square barrel of water containment.

The pressurized water tank is customized to allow two different roles in the system of flow boiling, vapor isolation from liquid flow and system pressurization up to designated level. The tube side of U-tube heat exchanger mates to the water tank by using a flange connection. This part is used for vapor condensation in the water tank. The chilled water from the central HVAC system of building flows through the tube bundle. The flow rate is controlled by a variable flow control valve to adjust the cooling efficiency of the U-tube heat exchanger. A nitrogen driven hydraulic accumulator connects to the bottom of water tank via a pressure-tolerant pipe and keeps the hydraulic fluid pressurized up to the designated level. And the hydraulic pressure of fluid is adjusted by varying the charged amount of nitrogen. A pressure relief valve with the upper limit of 3 MPa is mounted on the water tank to maintain the operation safety of system pressure.
The circulation pump drives the fluid flowing upward vertically throughout the tested tube. The mass flow rate driven by the circulation pump is directly controlled by the variable frequency inverter. This inverter receives and sends messages via an IBUS port linking to a host computer. A bypass flow loop is configured in parallel and connected to the primary pipeline of circulation pump. The purpose of bypass flow configuration is to protect the circulation pump from being damaged. When the flow path may be accidentally blocked but the pump still keeps running, with nowhere to flow, fluid could create too much pressure resulting in a stalled engine— or an explosion. But before that happens, the bypass flow loop can vent out the dynamic pressure of fluid and divert the fluid back to the reservoir zone of pump. The circulation pump was purchased from SAMJIN Co., Ltd (CC2A-1106-2SP) and the inverter was purchased from Hitachi, Ltd (NE-S1 Inverter). The full capacity of pump is 66.6 liter/min and the maximum head is 10.0 m. The maximum allowable mass flux of water at the tested section can reach up to 4000 kg/m²s.

A preheater with a PID control panel (from Watlow Electric Manufacturing Co.) is installed on the test loop of flow boiling. This preheater works with a chiller to adjust the inlet temperature of fluid. According to the working limit of the preheater, the maximum inlet temperature of fluid is set the saturation temperature of working fluid. If the preheater is shut down, then the minimum inlet temperature of fluid can be reached by using the chiller only down to 10°C. The cooling efficiency of chiller is adjusted by controlling the volumetric flow rate ball valve. For the superheated flow boiling experiments such as dryout experiments, an extra tube heater is added right before the tested section to generate two phase flows with different inlet vapor qualities. It should be noted that there is a flow rate valve at the outlet of preheater. This valve can work with the pump inverter to control the mass flux passing through the tested tube.

A DC programming power supply provides the tested tube with heat by either indirect or direct joule heating method. A pair of copper terminals are clamped on two ends of tested tube and copper cables transmit the electric energy from the power supply to the copper terminals. The customized LabView Interface sends and receives
messages via a RS485 cable linking between the host computer and the power supply. It is noted that the heated length, the wall thickness and the electrical resistance of tested tube decide the total ohmic resistance across the heater tube $R_e$ as follows

$$R_e = \rho_e \frac{4L_{heated}}{\pi(D^2 - (D - 2\delta_{th})^2)}$$  \hspace{1cm} (2.1)$$

where $\rho_e$ is the specific electrical resistance of material, $L_{heated}$, $D$ and $\delta_{th}$ are the actual heated length, the outer diameter and the wall thickness of heater tube respectively. The maximum thermal power $P_{up}$ is limited by the power capacity limit once the heater tube is selected. The rough calculations of maximum thermal power and its power incremental ($\Delta P$) are dependent on which control mode is utilized in the LabView Interface. For the voltage step-wise control, $P_{up}$ and $\Delta P$ are given as follows

$$P_{up} = \frac{U_{max}^2}{R_e} \leq P_{max}$$ \hspace{1cm} (2.2a)$$

$$I_{up} = \frac{U_{max}}{R_e} \leq I_{max}$$ \hspace{1cm} (2.2b)$$

$$\Delta P \approx \frac{2\Delta U}{R_e}$$ \hspace{1cm} (2.2c)$$

where $U_{max}$, $I_{max}$ and $P_{max}$ are the maximum allowable voltage, current and power in the power supply respectively, $I_{up}$ is the possible upper limit of current appearing in the voltage control mode, $\Delta U$ is the incremental step of voltage, $U$ is the measured voltage drop across the heater tube. For the current step-wise control, $P_{up}$ and $\Delta P$ are given as follows

$$P_{up} = I_{max}^2 R_e \leq P_{max}$$ \hspace{1cm} (2.3a)$$

$$U_{up} = I_{max} R_e \leq U_{max}$$ \hspace{1cm} (2.3b)$$

$$\Delta P \approx 2\Delta I R_e$$ \hspace{1cm} (2.3c)$$

where $U_{up}$ is the possible upper limit of voltage appearing in the current control mode, $\Delta I$ is the incremental step of current, $I$ is the measured current flowing through the
heater tube. To prevent the tripping of over voltage point and/or over current point, any of inequalities Eq.2.2a,2.2b,2.3a and 2.3b can not be overridden anytime. In practice, the current control mode of power generates a finer incremental step of power than that of the voltage control mode. However, the current control mode can trigger the over voltage point more likely than the voltage mode because the CHF occurrence may result in the heater broken or damage, leading to the over voltage point under the current supply mode of power.

Different instrumentations are adopted to measure and sample experimental data at the tested section including the temperature, pressure, and thermal power. Eight K-type surface temperature thermocouples are placed axially along the outer cladding surface of tested materials to measure the surface temperatures of cladding tubes, two T-type thermocouples and two pressure transducers are mounted on both the inlet and the outlet respectively to monitor the state properties of water, and a voltage-drop sensor and two Hall current sensors are installed on both ends of tested cladding tube to provide the joule heating power. Several other pressure transducers and thermocouples are mounted elsewhere upon the flow boiling loop to provide the monitoring information for the self-regulation and automation operation of experiment system.

2.2.3 Steady-State and Transient Experiments

In this dissertation study, the flow boiling experiments under both steady-state and power transient conditions will be performed under the atmospheric pressure of Albuquerque (84 kPa) at the inlet subcooling varying from 0°C to 50°C, and the mass flux from 200 kg/m²s to 3000 kg/m²s across a wide variety of cladding materials. The tested materials have a wide parametric range of thermal conductivity ($k$), thermal mass capacity ($\rho C_p$), and thermal emissivity ($\varepsilon$) and they are commonly used in thermal energy management systems. The more details about the tested materials can be found in Section 2.3 of this chapter.

To remove the surface contamination, the cladding materials are rinsed with acetone, methanol and distilled water consequently. Before directly applying power to the test section, the working fluid is heated up to 94°C for at least 30 minutes with
the intention of degassing dissolved in air, then the temperature of working fluid is reduced by the chiller and preheater to the specified inlet temperature.

**Steady-State Flow Boiling Experiments:** For the signifier of the steady-state condition, this experimental study adopts the following criterion such that the temperature difference between two consecutive mean temperatures measured at a point is less than 0.5°C, the mean temperature at a point is averaged by a time series of 30 measured temperatures points and all eight measured points are supposed to have the temperature difference less than 0.5°C. If this criterion is satisfied, then the present test section will be treated under the steady-state condition and then the heat flux will be incremented by increasing the DC power current by a step size of 1 A, corresponding to an approximate 4-W power step increase.

**Power Transient Flow Boiling Experiments:** Before directly applying the power transient to the tested specimens, the system is made sure under the steady-state condition. To avoid the power tripping, the peak power increases step by step. For example, the maximum power of the present cycle is 100 W. Then the system will be on hold to reach the steady-state again after the end of the complete transient cycle. Finally once the steady state is confirmed, the new next cycle of power transient will be initiated again at the peak power of 104 W. So on and so forth until the occurrence of power transient CHF.

As for the detection mechanism of CHF in this study, a moment when the temperature difference is greater than 30°C is marked as CHF occurrence. Then the system will immediately shut down the power supply. In order to make sure the surface temperature thermocouples within the measurement limit, the upper limit of cladding surface temperature is fixed at 300°C. Once the cladding surface temperature reaches 300°C, the DC power supply will be cut off with the tested section.

For the simulated power transients, two transient modes are only considered and evaluated for ATF and Zircaloy cladding material, i.e., Eq.6.2 and Eq.6.3 (n = 1 or n = 2). And the Nordheim-Fuchs RIA of Eq.6.2 is approximated by the *Raised Cosine* as follows

\[
q(t; \mu, s) = \frac{q_m}{2\Lambda} \left(1 + \cos\left(\pi \frac{t - \mu}{\Lambda}\right)\right)
\]

(2.4)
for $\mu - \Lambda \leq t \leq \mu + \Lambda$, otherwise, $q(t; \mu, \Lambda) = 0$. $\Lambda$ is the characteristic timescale of power transient, physically meaning that the average neutron generation time, $\mu$ is the time at which the maximum of the transient occurs and $q_m$ is the scaling factor of maximum power. The maximum power of Eq.2.4 is $\frac{q_m}{2\Lambda}$, which physically resembles that of the Nordheim-Fuchs RIA in LWRs.

2.3 Tested Materials

2.3.1 Selections of Tested Materials

There are many different types of alloys applied in the nuclear power plants from fuel-cladding and fuel supporting structures to thermal/biological shields and pressure vessels. The selections of in-core materials account for the requirements of neutronics, irradiation and corrosion behaviors, material thermal-mechanics, material thermal-physical properties and material T-H characteristics. Given that the limited testing capabilities of experimental facility configurations, some materials with electrical resistance and conductance are not within the experimental scope of this dissertation including copper & aluminum, and SiC & BN. In this dissertation, the various series of alloys listed as follows are tested their T-H characteristics across a wide range of flow boiling conditions,

(i) Zircalloys: The zirconium-based amorphous alloys have favorable characteristics in neutronics advantage of low thermal neutron absorption, high hardness, ductility and corrosion resistance including Zircaloy 2, Zircaloy 4, Zirlo and Zr705 (See Tab.2.1 for their chemical compositions). These zircalloys are employed as the fuel cladding materials in the current fleet LWRs. However, the biggest flaw of zircalloys is that the zirconium can fiercely react with the hot water steam at the temperature beyond 1230°C and oxidation of zirconium by water is accompanied by release of hydrogen gas and of exothermic heat. Up to now, most of commercial LWRs are still on the use of UO$_2$-Zircaloy fuel-clad systems. The available experimental data of T-H Zircaloy characteristics are still not sufficient
to support the comprehensive thermal safety assessment of Zircaloy claddings.

<table>
<thead>
<tr>
<th>Element</th>
<th>Zircaloy-2</th>
<th>Zircaloy-4</th>
<th>Zirlo</th>
<th>Zr705</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sn</td>
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<td>1.2-1.7</td>
<td>0-0.99</td>
<td>-</td>
</tr>
<tr>
<td>Fe</td>
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<td>0.18-0.24</td>
<td>0.11</td>
<td>0.20</td>
</tr>
<tr>
<td>Cr</td>
<td>0.05-0.15</td>
<td>0.07-0.13</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Ni</td>
<td>0.03-0.08</td>
<td>-</td>
<td>-</td>
<td>0.025</td>
</tr>
<tr>
<td>O</td>
<td>0.12</td>
<td>0.12</td>
<td>0.11</td>
<td>-</td>
</tr>
<tr>
<td>Hf</td>
<td>&lt;100 ppm</td>
<td>&lt;100 ppm</td>
<td>40 ppm</td>
<td>&lt;4.5</td>
</tr>
<tr>
<td>Zr</td>
<td>balanced</td>
<td>balanced</td>
<td>balanced</td>
<td>95.5</td>
</tr>
<tr>
<td>Nb</td>
<td>-</td>
<td>-</td>
<td>0.98</td>
<td>2.00-3.00</td>
</tr>
</tbody>
</table>

(ii) *FeCrAl Alloys:* As alternative candidates to zirconium-based alloys, these Fe-CrAl alloys (See Tab. 2.2) have better potentials in resistance to high-temperature steam oxidation, material structural strengths, and the resistances to irradiation and chemical corrosion. However, the FeCrAl alloys have higher absorption cross section of thermal neutrons. This makes the energy spectrum of neutrons hardening, resulting in unwanted outcomes including the reactivity suppression and the higher burnup. The T-H characteristics of FeCrAl alloys are of importance to license the ATF-loaded LWRs in the near-term implementation of ATF.

<table>
<thead>
<tr>
<th>Element</th>
<th>B126Y</th>
<th>B136Y</th>
<th>C26M</th>
<th>C36M</th>
<th>APMT</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fe</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Cr</td>
<td>12</td>
<td>13</td>
<td>12</td>
<td>13</td>
<td>21</td>
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<tr>
<td>Al</td>
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<td>6</td>
<td>6</td>
<td>5</td>
</tr>
<tr>
<td>Y</td>
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<td>0.05</td>
<td>0.05</td>
<td>0.05</td>
<td>-</td>
</tr>
<tr>
<td>Mo</td>
<td>-</td>
<td>-</td>
<td>2</td>
<td>2</td>
<td>3</td>
</tr>
<tr>
<td>Si</td>
<td>-</td>
<td>-</td>
<td>0.2</td>
<td>0.2</td>
<td>-</td>
</tr>
</tbody>
</table>

(iii) *Stainless Steels:* Austenitic stainless steels (See Tab. 2.3) are widely used as in-core and surrounding structural materials in current commercial LWR systems. Moreover, the stainless steels are often used as the reference materials in most of
T-H experiments. In the early stage of nuclear energy, the stainless steels were used as claddings to encase the UO$_2$ fuel pellets. This dissertation study adopts the stainless steels as the references basis to compare with T-H characteristics of other cladding materials.

Table 2.3: Typical Composition of Stainless Steels (Mass %)

<table>
<thead>
<tr>
<th>Element</th>
<th>SS304</th>
<th>SS310</th>
<th>SS316</th>
<th>SS321</th>
<th>SS347</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.07</td>
<td>0.10</td>
<td>0.07</td>
<td>0.08</td>
<td>0.08</td>
</tr>
<tr>
<td>Cr</td>
<td>17.50-19.50</td>
<td>24.00-26.00</td>
<td>16.50-18.50</td>
<td>17.00-19.00</td>
<td>17.00-19.00</td>
</tr>
<tr>
<td>Mn</td>
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<td>2.00</td>
<td>2.00</td>
<td>2.00</td>
<td>2.00</td>
</tr>
<tr>
<td>Si</td>
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<td>1.50</td>
<td>1.00</td>
<td>1.00</td>
<td>1.00</td>
</tr>
<tr>
<td>P</td>
<td>0.045</td>
<td>0.045</td>
<td>0.045</td>
<td>0.045</td>
<td>0.045</td>
</tr>
<tr>
<td>S</td>
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<td>0.015</td>
<td>0.015</td>
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<tr>
<td>Ni</td>
<td>8.00-10.50</td>
<td>19.00-22.00</td>
<td>10.00-13.00</td>
<td>9.00-12.00</td>
<td>9.00-12.00</td>
</tr>
<tr>
<td>N</td>
<td>0.10</td>
<td>0.11</td>
<td>0.2</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Fe</td>
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<td>balanced</td>
<td>balanced</td>
<td>balanced</td>
<td>balanced</td>
</tr>
<tr>
<td>Mo</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>2.00-2.50</td>
<td>-</td>
</tr>
<tr>
<td>Ti</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>0.70</td>
</tr>
<tr>
<td>Nb</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>1.0</td>
</tr>
</tbody>
</table>

(iv) Carbon-Iron Alloys: The reactor pressure vessel is usually made of carbon steels in the LWRs applications. The coolability margins of pressure vessel are assessed by the knowledge of the CHF during the in-vessel retention strategy. It is necessary to experimentally investigate the T-H characteristics of carbon-iron alloys.

(v) Nickel-Based Alloys: Nickel alloys are used extensively in nuclear reactor applications because they have high strength and good corrosion properties. However, nickel has some unique nuclear properties (including relatively high thermal neutron absorption cross-sections) that limit its use in power reactor cores. The primary application of nickel alloys is to manufacture the steam generator of LWRs. Besides the nuclear energy applications, nickel alloys are also used as the heating elements in the chemical and petrochemical industries. The T-H characteristics of nickel alloys can be transferred from the nuclear power to the chemical-related industries.
### Table 2.4: Typical Composition of Nickel Alloys (Mass %)

<table>
<thead>
<tr>
<th>Element</th>
<th>Monel 400</th>
<th>Inconel 600</th>
<th>Inconel 625</th>
<th>Inconel 718</th>
<th>Incoloy 800</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ni</td>
<td>63.7</td>
<td>77.38</td>
<td>61.495</td>
<td>53.12</td>
<td>32.0</td>
</tr>
<tr>
<td>Cr</td>
<td>-</td>
<td>16.2</td>
<td>21.739</td>
<td>17.65</td>
<td>21.0</td>
</tr>
<tr>
<td>Fe</td>
<td>2.5</td>
<td>6.3</td>
<td>3.304</td>
<td>18.63</td>
<td>43.8</td>
</tr>
<tr>
<td>Cu</td>
<td>31</td>
<td>0.006</td>
<td>0.151</td>
<td>0.02</td>
<td>-</td>
</tr>
<tr>
<td>S</td>
<td>0.02</td>
<td>0.004</td>
<td>0.002</td>
<td>0.03</td>
<td>-</td>
</tr>
<tr>
<td>C</td>
<td>0.3</td>
<td>0.016</td>
<td>0.021</td>
<td>0.04</td>
<td>0.1</td>
</tr>
<tr>
<td>Si</td>
<td>0.5</td>
<td>0.002</td>
<td>0.101</td>
<td>-</td>
<td>1.0</td>
</tr>
<tr>
<td>Mg</td>
<td>-</td>
<td>0.015</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Nb</td>
<td>-</td>
<td>-</td>
<td>3.271</td>
<td>4.79</td>
<td>-</td>
</tr>
<tr>
<td>Mo</td>
<td>-</td>
<td>-</td>
<td>9.479</td>
<td>3.07</td>
<td>-</td>
</tr>
<tr>
<td>Ti</td>
<td>-</td>
<td>-</td>
<td>0.166</td>
<td>0.86</td>
<td>0.3</td>
</tr>
<tr>
<td>Al</td>
<td>-</td>
<td>-</td>
<td>0.067</td>
<td>0.60</td>
<td>0.3</td>
</tr>
<tr>
<td>Mn</td>
<td>-</td>
<td>-</td>
<td>0.123</td>
<td>-</td>
<td>1.5</td>
</tr>
<tr>
<td>Co</td>
<td>-</td>
<td>-</td>
<td>0.080</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

(vi) **Other Commercial Alloys:** Besides the aforementioned alloys, other commercial alloys including brass, copper, aluminium, titanium and platinum, are also used as the tested materials of cladding materials to extend the scope of material thermal-mechanical properties. This will further allow the experimental investigations towards the effects of material properties on flow boiling performances.

These five different series of metallic alloys have wide applications in the nuclear power plants (NPPs) of LWRs. The steady-state/power transient boiling experiments will facilitate the mechanistic understanding of material properties in boiling heat transfer.

#### 2.3.2 Thermal-Physical Properties of Tested Materials

For two types of SS alloys (SS 304 & SS 316), this dissertation study uses their thermal properties recommended by Bogaard et al. (1993); C. S. Kim (1975), the
thermal-mechanical properties of SS 316 expressed in Eq.2.5,

\[ C_p(T) = 0.1097 + 3.174 \times 10^{-5}T \]  \hspace{1cm} (2.5a)
\[ k(T) = 9.248 + 1.571 \times 10^{-2}T \]  \hspace{1cm} (2.5b)
\[ \rho(T) = 8.0842 - 4.2086 \times 10^{-4}T - 3.8942 \times 10^{-8}T^2 \]  \hspace{1cm} (2.5c)

the thermal-mechanical properties of SS304 expressed in Eq.2.6

\[ C_p(T) = 0.1122 + 3.222 \times 10^{-5}T \]  \hspace{1cm} (2.6a)
\[ k(T) = 8.116 + 1.618 \times 10^{-2}T \]  \hspace{1cm} (2.6b)
\[ \rho(T) = 7.9841 - 2.6506 \times 10^{-4}T - 1.1580 \times 10^{-7}T^2 \]  \hspace{1cm} (2.6c)

where \( C_p(T) \) is in the unit of cal/(g·K), \( k(T) \) is in the unit of W/(m·K), \( \rho(T) \) is in the unit of g/cm\(^3\), and \( T \) is in the unit of K. Both Eq.2.5 and Eq.2.6 are valid from 300 K to 1600 K.

The empirical relation between the thermal conductivity of Zircaloy-2 and cladding temperature \( T \) was proposed by Murabayashi et al. (1975) as follows,

\[ k = 100(0.138 - 3.90 \times 10^{-5}T + 1.184 \times 10^{-7}T^2) \]  \hspace{1cm} (2.7)

where \( k \) is in the unit of W/(m·K) and \( T \) is from 300 K to 850 K. And for Zircaloy-4, Murabayashi et al. (1975) provided the following empirical relation

\[ k = 100(0.113 + 2.25 \times 10^{-5}T + 0.725 \times 10^{-8}T^2) \]  \hspace{1cm} (2.8)

where \( k \) is in the unit of W/(m·K) and \( T \) is from 300 K to 850 K. The specific heat capacity \( c_p \) of Zircaloy-2 is adopted from Thermophysical Properties Database of Materials for Light Water Reactors and Heavy Water Reactors (2006) as follows,

\[ C_p = 255.66 + 0.1024T \]  \hspace{1cm} (2.9)
where \( C_p \) is in the unit of J/(kg·K) and \( T \) is from 273 K to 1100 K. The specific heat capacity \( C_p \) of Zircaloy-4 was measured by Terai et al. (1997) across a wide range of temperature and its relation to temperature was proposed as follows (the relation correlation of drop calorimeter was adopted in this study)

\[
C_p = 23.324 + 8.2402 \times 10^{-3}T + 11.6313 \times 10^{-7}T^2
\]  

(2.10)

where \( C_p \) is in the unit of J/(mol·K) and \( T \) is from 300 K to 1000 K. The conversion of J/(mol·K) to J/(kg·K) for Zircaloy-4 is J/(kg·K) = 1000/91.5263 mol/kg × J/(mol·K). The thermal properties of Zirconium cladding recommended by Fink & Leibowitz (1995) is adopted in this study, as follows, for the thermal conductivity

\[
k = 8.8527 + 7.0820 \times 10^{-3}T + 2.5329 \times 10^{-6}T^2 + \frac{2.9918 \times 10^3}{T}
\]  

(2.11)

where \( k \) is in the unit of W/(m·K) and \( T \) is from 298 K to 2000 K. The \( \alpha-\) phase Zirconium metal has the specific heat capacity correlation being valid from 298 K to 1139 K

\[
C_p = 25.607406 + 6.80168 \times 10^{-4}T + 5.837384 \times 10^{-8}T^2 + 9.13714728 \times 10^{-10}T^3 - \frac{50466}{T^2}
\]  

(2.12)

where \( C_p \) is in the unit of J/(mol·K).

In this dissertation study, the thermal properties of Inconel alloys are from the database of the special metals company \(^1\) and the dependencies of thermal properties on temperature are tabulated for the academic use. Given that their empirical correlations are not provided in the thermal properties database of Nickel-based alloys, this dissertation study employs the spline regression technique to interpolate the thermal properties of interest within the range of measurement temperature. Blumm et al. (2003)

The empirical correlations of thermal properties of FeCrAl alloys are adopted from

\(^1\)URL:https://www.specialmetals.com/tech-center/alloys.html
Table 2.5: Thermal Properties of Nickel-based Alloys: Tabulated Data of Temperature Dependencies

<table>
<thead>
<tr>
<th>Temperature (°F)</th>
<th>Inconel 600 (8470 kg/m³)</th>
<th>Inconel 625 (8470 kg/m³)</th>
<th>Monel 400 (8800 kg/m³)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Thermal Conductivity (Btu/in² h°F)</td>
<td>Heat Capacity (Btu/lb °F)</td>
<td>Thermal Conductivity (Btu/in² h°F)</td>
</tr>
<tr>
<td>-300</td>
<td>N/A</td>
<td>N/A</td>
<td>113</td>
</tr>
<tr>
<td>-250</td>
<td>86</td>
<td>0.073</td>
<td>50</td>
</tr>
<tr>
<td>-200</td>
<td>89</td>
<td>0.079</td>
<td>52</td>
</tr>
<tr>
<td>-100</td>
<td>93</td>
<td>0.090</td>
<td>58</td>
</tr>
<tr>
<td>0</td>
<td>N/A</td>
<td>N/A</td>
<td>64</td>
</tr>
<tr>
<td>70</td>
<td>104</td>
<td>0.106</td>
<td>68</td>
</tr>
<tr>
<td>100</td>
<td>N/A</td>
<td>N/A</td>
<td>70</td>
</tr>
<tr>
<td>200</td>
<td>109</td>
<td>0.111</td>
<td>75</td>
</tr>
<tr>
<td>400</td>
<td>121</td>
<td>0.116</td>
<td>87</td>
</tr>
<tr>
<td>600</td>
<td>133</td>
<td>0.121</td>
<td>98</td>
</tr>
<tr>
<td>800</td>
<td>145</td>
<td>0.126</td>
<td>109</td>
</tr>
<tr>
<td>1000</td>
<td>158</td>
<td>0.132</td>
<td>121</td>
</tr>
<tr>
<td>1200</td>
<td>172</td>
<td>0.140</td>
<td>132</td>
</tr>
<tr>
<td>1400</td>
<td>186</td>
<td>0.145</td>
<td>144</td>
</tr>
<tr>
<td>1600</td>
<td>200</td>
<td>0.149</td>
<td>158</td>
</tr>
<tr>
<td>1800</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>2000</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

N/A: Not Applicable

Handbook on the Material Properties of FeCrAl Alloys for Nuclear Power Production Applications (Field et al., 2018) to evaluate their T-H characteristics affected by the material properties. Considering that the surface temperature of ATF claddings is less than their Curie temperatures $T_c$, the empirical correlations of thermal properties of FeCrAl alloys are approximated in the form of third-order polynomial by regression fitting techniques from 300 K to $T_c$, as follows,

\begin{align}
C_p(T) &= aT + bT^2 + cT^3 \quad (2.13a) \\
k(T) &= A_1T^2 + A_2T + A_3 \quad (2.13b)
\end{align}

where $C_p$ and $k$ are in the units of J/(kg·K) and of W/(m·K) respectively, $T$ is K, and the polynomial coefficients are tabulated in Tab.2.6. Tab.2.6 can provide the essential parameters for the numerical evaluations to T-H characteristics of ATF claddings.

The empirical correlations and/or tabulated datasets of thermal properties of other materials are briefly described in this dissertation study. The thermal conductivities of aluminum and of copper are from the technical report of Hust & Lankford (1984). The specific heat capacities of Aluminum and of Copper are based on the experimental measurements done by Takahashi et al. (1989) and Stevens & Boerio-Goates (2004) respectively.
Table 2.6: Polynomial Coefficients of Eq.2.13

<table>
<thead>
<tr>
<th>Alloy</th>
<th>Kanthal APMT</th>
<th>CO6M</th>
<th>C35M</th>
<th>C36M</th>
</tr>
</thead>
<tbody>
<tr>
<td>a</td>
<td>2.540 ± 0.003</td>
<td>2.430 ± 0.002</td>
<td>2.450 ± 0.003</td>
<td>2.995 ± 0.006</td>
</tr>
<tr>
<td>b</td>
<td>-4.311 ± 0.011</td>
<td>-3.957 ± 0.006</td>
<td>-4.002 ± 0.010</td>
<td>-5.953 ± 0.021</td>
</tr>
<tr>
<td>c</td>
<td>2.982 ± 0.009</td>
<td>2.656 ± 0.004</td>
<td>2.720 ± 0.008</td>
<td>4.516 ± 0.018</td>
</tr>
<tr>
<td>Te</td>
<td>852 K</td>
<td>888 K</td>
<td>870 K</td>
<td>771 K</td>
</tr>
<tr>
<td>A₁(×10⁻⁷)</td>
<td>-7.223 ± 0.128</td>
<td>6.762 ± 0.155</td>
<td>-19.860 ± 12.980</td>
<td>-9.184 ± 26.500</td>
</tr>
<tr>
<td>A₂(×10⁻³)</td>
<td>1.563 ± 0.211</td>
<td>1.032 ± 0.256</td>
<td>1.537 ± 0.214</td>
<td>1.368 ± 0.437</td>
</tr>
<tr>
<td>A₃</td>
<td>6.569 ± 0.738</td>
<td>9.956 ± 0.895</td>
<td>8.502 ± 0.747</td>
<td>8.187 ± 1.526</td>
</tr>
</tbody>
</table>

2.4 Conclusions

This chapter provides a detailed explanation on the experimental facilities and the operation procedures of steady-state pool/flow boiling experiments. The selections of tested materials are also elaborated as why these materials are chosen in this dissertation study. And also their material thermal-physical properties are technically prepared for evaluating the effects of material on T-H characteristics.
Chapter 3

Recent Progresses on T-H Evaluations of ATF Cladding Materials

T-H experiments have been on progress to evaluate characteristics of ATF candidates and further support the core-loading of these advanced ATF concepts for LWRs. This chapter covers recent experimental studies of ATF claddings and summarizes some significant results. Recent T-H experimental results of ATF cladding candidates demonstrated that the material thermal-physical properties have pronounced effects on CHF. Such effects have not been well resolved in the current physics-governing models. These effects of cladding materials on CHF could be potentially rationalized by the material-conjugated boiling heat transfer, where the material-dominated heat conduction competes with the flow boiling-dominated heat convection. As progressively increasing the mass flux and/or liquid subcooling, CHF differences between different materials gradually decrease, which is in agreement with the recent T-H experimental results of ATF claddings. This is because the dominance of heat convection over heat conduction is primarily improved by liquid subcooling and/or mass flow rate. It is noteworthy that surface oxidization of claddings may have unknown impacts on T-H performances because the formation of oxide layers on cladding sur-
faces introduces variations of material thermal-physical properties and then changes the surface morphological features, which results in some difficulties in evaluating the thermal safety margins of ATF claddings\textsuperscript{1}.

### 3.1 Prologue

The replacement of the traditional zirconium-based cladding has been driven by the incoherent safety concerns resulting from the triggering mechanisms of nuclear reactor disasters at the Fukushima-Daiichi power plant (Terrani, 2018). The ATF fuel program was initiated by the U.S. DOE to advance the safety integrity of fuel assembly for commercial LWRs. Three alternative materials were identified as promising candidates of ATF claddings to encase uranium-based fuel pellets, i.e., chromium-coated zircalloys, FeCrAl alloys, and SiC/SiC\textsubscript{f} matrix composites (Terrani, 2018). The ATF cladding candidates have gained a comprehensive understanding of material behaviors under the irradiation and corrosion tests (Field et al., 2017; Copeland-Johnson et al., 2020; Duan et al., 2017). Another high priority of ATF cladding candidates is to address their corresponding thermal-physical properties and T-H performances. This helps demarcate the thermal safety margins of ATF-loaded reactor cores in LWRs including peak cladding temperature (PCT) and CHF. The temperature dependent thermal physical properties of ATF cladding materials have been tabulated or presented in empirical correlations to support the numerical T-H evaluation of ATF claddings (Field et al., 2018; Snead et al., 2017). The up-to-date experimental evaluations of ATF claddings’ T-H performances may not be able to sufficiently support the core-loading of ATF by the midst of 2020 and the future licensing of ATF-load LWRs. This leads to the necessary understanding of how the ATF candidates adapt to the zircaloy-based designs of current commercial LWRs with no/little modification of the current operation protocols.

Experimental and mechanistic investigations of CHF phenomena have been across

\textsuperscript{1}This chapter is adopted from one of author’s publications: M. He., J. Wang., and M. Chen., Recent progresses on thermal-hydraulics evaluations of accident tolerant fuel cladding materials, *Annals of Nuclear Energy*, 2021, vol. 161, article ID 108391.
a wide parametric range of pressure, mass flux and equilibrium quality since the advent of LWRs. Boiling experiments under different geometrical configurations of flow channel allow the extension of knowledge application from the lab scale to the component scale of LWRs (Mishima & Nishihara, 1987; Pioro et al., 1999). The continuously increasing number of various experiments expands the data bank of CHF and facilitates the understanding of physics rationales behind CHF. These efforts and progresses make it possible that CHF could be accurately predicted based on the large database of CHF using different methodologies. However, the material dependent study of T-H performances started gaining attentions after the U.S. DOE advanced fuels campaign initiated. For example, Shirvan (2020) indicated that ATF claddings have higher allowable PCT limits and considerable longer survival chance of CHF. The gap differences of T-H performances contributed by cladding materials should be paid attention with the approaching deployment of the near-term and long-term concepts thereafter.

Recent experimental progresses of ATF cladding T-H performances have been made for their potential impacts on boiling heat transfer. But comparative differences between traditional and ATF claddings are not assessed in a detailed manner and some important gaps that were missed in present experiments are identified for the future closures. The rest of this paper is organized as follows: Section 3.2 discusses the T-H experimental results of traditional and ATF claddings, the effects of oxide layer on T-H performances are discussed in Section 3.3. The surface effects of coating/oxide layers on pool boiling heat transfer are briefly discussed in Section 3.4. Section 3.5 presents the possible topics of MC-BHT for the future studies and some conclusions are listed in Section 3.6.

3.2 Boiling Heat Transfer of ATF

Over the past decades, stainless steels have been used as common heater materials in the T-H experiments of boiling heat transfer and only a few studies of the T-H performances have been done using different cladding materials. This topic started
gaining the attention till the recent T-H experiments of ATF materials.

### 3.2.1 Pool Boiling Experiments

Several studies of material-dependent pool boiling experiments (Golobič & Bergles, 1997; Raghupathi & Kandlikar, 2017) demonstrated that pool boiling CHF is dependent on materials’ thermal-physical properties. The effect of the thermal-physical properties was not well understood while the roles of the liquid properties, surface morphologies, and T-H conditions were well explored and investigated in pool boiling experiments. Experimental pool boiling evaluations of different candidates of ATF claddings and their improved variants were performed in recent years. Pool boiling CHF experiments using other common materials (Mei et al., 2018; Raghupathi & Kandlikar, 2017) were also conducted.

Prior to ATF concepts, mainstream materials of nuclear fuel cladding were zirconium based alloys (zircaloys 2&4, Zirlo and etc). CHF enhancement by morphological modification of wall surface was a potential topic of boiling heat transfer. Ahn et al. (2010); Ahn, Lee, et al. (2012) explored the enhancement possibility of the pool boiling CHF by the presence of micro/nanoscale structure on zircaloy-4 surface and attributed the CHF enhancement to capillary wicking ability of artificial microstructure. Besides introducing micro/nano structure to cladding material surfaces, the nano-particles that are chemically and physically compatible with extreme environment of LWR are used as nanofluid additives including ZrO$_2$ to change the thermal-physical properties of water coolant and result in higher CHF (Sarafraz et al., 2016). However, either the introduction of micro structure or the application of nanofluid have not been practically applied to LWRs yet. It is noteworthy that CHF difference between zirconium based alloys could be significant even if they have similar chemical compositions. For example, pool boiling CHF of zircaloy-4 (Ahn et al., 2010) is 1004 kW/m$^2$ ($\theta_r$:49.3°) while that of Zirlo (Jo et al., 2019b) is 645 kW/m$^2$ ($\theta_r$: 63°), such CHF difference gap cannot be explained by the role of wettability using Kandlikar CHF model (Kandlikar, 2001). Since the advent of ATF concept, the difference gaps of CHF, ONB and NB-HTC contributed by materials’ thermal-
physical properties have been attracted some attentions. In this study, the saturated pool boiling CHF experiments of ATF cladding candidates were summarized in Tab. 3.1 and their experimental CHF data results were collected and compiled for understanding of material impacts on pool boiling CHF (See Fig. 3-1). Kandlikar (2001) improved the Zuber model to predict CHF of saturated pool boiling considering the effects of surface wettability, as follows,

\[
q''_{\text{CHF}} = \Delta H_f \rho_g 0.5 [\sigma G (\rho_l - \rho_g)]^{0.25} \frac{1 + \cos \theta_r}{16} \left( \frac{\pi}{2} + \frac{\pi}{4} (1 + \cos \theta_r) \cos \phi \right)^0.5
\]  

(3.1)

where \(q''_{\text{CHF}}\) is the pool boiling CHF, \(\Delta H_f\) is the latent heat of vaporization, \(\rho_g\) and \(\rho_l\) are the vapor and liquid densities of working fluid, \(\sigma\) is the liquid surface tension, \(G\) is the gravitational acceleration constant, \(\phi\) is the surface inclination angle and \(\theta_r\) is the receding contact angle. As implied by Eq. 3.1, the Kandlikar model divides the mechanistic rationales of pool boiling CHF in the fluid’s thermal-physical properties and surface wettability. The Kandlikar model is used to assess pool boiling CHF of ATF cladding candidates. As shown in Fig. 3-1, the Kandlikar pool boiling CHF model (Kandlikar, 2001) can not explain the parametrical trend of pool boiling
candidates, they investigated the effects of heater materials on pool boiling CHF. S
‡ DC: direct current, RF: radio frequency.
N/A: not applicable, SS: stainless steel, PVD: physical vapor deposition, OD: outer diameter, WT: wall thickness, HL: heated length, DC: direct current, RF: radio frequency.

Table 3.1: Recent Saturated Water Pool Boiling Heat Transfer Experiments of ATF and Traditional Claddings

<table>
<thead>
<tr>
<th>Reference</th>
<th>Test Samples</th>
<th>Coating/Substrates</th>
<th>Re</th>
<th>Pr</th>
<th>Others</th>
</tr>
</thead>
<tbody>
<tr>
<td>Kim et al. (2018)</td>
<td>stainless plates</td>
<td>25.1 ± 0.1 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>3 0 ± 0.32°C</td>
<td>N/A</td>
</tr>
<tr>
<td>Koo et al. (2015)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Koo et al. (2015)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>Je et al. (2015)</td>
<td>Zirlo &amp; SiC plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>G. R. See et al. (2015)</td>
<td>two horizontally-placed tubes</td>
<td>OD (14.1) WT (5.0) HL (33.9) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>G. R. See et al. (2015)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>G. M. Song et al. (2018, 2015)</td>
<td>horizontally-placed tubes</td>
<td>OD (5.0) &amp; WT (2.5) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Ali et al. (2018, 2020)</td>
<td>vertically-placed tubes</td>
<td>OD (3.5) WT (1.0) HL (3.0) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>H. H. Son &amp; Kim (2019)</td>
<td>horizontally-placed tubes</td>
<td>OD (9.52) &amp; WT (0.76) mm</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>H. H. Son et al. (2018)</td>
<td>SS 304 plates</td>
<td>10 3 x 10 3 x 0.01 mm</td>
<td>4 3±7 -0.07</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>H. H. Son et al. (2018)</td>
<td>SS 316 plates</td>
<td>10 3 x 10 3 x 0.01 mm</td>
<td>4 3±7 -0.07</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>H. H. Son et al. (2019)</td>
<td>SS 316 plates</td>
<td>10 3 x 10 3 x 0.01 mm</td>
<td>4 3±7 -0.07</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>H. H. Son et al. (2019)</td>
<td>SS 316 plates</td>
<td>10 3 x 10 3 x 0.01 mm</td>
<td>4 3±7 -0.07</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>G. C. Lee et al. (2019)</td>
<td>stainless plates</td>
<td>25 3 ± 0.1 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>H. H. Son, Hong, Soo, &amp; Kim (2016)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>H. H. Son, Hong, &amp; Soo (2017)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>H. H. Son et al. (2018)</td>
<td>horizontally-placed tubes</td>
<td>OD (9.52) WT (5.0) HL (33.9) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>H. H. Son et al. (2018)</td>
<td>vertically-placed tubes</td>
<td>OD (3.5) WT (1.0) HL (3.0) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Yoon et al. (2019)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>C. Y. Lee et al. (2015)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>0 0 ± 0.19°C</td>
<td>N/A</td>
<td></td>
</tr>
<tr>
<td>Song et al. (2018, 2011)</td>
<td>horizontally-placed tubes</td>
<td>OD (14.1) WT (5.0) HL (33.9) mm</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>N. Kim et al. (2018)</td>
<td>Zirlo plates</td>
<td>25 3 ± 0.1 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>N. Kim et al. (2018)</td>
<td>Zirlo plates</td>
<td>25 3 ± 0.1 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>Raghupathi &amp; Kandlikar (2017)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
<tr>
<td>Tachibana et al. (1967)</td>
<td>Zirlo plates</td>
<td>10 3 ± 0.8 mm²</td>
<td>6 0 ± 0.39°C</td>
<td>8 9±9 -0.024</td>
<td>N/A</td>
</tr>
</tbody>
</table>

* N/A: not applicable, SS: stainless steel, PVD: physical vapor deposition, OD: outer diameter, WT: wall thickness, HL: heated length, DC: direct current, RF: radio frequency.
CHF of ATF materials with respect to $\theta_r$. Although the variations of surface morphological features induced by different coating methods (DC-sputtering, cold spray, PVD-sputtering and etc) can be rationalized by surface wettability that is usually characterized by contact angles (Yang et al., 2006; Khorasani et al., 2005), the other features of wall surface, such as porous and/or micro/nanoscale engineered structures (Ahn et al., 2010; Ahn, Lee, et al., 2012), thermal-physical properties of heater materials, and composite layers (material-coating and formation of oxide layer), are still not well understood mechanistically and experimentally. Besides these pronounced effects of salient surface features on pool boiling CHF, the geometry shape and dimension of heater element also has noticeable impacts on pool boiling (M. He & Lee, 2019). The inconsistency of surface morphological features, heater sizes and shapes, and experiment setups may be also the sources of pool boiling CHF difference between ATF cladding candidates. Therefore, the systematic experiments of ATF cladding materials will be needed to target at the effect of thermal-physical proprieties of cladding materials on pool boiling heat transfer only.

Saturated pool boiling experiments using various ATF cladding candidates have been conducted to validate their thermal superiority over the traditional claddings. Kam et al. (2015) coated SiC by PVD-sputtering and Cr by electroplating respectively on stainless steel (SS) plates (the thickness of coating layer is dependent of coating process time), and measured pool boiling CHF of these ATF-coated SS plates in comparison with pool boiling CHF values of bare SS and zircaloy-4 plates. Their pool boiling CHF experimental results showed that SiC-coated SS plates have higher CHF values than bare SS and zircaloy-4 plates but Cr-coated SS plates have lower CHF values. As shown in Fig. 3-2, the Kandlikar CHF model can predict the pool boiling CHF values of bare SS and SiC coated (1$\mu$m-thick) plates accurately within the measurement uncertainty but can not predict the pool boiling CHF for Cr-coated, SiC coated (0.4 $\mu$m-thick) and bare zircaloy-4 plates. This may imply that the thermal-physical properties and thickness of deposited coating and substrate materials would have pronounced effects on pool boiling CHF. G. H. Seo et al. (2015) compared thermal performances of CHF and NB-HTC between horizontally-placed
SiC and zircaloy-4 cladding tubes under saturated pool boiling, and showed that the SiC cladding tube has a better thermal superiority of NB-HTC and CHF than that of zircaloy-4. After occurrence of pool boiling CHF, the SiC cladding could still maintain a structural integrity while the zircaloy-4 failed. Besides the surface-coating materials, the impacts of the coating method and pre-processing environment on pool boiling CHF could be also notable (G. H. Seo et al., 2016b). As shown in Fig. 3-3, the Kandlikar CHF model underpredicts pool boiling CHF of FeCrAl-APMT coated SS 316 under the saturated boiling. The introduction of FeCrAl-AMPT coating to SS plates could have beneficial aspects of thermal performances (G. H. Seo et al., 2016b) while the FeCrAl(Cr) coated Zirlo plates have a lower pool boiling CHF value than that of the bare Zirlo sample (Jo et al., 2019b). As shown in Fig. 3-4, pool boiling CHF values of bare and Cr-coated Zirlo plates are overestimated by the Kandlikar CHF model. This may indicate that there would be some essential effects not being
Figure 3-4: Experimental CHF data of bare SS 304 & Zirlo, of FeCrAl-coated and Cr-coated Zirlo plates (Data reproduced from Jo et al. (2019b))

incorporated by the Kandlikar CHF model including the porous structures and the coating materials. Besides coating ATF materials on plates, G. M. Son et al. (2015, 2018) coated Cr and Cr$_2$O$_3$ on the surface of Nichrome wire heaters and showed that the oxidized samples present different thermal performances from that of non-oxidized counterparts (See Fig. 3-5). Ali et al. (2018, 2020) conducted the pool boiling CHF experiments of zircaloy-4, Cr-coated zircaloy-4, and FeCrAl-C35M and their oxidized samples in LWRs. Their experimental results implied that both the water chemistry condition (See Fig. 3-6(a)) and the irradiation (See Fig. 3-6(b)) affect T-H performances of ATF-cladding materials. One of interesting findings presented by Ali et al. (2020) is that all the irradiated samples become more hydrophobic than the non-irradiated samples. G. C. Lee et al. (2019) coated ZrSi on zircaloy-4 plates, made zircaloy-4 and its ZrSi coated samples oxidized in ambient air and measured CHF of their samples under the saturated pool boiling. Their experimental results
indicated that the oxidized samples of bare or ZrSi coated zircaloy-4 samples have higher CHF and better surface super-hydrophilicity than their non-oxidized counterparts (See Fig. 3-7). H. H. Son, Jeong, Seo, Jeun, & Kim (2016); H. H. Son, Seo, et al. (2017); H. H. Son et al. (2018); H. H. Son, Cho, & Kim (2017); H. H. Son et al. (2019) coated Cr and FeCrAl-APMT on SS plates, sandblasted these coated samples using sandpapers with different grit numbers to permit different surface roughnesses, and evaluated the impacts of surface morphology on CHF. As shown in Fig. 3-8, their experimental CHF results demonstrated that FeCrAl-APMT- and Cr-coated SS samples have higher CHF values their non-coated counterparts. This experiment observation was further confirmed in their later studies of Cr-coated SS plates (H. H. Son & Kim, 2019; H. H. Son et al., 2020) and was in agreement with
the pool boiling CHF experiments Cr-coated SS of N. Kim et al. (2019). This contradicted to the experimental CHF results of Cr-coated SS plates (Kam et al., 2015), which could be attributed to rough surfaces of Cr-coated plates (H. H. Son, Cho, & Kim, 2017; H. H. Son et al., 2019). In this regard, the CHF deterioration stemming from the coating layer of Cr deposited on traditional claddings could be compensated by the CHF enhancement techniques such as surface roughening and micro/nano engineered surface structures (M. He & Lee, 2020).

The pool boiling experiments of ATF cladding discussed above could speak to the incapabilities of present hydrodynamics theories and models in predicting pool boiling CHF across a wide variety of materials. Among these pool boiling CHF experiments, Tested samples of ATF cladding candidates present inconsistencies in surface morphologies, roughness and micro/nano structures. These inconsistencies can obscure the physics insights on the effects of heater materials on pool boiling heat transfer. Importantly, Yeom et al. (2020) conducted the pool boiling CHF experiments on ATF cladding samples with approximately same surface roughness and morphologies. As shown in Tab. 3.2, their experimental results showed that the polished Zirlo plate has much higher CHF than the polished SiC plate while the
Table 3.2: Pool Boiling Experimental Results of ATF Claddings Done by Yeom et al. (2020)

<table>
<thead>
<tr>
<th>Tested Samples</th>
<th>$\theta_s$ ($^\circ$)</th>
<th>$R_a$ ((\mu)m)</th>
<th>PV ((\mu)m)</th>
<th>CHF (kW/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Zirlo (Po)</td>
<td>72±13</td>
<td>0.12± 0.02</td>
<td>3.13± 0.04</td>
<td>645 ± 10</td>
</tr>
<tr>
<td>Zirlo (Po-Au)</td>
<td>52±10</td>
<td>0.21± 0.06</td>
<td>7.56± 2.83</td>
<td>814 ± 65</td>
</tr>
<tr>
<td>Cr-Zirlo (Ad)</td>
<td>107±9</td>
<td>5.72± 0.40</td>
<td>80.93± 9.87</td>
<td>931 ± 94</td>
</tr>
<tr>
<td>Cr-Zirlo (Po)</td>
<td>77±6</td>
<td>0.07± 0.01</td>
<td>3.14± 0.52</td>
<td>558 ± 32</td>
</tr>
<tr>
<td>Cr-Zirlo (Po-Au)</td>
<td>93±5</td>
<td>0.17± 0.02</td>
<td>4.17± 1.17</td>
<td>550 ± 16</td>
</tr>
<tr>
<td>FeCrAl-Zirlo (Po)</td>
<td>86±5</td>
<td>0.07± 0.01</td>
<td>2.94± 0.34</td>
<td>630 ± 14</td>
</tr>
<tr>
<td>FeCrAl-Zirlo (Po-Au)</td>
<td>79±9</td>
<td>0.15± 0.05</td>
<td>7.86± 7.55</td>
<td>737 ± 58</td>
</tr>
<tr>
<td>SiC (Po)</td>
<td>33±6</td>
<td>0.14± 0.01</td>
<td>5.02± 0.49</td>
<td>454 ± 50</td>
</tr>
<tr>
<td>SiC (Po-Au)</td>
<td>77±15</td>
<td>0.15± 0.02</td>
<td>5.36± 0.66</td>
<td>667 ± 84</td>
</tr>
</tbody>
</table>

Noting that the heating size of all samples is 20×20mm², the thicknesses of the Zirlo samples and the SiC sample are 0.8 mm and 1 mm respectively. The maximum peak-to-valley height: PV, polish:Po, as-deposited:Ad, and autoclave test:Au (immersion corrosion in the hot water at 360°C and 18.6 MPa for 360 hrs). The surface wettability of polished Zirlo plate is less than that of polished SiC although the surface roughness and microstructure of the polished Zirlo is closely same to the polished SiC. This significant difference gap of pool boiling CHF can be neither explained by the surface wettability theories of Kandlikar (2001) nor rationalized by the surface roughness role of the nucleate site density theories. It only seems that the CHF difference gap between Zirlo and SiC is contributed by the role of material thermal-physical properties in the MC-BHT.

Tab.3.1 presents the recent saturated water pool boiling experiments of ATF and traditional claddings to address the lateral comparisons of experimental configuration differences. The coating materials, surface roughnesses, contact angles, and heater dimensions could significantly affect NB-HTC and CHF under the pool boiling conditions. Therefore, due to the large variations arising from experimental conditions and/configurations, these pool boiling CHF experimental results of ATF claddings could not sufficiently demystify the sophisticated roles of heating elements of claddings in MC-BHT. However, the pool boiling CHF experiments done by Tachibana et al. (1967); Raghupathi & Kandlikar (2017) still reasonably support that the thermal-physical properties of heater’s materials have unclarified effects on CHF and NB-
The surface wettability, reflecting the intermolecular interactions between heater solid and working liquid, can partially illuminate the complicated effects of heater materials on pool boiling CHF and NB-HTC. However, its derived models, such as the Kandlikar CHF model, fail at the CHF prediction for a wide variety of materials and their variants. This is possibly because the thermal-physical properties of heater material could play a significant role in pool boiling heat transfer process. On the top of pool boiling experiments of various metals and alloys, Tachibana et al. (1967) thought that the purely hydrodynamics theories were insufficient for general predication of CHF. To make the hydrodynamics-based models more applicable and general, several material-related parameters were proposed as correction factors of CHF prediction (Tachibana et al., 1967; Arik & Bar-Cohen, 2003; Guglielmini & Nannei, 1976).

From the standpoint of material conjugate boiling heat transfer (MC-BHT), the dependence of boiling heat transfer on heater material may stem from the competing mechanisms between heat conduction (noting that in this study, heat conduction herein represents heat transfer between heater solid and working fluid through the conduction mechanism) and heat convection. The present models of pool boiling CHF, including hydrodynamics-based model, macrolayer dryout model, hot/dry spot model, and etc., ultimately rationalize the role of heat convection in the occurrence of CHF but do not address the role of heat conduction. Strengthening the dominance of heat convection over heat conduction remarkably can make the conjugate effects of heat conduction negligible and allow the heat-convection dominated theories applicable to CHF prediction.

3.2.2 Flow Boiling Experiments

Increasing of mass flux and/or equilibrium quality improves the dominance of heat convection over heat conduction, which implies that the forced convection and/or subcooled boiling could possibly close the CHF difference gap contributed by the material thermal-physical properties. As shown Fig. 3-9(a), increasing mass flux gradually re-
duces the experimental CHF difference gap contributed by different materials (Zirlo, SS 316, and Inconel 600). This is because the greater shear stress stemmed from increasing mass flux could promote the bubble departure frequency. Based on CHF lookup tables of SS 316 (Groeneveld et al., 2007) and Inconel 625 (H. C. Kim et al., 2000), CHF values of SS 316 and Inconel 625 can be predicted with respect to inlet subcooling through the interpolation method. As indicated in Fig. 3-9(b), the modelling CHF difference between two different materials decreases over the increasing of inlet subcooling. This could be explained that the proportion of natural convection

(a) Impact of various mass fluxes on "CHF-to-Material" sensitivity (Experimental conditions: inlet subcooling of 10°C and 101 kPa, tested material: outer diameter of 0.375 inch and wall thickness of 0.02 inch)

(b) Impact of various inlet subcoolings on "CHF-to-Material" sensitivity (Modeling conditions: mass flux of 500 kg/m²s and 101 kPa, material: outer diameter of 0.375 inch and wall Inconel 600 CHF lookup table of H. C. Kim et al. (2000) and SS 316 CHF lookup table of Groeneveld et al. (2007))

Figure 3-9: Impacts of boiling conditions on "CHF-to-Material" sensitivity

is enhanced by the greater temperature drop across the thermal boundary layer of heated wall. However, except for SS 316, there have not been flow boiling CHF experiments of other tested materials being conducted across a wide range of pressure but under low mass flux and/or high inlet temperatures. This restricts the physical insights into the effect of pressure on the CHF difference between various cladding materials. The role of pressure in heat convection dominated boiling heat transfer is sophisticated because pressure affects many parameters of CHF-triggering mechanisms including thermal-physical properties of working fluids, surface wettability, hydrodynamics instabilities, and bubble dynamics behaviors. It could be anticipated that under the heat convection dominated regime of flow boiling, the effects of
material thermal physical properties on steady-state CHF is negligible (Hata et al., 2007). This may imply that the thermal-hydraulics knowledge procured from SS 316 experiments could be extended to the ATF-cladding loaded PWR core.

For the construction of CHF look-up tables, Inconel alloys including 600, 625, and 718 (Merilo, 1977; H. C. Kim et al., 2000; Pioro et al., 1999), SS 304 and Cu-Ni alloys (Mudawar & Bowers, 1999; Hall & Mudawar, 1999, 2000a,b) were also used to conduct flow boiling CHF experiments. Haas et al. (2013, 2018) experimentally investigated the flow boiling CHF of vertical internally heated zircaloy-4 annuli. Ahn, Kang, et al. (2012) conducted flow boiling CHF experiments on micro-structured Zirlo tubes for probing the effects of liquid spreading on CHF. M. Kim et al. (2018) showed that two ZrSi$_2$-coated zircaloy-4 samples have higher have higher NB-HTC than the plain zircaloy-4 and have 58% flow boiling CHF enhancement at the mass flux of 500 kg/m$^2$s and with the inlet temperature of 96 °C. Besides using ZrSi$_2$-coated zircaloy-4, D. Lee et al. (2021) compared the flow boiling performances differences between Cr-coated zircaloy-4 specimens using PVD-sputtering and cold spray methods, and plain zircaloy-4 at the mass flux of 750 kg/m$^2$s and with the inlet temperature of 24 °C under the atmospheric pressure. Their results showed that the Cr-coated zircaloy-4 samples have 5.2% enhancement of NB-HTC while 11.6% decrement of flow boiling CHF in comparisons with the plain zircaloy-4. This result is in agreement with the saturated pool boiling CHF experiments of Cr-coated zircaloy-4 specimens. D. Lee et al. (2021) thought that the flow boiling performances differences between their tested samples are reasonably resolved by the statistical uncertainties. However, in this paper’s standpoints, such an insignificant difference gap of flow boiling CHF may be attributed to the low inlet temperature of experiment condition ~ 24 °C because the high subcooling of 76 °C can suppress the material-dominated effects of coating on flow boiling CHF. S. K. Lee et al. (2019); S. K. Lee, Lee, et al. (2020); S. K. Lee, Liu, et al. (2020) conducted flow boiling CHF experiments across a wide range of mass flux using ATF cladding and other commerical materials and showed that various materials exhibit appreciable CHF differences at low mass flow rates while increasing mass flux reduces the CHF sensitivity to material and closes NBHTC differences. As G. Su et
al. (2020) reported, the present experimental knowledge of ATF cladding candidates is still too limited to understand how and how much these materials may affect two-phase heat transfer phenomena in LWR conditions. These experimental knowledge that are acquired from flow boiling CHF experiments of various heater materials consolidates the role of heater materials in MC-BHT, that is, when the contributions of heat convection are comparable with those of solid-liquid heat conduction in CHF triggering mechanisms, the CHF difference gaps resulting from various materials are appreciable. Yet, regarding to the competing but conjugated mechanisms between heat conduction and heat convection, the quantitative analyses are still limited and the mechanistical rationales remain ambiguous even controversial.

3.2.3 Quenching Experiments of ATF-Claddings

The quenching experiments are a useful methodology for understanding rapid cooling and investigating post-CHF boiling regimes. Its application to nuclear engineering is to simulate reflooding-water quenching phenomena of fuel cladding by safety injection of emergency core cooling water in the event of DBAs such as LOCA (K.-G. Lee et al., 2019). It is of importance to understand the T-H responses of ATF cladding to the reflooding-water quenching, because the post-CHF film boiling can occur and lead to PCT of ATF cladding beyond their temperature limits during the cladding quenching. The potential candidates of ATF cladding materials have been investigated in water-quenching experiments and there are three key parameters of film boiling regime receiving attentions in ATF cladding quenching experiments, i.e., quenching front speed $v_{qf}$, $T_{MFB}$ and MFB-HF at the MFB point.

In recent experimental water-quenching investigations of ATF-cladding (See Tab. 3.3), the evaluation efforts were put into the effects of material properties, surface morphologies, pre/post-oxidization and liquid subcooling on film boiling regime. And understanding the physics responses of $v_{qf}$, $T_{MFB}$ and MFB-HF to these aforementioned factors could substantially contribute to the system-level simulation of ATF cladding behaviors under DBAs (Seshadri et al., 2018; J.-y. Kang et al., 2019).
i) thermal mechanical properties of cladding material: from the standpoint of transient heat conduction, the quenching duration is implicitly dependent of the volumetric heat capacity and the thermal conductivity (C. Y. Lee & Kim, 2017). Under the saturated water quenching condition, MFB-HF and $T_{MFB}$ are modelled dependent on the solid-to-liquid thermal effusivity ratio. However the ATF-cladding water quenching experiments (C. Y. Lee & Kim, 2017; J.-y. Kang et al., 2018, 2019; Xiong et al., 2020) demonstrated that the effects of thermal mechanical properties of solid/liquid are more complicated than ever expected. The hydrodynamical behavior of vapor layer is also impacted by the thermal effusivity of cladding material for example the large thermal effusivity of FeCrAl results in a thicker vapor film than that of zircaloy-4 (Xiong et al., 2020). This is because the thermal effusivity indicates the material ability to exchange thermal energy with its surroundings and the higher thermal effusivity could lead to the condenser and fiercer nucleation of bubbles and yield the thicker vapor layer. It is noteworthy that the impacts of thermal-mechanical properties of cladding on post-CHF regimes could be alleviated by the dominate heat convection of subcooled water.

ii) surface morphologies: the small micro-scale surface roughness has inappreciable impacts on quenching performances of ATF-cladding materials (Seshadri et al., 2018; K.-G. Lee et al., 2019; Xiong et al., 2020) however the large micro-scale surface roughness has notable effects on quenching behaviors including the quenching duration and $T_{MFB}$ (J.-y. Kang et al., 2018, 2019; Yeom, Jo, et al., 2018; Yeom, Lockhart, et al., 2018; Z. Wang et al., 2021). Besides surface roughness, the porous structures could play a decisive role in enhancing the capillary wicking effects on the Leidenfrost point (C. Y. Lee & Kim, 2017; Seshadri et al., 2018). But for mirror-finished samples, the intrinsic surface wettability is of significance to $T_{MFB}$ and $v_{eq}$ because the better wettability promotes the surface spreading rate of liquid once the quenching starts (Xiong et al., 2020).

iii) liquid subcooling: the parametric trend of $T_{MFB}$ and MFB-HF shows the in-
creasing behavior with respect to increasing of liquid subcooling (J.-y. Kang et al., 2018, 2019; Yeom, Jo, et al., 2018; Yeom, Lockhart, et al., 2018; Z. Wang et al., 2021). As the liquid subcooling increases, the more the heat released out of cladding is used for heating up the subcooled liquid and the less that heat is utilized for evaporating the liquid (Xiong et al., 2020). This mechanistically explains why increasing of liquid subcooling makes the vapor layer thinner (J.-y. Kang et al., 2018, 2019; Z. Wang et al., 2021) and even allows it to vanish under the highly sucooled water (C. Y. Lee & Kim, 2017; K.-G. Lee et al., 2019). Also K.-G. Lee et al. (2019) found that there is no significant difference between bare and Cr-coated zircaloy-4 samples in their quenching processes at the liquid subcooling of 60 °C. This effect of liquid subcooling on the material-dependent film boiling performances resembles the role of subcooling in MC-BHT. And intuitively the increasing of liquid subcooling accelerates the cooling rate and reduces the quenching duration.

iv) surface pre/post-oxidization & material coating: the effects of either surface oxidization or material coating are convoluted arising from variation of thermal mechanical properties of oxide/coating layer and new surface morphologies. Considering that the thicknesses of oxide/coating layer are at micron scales, the oxide/coating layer coupled with the substrate material could have influential and complicated impacts on the quenching processes and performances thereon. For example, the quenching duration of Cr-coated SS is slightly greater than that of bare SS while there is a negligible difference between Cr-coated and bare SS samples in terms of $T_{MFB}$, however differences of $T_{MFB}$ and of quenching duration are evident between the Cr-coated and bare Niobium specimens (C. Y. Lee & Kim, 2017).

Fig. 3-10 presents $T_{MFB}$ of various ATF cladding candidates for the rodlet quenching in the saturated water of 1 atm. Generally speaking, $T_{MFB}$ of FeCrAl claddings is lower than that of plain zircaloy-4 (J.-y. Kang et al., 2018, 2019; Xiong et al., 2020) while the ATF-coated zircaloy-4 samples have higher $T_{MFB}$ than plain zircaloy-4 (Yeom, Jo,
et al., 2018; Yeom, Lockhart, et al., 2018; Seshadri et al., 2018).

Besides the water quenching experiments of ATF-cladding candidates, other accident-simulating based experiments were also conducted to understand the T-H responses of ATF-cladding to different accidental scenarios such as the experimental simulation of droplet impingement to the droplet impact cooling of the uncovered fuel rods (Z. Wang et al., 2019, 2020), oxidation kinetics in high temperature steam for SiC (Avincola et al., 2015), and Cr-coated zircaloy-4 oxidation under the high temperature steam of LOCA (H.-G. Kim et al., 2018; Brachet et al., 2019).

Table 3.3: Recent Water Quenching Experiments of ATF Claddings

<table>
<thead>
<tr>
<th>Author-Year</th>
<th>Cladding Materials</th>
<th>Experiment Conditions</th>
<th>Post-CHF Boiling Parameters</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>C. Y. Lee &amp; Kim (2017)</td>
<td>Rodlets D:10-14mm H:50-65mm</td>
<td>zircaloy, SS 304, Niobium, Copper</td>
<td>Pressure: 1 atm Subcooling: 0°C &amp; 75°C</td>
<td>Studying effects of liquid subcooling, material properties and surface pre-oxidation on film boiling</td>
</tr>
<tr>
<td>Seshadri &amp; Shirvan (2018)</td>
<td>Rodlets D:4.8mm H:50mm</td>
<td>Bare, Cr-Coated (100 μm), FeCrAl-Coated (100 μm), Mo-Coated (100 μm)</td>
<td>Pressure: 1 atm Subcooling: 0°C</td>
<td>Investigating how the surface characteristics resulting from pre-oxidation and irradiation impact on film boiling</td>
</tr>
<tr>
<td>J.-y. Kang et al. (2018, 2019)</td>
<td>Rodlets D:12mm H:60mm</td>
<td>zircaloy-4 FeCrAl-APM SiC-CVD</td>
<td>Pressure: 1 atm Subcooling: 0°C &amp; 17°C</td>
<td>Studying effects of material properties and pre/post oxidization on film boiling</td>
</tr>
<tr>
<td>Yeom, Jo, et al. (2018)</td>
<td>Bulk Flat Plates 12.7 x 12.7 x 2.5 mm</td>
<td>Zr50CoCr50-Coated(50 μm) FeCrAl-Coated(50 μm) MC</td>
<td>Pressure: 1 atm Subcooling: 0°C &amp; 15°C</td>
<td>Studying effects of surface oxidation and morphologies in presented and subcooled conditions</td>
</tr>
<tr>
<td>K.-G. Lee et al. (2018)</td>
<td>Rodlets D:10mm</td>
<td>Cr-Coated (200 μm), SiC-Coated (200 μm), Nie-Coated, Molybdenum Coated</td>
<td>Pressure: 1 atm Subcooling: 0°C &amp; 50°C</td>
<td>Studying effects of Cr coating thickness and pre-oxidation on film boiling</td>
</tr>
<tr>
<td>Xiong et al. (2020)</td>
<td>Rodlets D:10mm</td>
<td>FeCrAl from Kanthal® APM</td>
<td>Pressure: 3 atm Subcooling: 0°C, 10°C, 15°C and 20°C</td>
<td>Studying effects of liquid subcooling, material properties and surface morphologies on film boiling</td>
</tr>
</tbody>
</table>

Noting that the tested samples are dropped up from the furnace down to the quenchant pool, $T_c$ is the initial rod center temperature, $T_s$ is the initial surface temperature of tested material.

In comparison with the material-dependent nucleate boiling, the effects of material...
properties on film boiling are more intricate because, besides the volumetric heat capacity, thermal conductivity of cladding material, the thermal emissivity of cladding is a decisive parameter in the contribution of thermal radiation to film boiling heat transfer across the vapor layer. Seshadri & Shirvan (2018) attributed the higher emissivity of FeCrAl to its film boiling rate enhancement and implied the greater potentials of FeCrAl cladding to decreasing PCT than that of zircaloy-4. Also the lower emissivity could partially explain why Cr-coated zircaloy-4 samples need longer time to reach the initial surface temperatures through the radiant heating (K.-G. Lee et al., 2019). This implies that the role of heater material in the MC-BHT may be sensitive to the boiling regime of interest and the T-H boiling conditions.

3.2.4 Numerical Evaluations of ATF-Loaded Cores

Numerical evaluation is an essential supplement of experimental work as a comparison benchmark and a critical tool to evaluate the ATF performance under the beyond design basis accident conditions. The ATF study’s numerical tools vary from the fuel analysis code FRAPCON/FRAPCAN (Geelhood et al., 2016), sub-channel analysis code COBRA-TF (Salko & Avramova, 2015), to the system level codes TRACE/RELAP5, and severe accident code MELCOR. Besides, the uncertainty quantification codes RAVEN (Rabiti et al., 2017), DAKOTA (Adams et al., 2020) are also used in those evaluations.

Gorton et al. (2019) analyzed the transient CHF experiments of FeCrAl and other alloys. They used RELAP5-3D and RAVEN to determine the sensitivity of CHF and peak test section temperature, an analog to PCT, to heat transfer coefficients, a CHF multiplier, and uncertainties in the thermal conductivity and volumetric heat capacity. Qiu et al. (2018) studied the in-pile mechanical, thermohydraulic and oxidation models of SiCf/SiC cladding, FeCrAl cladding, FCM pellet by a modified FRAPCON and FRAPTRAN. They modeled the key performance parameters under normal and accident conditions. The results show that ATFs possess significant advantages under accident conditions and delay the cladding failure effectively. S. He et al. (2019); S. He & Cai (2020) used COBRA to perform sensitivity analysis to investigate the impact
of ATF claddings and coolant geometry size under RIA condition. They found that PCT would significantly decrease with 10% enhancement of coolant channel lateral length and the heat transfer deterioration was much better, which was the best choice for ATF fuels in RIA. Z. Chen et al. (2018) assessed the effect of various ATF materials, such as FeCrAl and SiCf/SiC on thermal-hydraulic behaviors in RIA. They also performed a sensitivity analysis by sub-channel analysis code COBRA-TF codes to identify the potential Impact of nucleate boiling heat transfer coefficient and CHF. Wu & Shirvan (2020) modified TRACE code to simulate the behavior of FeCrAl cladding and Cr coated zircaloy cladding in three Station Black-Out scenarios. Their simulation results showed that the hydrogen productions from FeCrAl and Cr-coating are one to two orders of magnitude less that those from zircaloy cladding and those generated by FeCrAl claddings are reduced by 37% ~ 48% due to its thinner cladding thickness. J. Wang et al. (2017) modified the MELCOR code with cladding material properties for performance analysis on FeCrAl cladding in SBO. They confirmed that FeCrAl could slow the accident progression and reduce the amount of hydrogen generated due to the slower oxidation kinetics of this metallic alloy (See Fig. 3-11). Feng et al. (2020) investigated the uncertainty quantification (UQ) during the ATF performance evaluation. Feng Feng et al. (2020) invested the UQ source terms such

![Figure 3-11: Simulation results of total hydrogen mass generated in a typical BWR core by MELCOR](image)

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as the oxidation kinetic, cladding, and coating failure temperature. They used the UQ code Dakota to decide the input distribution by the Monte Carlo method. Evaluation criteria include the hydrogen generation initial, cladding failure, hot leg creep rupture, and the core slump times.

Those simulation studies (J. Wang et al., 2017; Qiu et al., 2018; Z. Chen et al., 2018; H. Yu et al., 2020) implied that ATF cladding concepts have superior advantages over the traditional concepts in the prevention and mitigation strategies of DBA occurrences but have no significant improvement of the thermal performances. This may be because those simulations were performed under the dominate convection regions of LWR based on the T-H codes without incorporating the effects of cladding materials on boiling heat transfer. With the T-H experimental progresses of ATF cladding concepts, what roles of the ATF cladding materials are in the T-H performances of LWRs could be possibly rationalized.

3.3 Oxidization Effects on Thermal Hydraulics Performances of Claddings

3.3.1 Formation of Oxide Layers on Cladding Materials

In extreme environments, the cladding materials in LWRs are exposed to high-temperature pressurized water/steam mixtures and high-dose irradiation, which oxidizes the cladding surface and permits the formation of oxide layers. The chemical compositions of oxide layers are determined by the chemical constituents of cladding materials, such as ZrO on zircaloys (de Gabory et al., 2015), FeO-Fe$_2$O$_3$ & Al$_2$O$_3$ & Cr$_2$O$_3$ on FeCrAl alloys (D. J. Park et al., 2015; Terrani et al., 2016), Cr$_2$O$_3$ on Cr-Coated zircaloys (J.-H. Park et al., 2015), SiO$_2$ on SiC/SiC (Terrani et al., 2014), water chemistries (Terrani et al., 2016) as well as the operation conditions of LWRs (Gupta et al., 2018; Rebak et al., 2018). For example, it was found that the chemical compositions and thicknesses of oxide layers are different in various operation conditions, the Al$_2$O$_3$ layer on FeCrAl cladding serves as a protective coat under the LOCA
conditions while the \( \text{Cr}_2\text{O}_3 \) covers the cladding surface under the normal operation (Gupta et al., 2018; Rebak et al., 2018) (See Fig. 3-12). The irradiation could impact the kinetics behaviors of oxide layers (Terrani et al., 2016). The nucleation, growth and departure of steam bubbles around nucleation sites lead to the significant spatiotemporal variations of inter-facial temperature at the solid-liquid-vapor interface, further makes the outer wall surface of cladding undergoing the physical-chemical interactions between cladding materials and water/steam mixtures. Another superiority of ATF cladding candidates is that they are able to maintain the structural and mechanical integrity at the snapshot of DNB/burnout occurrence and sustain the film boiling for a while. This could result in the formation of oxide layers on cladding materials.

In general, the formation of oxide layer and the irradiation-induced changes of cladding materials’ properties lead to the variations of cladding surface wettability (Hong et al., 1994; Umretiya et al., 2020), redistribution and reformation of surface morphologies, and the formation of oxide layers. This implies that in MC-BHT, the effect of material degradation can not be neglected in the material-dominated boiling regime, especially towards the natural-convection/circulation driven thermal systems such as pool-type water reactors and storage pools of spent nuclear fuel.

### 3.3.2 Impacts of Oxide Layers on Pool Boiling CHF

Although SA 508 carbon steels are not ATF cladding concept, they are the mainstream materials of reactor pressure vessel. The impacts of oxide layers on CHF are
evident and significant in pool boiling experiments of SA 508 (J. Lee & Chang, 2012; H. H. Son, Jeong, Seo, & Kim, 2016; H. Chang et al., 2017; Mei et al., 2018; Kam et al., 2018; K. Wang, Erkan, et al., 2018; K. Wang et al., 2019). Several physical rationales are proposed to explain the CHF enhancement of SA 508. For example, porous structures of Fe$_3$O$_4$ nanoparticles produced at the nucleate boiling regime are attributed to pool boiling CHF enhancement of SA 508 (J. Lee & Chang, 2012; H. H. Son, Jeong, Seo, & Kim, 2016; H. Chang et al., 2017; Mei et al., 2018) but the SA 508 samples that are pre-oxidized in air deteriorate pool boiling CHF due to lower heat dissipation of the oxide layer (H. H. Son, Jeong, Seo, & Kim, 2016).

C. Y. Lee et al. (2015) oxidized the wall surface of zircaloy-4 in a furnace up to different target temperatures with fixed heating/cooling rates. As shown in Fig. 3-13, their experimental results implied that the oxidized zircaloy-4 could enhance the pool boiling CHF but there was no significant difference of CHF between oxidized samples. This is because the thickness of oxide layer increases asymptotically over the target temperature and further increasing the oxidization temperature can not significantly increase CHF. That is why oxidized zircaloy-4 samples have almost same CHF values within the measurement uncertainty in the study of C. Y. Lee et al. (2015). N. Kim et al. (2019) oxidized the Cr-coated SS 304 samples and explored that CHF differences between oxidized and non-oxidized Cr-coated samples to investigate the CHF enhancement by surface oxidization. Their pool boiling CHF experiments demonstrated that in comparison with the bare sample, the pool boiling CHF of fresh Cr-coated SS samples are enhanced by up to 31%, and after surface oxidization,
the CHF is further enhanced up to 48%. On the top of their previous studies of the effects of coating layer on pool boiling CHF, Kam et al. (2021) performed experimental investigations to the effects of oxide nanostructures on Zirlo plates on pool boiling CHF using the anodization method to oxidize the Zirlo surface as the protective layer. They controlled the total time of anodization to form the oxide layers with different thicknesses. Their anodization results (Kam et al., 2021) showed that as the anodization time progressively increases from 0 min to 20 min at the incremental step of 5 min, the static contact angle decreases from 50.83°C of the non-anodized sample to 28.50°C of the 20 min-anodized sample. Their pool boiling results (Kam et al., 2021) showed that the parametric trend of pool boiling CHF enhancement increases asymptotically up to 60% with respect to the thickness of oxide layer. Their irradiation studies (Kam et al., 2021) also showed that the 10min-anodized sample that underwent the irradiation of a 200 keV proton beam has less surface wettability than the non-irradiated counterpart, which is in agreement with the irradiation studies of Ali et al. (2020). The experimental results of oxidized and non-oxidized SiC plates presented by Yeom et al. (2020) can further support that the effects of oxide layer on pool boiling CHF are significant due to the thermal-physical properties of oxide layer instead of the surface roughness.

Although the Kandlikar CHF model (Kandlikar, 2001) and its improved variants (Chu et al., 2012; J. Kim et al., 2016) are able to correlate the CHF enhancement by surface morphological factors of oxide layers including surface wettability, capillary wicking, surface roughness and etc, the role of thermal physical properties of oxide layers in CHF enhancement/deterioration still remains vague and there are no systematic investigations towards how the oxide layers are mechanistically coupled with their non-oxidized substrates to affect pool boiling MC-BHT.

3.3.3 Impacts of Oxide Layers on Flow Boiling CHF

The presence of oxide layers could augment/degrade the influence of material-side factors on MC-BHT. K. Wang et al. (2020b) repetitively performed flow boiling CHF experiments on same carbon steel plates without surface treatments and found that
the flow boiling CHF increases over the repetition cycles across the mass flux range from 160 kg/m²s to 640 kg/m²s (See Fig. 3-14). The increasing tendency of flow boiling CHF on temperature-overshooting carbon steel plates was associated with the increasing level of surface oxidation (K. Wang et al., 2020b). Different from the surface oxidization by CHF occurrence, Trojer et al. (2018) pre-oxidized the 18MnD5 low-carbon steel samples in a controlled, high-temperature, and humid-air environment to make boiling surfaces of pre-oxidized samples super-hydrophilic and thus capable of wicking water. In comparison with SS 316, the CHF enhancement of oxidized carbon steel can reach up to 70% under the same flow boiling conditions (Trojer et al., 2018). The significant heat convection contributed by flow boiling condition could reduce the impacts of oxide layers on boiling curves. Under the high mass flux and/or high liquid subcooling influences of oxide layers are negligible. For example Hata et al. (2004) found that there was no influence of surface roughness on flow boiling CHF under the high mass flow rates and S. K. Lee et al. (2019) thought that surface wettability enhanced by formation of oxide layers had limited influence on flow boiling CHF. The dominance of heat convection over the material-side factors mechanistically rationalizes no evident variations of flow boiling CHF experimental data repeatedly procured on FeCrAl C36M cladding at a high mass flux (S. K. Lee et al., 2019).

Apart from the high temperature water/steam mixtures, surface temperature overshooting of local CHF occurrence, and irradiation, the water chemistry environment

Figure 3-14: Flow boiling CHF enhancement with boiling cycle for different mass fluxes (Carbon steel experimental data reproduced from K. Wang et al. (2020b))
(J. Lee et al., 2010; H. M. Park et al., 2014; Kam et al., 2020) also plays a critical role in the formation of oxide layers. Also the presence of additives including tri-sodium phosphate and boric acid introduces the new variations to the thermal-mechanical properties of water coolant and possibly have unknown impacts on the contribution of heat convection to MC-BHT. For example, J. Lee et al. (2010) observed the 21.4% enhancement of flow boiling CHF for the additive of tri-sodium phosphate and 12.4% enhancement for the additive of boric acid at the inlet subcooling of 50°C and the mass flux of 100 kg/m²s under one the pressure of 1 atm. As shown in Fig. 3-12, the presence of oxide layer is also dependent of reactor core states, which means that different oxides determines different T-H performances of ATF claddings. These new concerns arising from oxide layers, water chemistries, and cladding materials make it difficult to analytically resolve the MC-BHT under various operation conditions.

3.4 Beyond the Material Thermal-Physical Properties: Surface Effects

It is known that the cladding surface characteristics, in addition to thermal-physical properties of cladding materials, also play vital roles in their boiling heat transfer performances. These cladding surface characteristics are divided in the hydrodynamics-related factors and the nucleation-related factors. The hydrodynamics-related factors resulting from solid-liquid intermolecular interactions affect the macro-scale behaviors of bubbles, which are indicated by the surface wettability and wickability while the nucleation-related factors have influential impacts on the micro-scale behaviors bubbles. In this review paper, the nucleation-related factors of ATF-coating and oxide layers are only discussed.

3.4.1 Surface Morphologies and Roughness

Given that there are many different surface morphological characteristics that enhance CHF significantly including the micro/nano structured surfaces (Shojaeian &
Koşar, 2015), porous coatings (Mori & Utaka, 2017), and textured surfaces (Ferjančič et al., 2020), the surface morphological characteristics that are related with ATF-coating strategies and oxidization processes are only evaluated in this review paper. Regarding to the roles of micro/nano surface modifications on boiling heat transfer enhancement, the review paper of D. E. Kim et al. (2015) provides comprehensive insights on the look of their physical mechanisms. Fig.3-15 presents the SEM images scanned from the surfaces of the bare, SiC-coated and Cr-coated SS samples respectively and the SiC and Cr are coated on the SS plates by the PVD sputtering method (Kam et al., 2015). As observed in Fig. 3-15, there are no obvious micro-scale surface morphological structures enhancing pool boiling CHF at the resolution scale of 2 μm. Kam et al. (2015) addressed that it was due to the material characteristics of the coating surfaces that pool boiling CHF was enhanced or degraded on the coated samples. Jo et al. (2019b) coated FeCrAl and Cr on Zirlo plates by the cold spray method and the 100 μm-resolution SEM images of the coated and non-coated samples showed that there is no significant difference of the surface parameters, particularly of the surface roughness. Although the surface morphological characteristics of claddings on which ATF materials are coated by cold spray and PVD sputtering did not have significant contributions to the impacts of coating layers on pool boiling CHF (Kam et al., 2015; Jo et al., 2019b), for the Cr&FeCrAl-coated claddings using DC sputtering method (H. Seo et al., 2015; G. H. Seo et al., 2016b; H. H. Son, Seo, et al., 2017; H. H. Son et al., 2018; H. H. Son & Kim, 2019; H. H. Son et al., 2020) those pool boiling experiments attributed the micro-grooved surface structures (See Fig. 3-17) to the pool boiling CHF enhancement because of the wickability improvement by the surface modifications of ATF-coating. The sizes and distributions of these micro-grooved surface structures depends on the DC-sputtering condition settings including the substrate temperature and the deposition time (N. Kim et al., 2019). This speaks to the possibility of having closely same micro/nano surface morphological characteristics for traditional cladding samples upon which various ATF candidates are coated.

The surface coating/oxidization could modify roughness scales and distributions of
Figure 3-15: SEM images of (a) the bare SS, (b) the 400 nm thick SiC coated SS, and (c) the 1 μm thick Cr coated SS (Kam et al., 2015)
Figure 3-16: SEM images of (a) the bare Zirlo, (b) the Cr coated Zirlo, (c) the FeCrAl coated Zirlo, and (d) the bare SS 304 (Jo et al., 2019b)

Figure 3-17: Cross-section views of the micro-grooved surface structures of FeCrAl-coated cladding using DC sputtering at different substrate temperatures (G. H. Seo et al., 2016b)
cladding materials. The role of surface roughness in boiling heat transfer was clarified in these experimental studies (M.-G. Kang, 2000; Alam et al., 2013; J. Kim et al., 2016). The pool boiling CHF experiments of Cr-coated SS 304 specimens (H. H. Son & Kim, 2019; H. H. Son et al., 2020) demonstrated that the Cr-coating layer leads to an increase in surface roughness and the delayed growth of the dry area, and further result in the CHF enhancement due to the greater capillary wickability. Besides the surface coating, the cladding surface oxidization alters the surface roughness, which in turn affects the capillary wicking and wetting characteristics and pool boiling CHF (C. Y. Lee et al., 2015; N. Kim et al., 2019).

3.4.2 Wettability and Wickability

The variations of surface morphological characteristics stemming from the surface coating/oxidization not only influence the nucleation site distributions but also affect the wetting and wicking behaviors of liquid. The intrinsic role of surface wettability in pool boiling heater transfer was clarified based on the super-polished silicon surfaces (Mohammadi et al., 2018) and how the surface wickability affects pool boiling CHF was reported in Refs. (Rahman et al., 2014, 2020).

The Cr/FeCrAl-coated cladding surfaces that are processed by cold spray (Jo et al., 2019b; Ali et al., 2020; Yeom et al., 2020) or PVD method (Kam et al., 2015) could be either more or less hydrophilic than their non-coated substrates while the Cr/FeCrAl-coated surfaces that are processed by DC sputtering are more hydrophilic even super-hydrophilic than their non-coated substrates (G. M. Son et al., 2018; H. H. Son, Seo, et al., 2017; H. H. Son et al., 2018, 2019; H. H. Son & Kim, 2019; H. H. Son et al., 2020; N. Kim et al., 2019). This speaks to that the effects of coating layer on the surface wettability are affected by the surface coating methods. In general, the surface-oxidized cladding materials are hydrophilic than non-oxidized samples (C. Y. Lee et al., 2015; Kam et al., 2021). This allows more safety margin of oxidized cladding in pool boiling type reactors.

In the pool boiling CHF experiments of anodized zircaloy-4 plates (Ahn et al., 2010), it was illustrated that CHF of treated surfaces could not be explained only
with the surface wettability when the contact angles were below $10^\circ$C. The liquid spreading phenomenon associated with the surface wickability was observed on the surfaces with contact angles less than $10^\circ$C, which was interpreted as the primary contribution to CHF enhancement on the highly hydrophilic surfaces. Furthermore, on the super hydrophilic surfaces of Cr-coated samples with the contact angles of $\sim 0^\circ$C (H. H. Son, Seo, et al., 2017; H. H. Son et al., 2018, 2019; H. H. Son & Kim, 2019; H. H. Son et al., 2020; N. Kim et al., 2019), it was thought that the capillary wickability was responsible to the pool boiling CHF difference between Cr-coated samples that underwent various DC-sputtering conditions. H. H. Son, Seo, et al. (2017) showed that the capillary wickability analysis gives better prediction to the monotonic CHF increase with respect to the capillary liquid flow because the low roughnesses of these hydrophilic surfaces don’t affect the pool boiling CHF significantly. By looking at the Cr and FeCrAl coated SS 316 samples, H. H. Son et al. (2019) provided the experimental evidences that the product of $u_s$ (the water capillary flow velocity) and $R_a$ has a better relation of linearity with pool boiling CHF than the contact angles. In Refs. (H. H. Son & Kim, 2019; H. H. Son et al., 2020), the receding behaviors of water capillary flow on super hydrophilic Cr-coated surfaces were analyzed to develop the prediction model of the wicking-enhanced pool boiling CHF accounting for the surface roughness, advancing capillary flow velocity and surface wettability.

### 3.4.3 Implications of Surface Coating/Oxidization

Although the layer thickness of coating/oxide varies from several microns to several dozens of microns, their impacts on boiling heat transfer cannot be negligible under the heat-convection weak boiling conditions. For example, the Cr-coated and pre-oxidized SS 304 samples have $\sim 49\%$ enhancement of pool boiling CHF in comparison with the bare counterpart (N. Kim et al., 2019). This infers that the present best-estimate-plus-uncertainty methodologies may not be able to resolve the variations of thermal safeties resulting from the coating/oxide layers.

The recent experimental boiling studies of ATF-coated and oxidized claddings
demonstrated there are many micro-sale hydrodynamics-related factors behind the pool boiling CHF degradation/enhancement including surface morphological characteristics, and surface wettability and wickability. However these surface effects-based mechanisms cannot rationale the contradictions of ATF-coated experimental results. For example, the pool boiling CHF degradation induced by the Cr-coating layer was attributed to the effects of material thermal-physical properties on MC-BHT (Kam et al., 2021, 2015) while the pool boiling CHF enhancement observed in the Cr-coated samples was primarily contributed by the more porous and textured surface morphological characteristics (N. Kim et al., 2019; H. H. Son, Seo, et al., 2017; H. H. Son et al., 2018, 2019; H. H. Son & Kim, 2019; H. H. Son et al., 2020). From the stand points of interfacial physics, the phase change of of nucleate boiling may have the solid-liquid/solid-vapor heat conduction involved and the heat dissipation capability of coating/oxide layer may have deciding impacts on the irreversible formation of dry patches. Thus, it is still too earlier to draw solid conclusions upon whether either the hydrodynamics-related factors or the thermal-physical factors contribute to the CHF enhancement/degradation or both.

3.5 Missing Gaps of ATF Claddings on Boiling Heat Transfer

From the discussions and analyses on the recent T-H experimental studies of ATF cladding candidates, the missing gaps of ATF-cladding concepts are pinpointed for their future experimental studies to clarify the roles of material properties on MC-BHT:

a) The role of thermal emissivity in quenching heat transfer have seemed to not be addressed over past decades. However, the recent quenching experiments of ATF cladding candidates (Seshadri & Shirvan, 2018; K.-G. Lee et al., 2019) implied that the thermal emissivity might have influential impacts on $T_{\text{MFB}}$ and the film boiling heat transfer because the thermal radiation is the primary
heat transfer mechanism in the film boiling regime. A wide variety of fuel claddings and commercial alloys will be supposed to be tested for clarifying the impacts of thermal emissivity on quenching heat transfer and film boiling. Although the thermal conductivity and capacity of ATF cladding candidates are tabulated for their dependencies of bulk temperature, the thermal emissivity of ATF claddings are still not technically ready. The thermal emissivity of ATF cladding candidates should be measured for the future post-CHF evaluations.

b) To ultimately demystify the effects of material’s thermal-physical properties on CHF, the saturated pool boiling experiments should be performed across a wide variety of different materials including metallic alloys, ceramics and their oxides on the tested samples with closely same surface morphological characterises under the same experimental system configurations. This will experimentally shed light on the mechanistic understanding of material-side factors to MC-BHT.

c) More flow boiling experiments between the selected materials of pool boiling experiments should be performed in comparison with pool boiling experiments to elucidate the effects of mass flux, liquid subcooling and pressure on MC-BHT. Given that the ATF-cladding flow boiling experiments are still too limited to support thermal safety evaluations of ATF-loaded LWRs, the systematic flow boiling experiments of ATF cladding candidates will be much needed to understand their T-H characteristics under the steady-state and DBA conditions.

d) In present pool boiling experiments of material-coated and surface-oxidized samples, it is difficult to tell whether either the variations of material thermal-mechanical properties or the changes of surface morphological characteristics play significant contributions to CHF enhancement/degradation or both of them do. This needs more mechanics clarifications made from the experimental observations to the significant impacts of coating(oxide) layers on MC-BHT even though usually the layer thickness is micro/nano-metrically thin.
With the rapid development of more advanced sensors and instrumentations, the roles of material-side factors in MC-BHT will be utterly demystified for the better management of thermal energy systems.

### 3.6 Conclusions

Pool boiling experimental evaluations of ATF cladding candidates prove their T-H superiorities and provide physics insights into the effects of material properties on CHF but the flow boiling experiments are limited to support the safety margin evaluations of ATF-loaded LWRs based on current protocols of design & operation. The experimental knowledge obtained from a number of flow boiling CHF experiments of ATF-cladding materials indicated that under the dominate regime of heat convection (i.e., the high mass flux and the low equilibrium quality), CHF variations induced by thermal-physical properties, oxide layers and surface morphologies of ATF-cladding candidates could be negligible. This implies that the previous CHF knowledge procured from SS 316 heaters and/or rod bundles could be potentially applied to the safety margin evaluation of ATF-cladding in current commercial PWRs because of highly subcooled boiling and high mass flow rate. However, the heat convection in BWR is less pronounced than that in PWRs due to its less mass flow rate and the higher equilibrium quality, which makes the material-side factors non-negligible for the ATF-loaded BWR cores. It is also noteworthy that under the accident progression scenarios of LWRs including LOCA and LOFA, the ATF cladding candidates may show appreciable effects of material-side factors on their T-H characteristics.

So far, there have been no mechanistic models predicting MC-BHT and the physical explanations for effects of material properties are not universally generalized. For example under the super hydrophilic surface condition, the Cr-coated zircaloy deteriorates pool boiling CHF while the Cr-coated SS enhances pool boiling CHF. This can not be physically resolved by either hydrodynamics or nucleation theories. It could be anticipated that incorporating the effects of materials into MC-BHT requires an accurate prediction of average wall superheat to evaluate the thermal-physical properties.
for the wall solids. This could make the MC-BHT modeling more complicated. In the mean time, MC-BHT is of importance to assess the variations of T-H performances due to the formation of oxide layers or introduction of ATF material coating.

To fully advance the deployment of ATF loading for LWRs, the systematic experimental investigations of ATF cladding candidates will be supposed to be conducted across a wide parametric range of pressure, mass flux and equilibrium quality and under various normal/abnormal conditions. To demystify the effects of material-side factors on boiling heat transfer, a series of pool/flow boiling experiments are required to evaluate a wide variety of heater materials for the development of mechanistic models accounting for the material thermal-mechanical properties. This will benefit the use of innovative materials in thermal power industry and in turn provide improvements to the development of better thermal energy materials.
Chapter 4

Steady-State Pool Boiling
Experiments of Various Cladding Materials

As mentioned in Chapter 3, the material-side factors including thermal-physical properties, surface hydrodynamics, cladding wall thickness, are dominant in the material-dependent boiling heat transfer in the saturated pool boiling. In this chapter, the pool boiling CHF experimental results are obtained from the horizontally-placed tubes. As implied by these pool boiling results, the thermal-physical properties, wall thickness and surface morphologies can have influential impacts on the CHF and HTCs.

4.1 Prologue

Boiling heat transfer is one of common heat removal strategies in thermal management systems such as the microelectronics coolers (Ali & El-Genk, 2012b,b), industrial boilers (Dhillon et al., 2015), and LWRs (Bandaru et al., 2020). The thermal safety assessment of boiling heat transfer systems is determined by cladding surface CHF. There are several influential factors physically contributing to the CHF occurrence including surface morphological characteristics (J.-Y. Zhang et al., 2020), heater dimension (M. He & Lee, 2019) and orientation (Gong et al., 2018), system conditions,
and thermophysical properties of liquid/solid. However, the effects of heater thermophysical properties on boiling heat transfer still remain ambiguous while are worthy of systematic investigations. The use of optimal engineering materials can help improve the thermal efficiencies of boiling heat transfer systems without the help of micro/nano surface structures (M. He & Lee, 2020; D. E. Kim et al., 2015; Dhillon et al., 2015) or nanofluids (G. M. Son et al., 2018).

Owing to the rapid advancing of high-entropy alloys (HEAs), the enlarging bank of thermal engineering materials enables versatile considerations to support the optimal design of thermal systems. For example, besides SiC/SiC, FeCrAl alloys, and Cr-coated Zircalloys, HEAs also seem promising candidates to the ATF claddings of LWRs (W. Zhang et al., 2018; Tao et al., 2021; Pickering et al., 2021). The physical understanding to the mechanistic roles of thermophysical properties in boiling heat transfer are needed to support the future development of CHF-oriented engineering materials. This becomes more and more compelling since the advent of ATF concepts and the ATF-loaded core deployments. Several pool boiling experimental investigations have been performed across a variety of materials to analyze the contributions of thermophysical properties to significant CHF variations (Godinez et al., 2021; Tachibana et al., 1967; Golobič & Bergles, 1997; Raghupathi & Kandlikar, 2017; Yeom et al., 2020). Tachibana et al. (1967) pointed that the mechanistic aspects of hydrodynamics cannot rationalize pool boiling CHF variations between various materials with different wall thicknesses alone. The thermal activity (the product of wall thickness and thermal effusivity) was proposed by Golobič and Golobič & Bergles (1997) to correlate the effects of thermophysical properties on pool boiling CHF. Raghupathi & Kandlikar (2017) thought that the square root of volumetric heat capacity was a better physical indicator than the thermal activity to predict the impacts of heater on pool boiling CHF. However, based on pool boiling CHF experiments of several ATF and traditional cladding materials, Yeom et al. (2020) concluded that the effects of surface morphologies on pool boiling CHF are more dominant than that of thermal activity. Insofar, the effects of thermophysical properties on CHF still remain ambiguous and their mechanistic mechanisms behind CHF occurrence are controversial.
Besides the thermophysical properties, the geometrical dimensions and configurations also have influential impacts on thermal performances of pool boiling. For example, the pool boiling CHF of wire heater is significantly dependent of wire diameter (Sun & Lienhard, 1970; Lienhard & Eichhorn, 1976; Rao & Andrews, 1976; Pattanayak & Kothadia, 2021), and such this dependence is attributed to the hydrodynamics instabilities. This is also observed in the pool boiling CHF experiments using square plates (M. He & Lee, 2019; Rainey & You, 2001) and circular disks (She & Dhir, 2021). Using the cylindrical vessel as the boiling chamber, the effects of confined space on pool boiling CHF are studied in the experimental investigations (She & Dhir, 2021; Ahn et al., 2022). Both experimental studies give a clear observation that increasing the vessel size can help enhance pool boiling CHF. It could be anticipated that the further increasing of vessel size beyond a certain point might not further enhance pool boiling CHF because the bubble behaviors adjacent to the heater-influenced region are not subjected to confined space any more. The effects of confined space and heater size on pool boiling CHF matter a lot in the design of mini boiler systems such as the micro-chip cooling devices (Ali & El-Genk, 2012a) and industrial small-scale boilers. It was postulated that the physical mechanisms behind the effects of heater shape, size and orientations are contributed by the interactive behaviors of bubble crowds that are usually demystified by hydrodynamics instability and nucleation site density theories (Pioro et al., 2004). However, the hydrodynamics-related theories cannot support the mechanistic rationalization to the effect of heater wall thickness on pool boiling thermal performances. For the plate-type heater, the parametric trend of pool boiling CHF shows an asymptotical increasing behavior with respect to wall thickness. However, this experimental observation could not be applied to the cylindrical-type heater such as wire and tubing heaters.

In the present engineering material bank, there are many families of alloys for various industrial applications. For instance, Zircalloys and FeCrAl alloys are claddings of nuclear fuel pellets for the present and near-future concepts to prevent the release of radioactive elements in the water coolant. Inconel alloys are favorable candidates to chemically corrosive and high-temperature applications such as jet-engine, chemical
reactors, and heat exchanger. Stainless steels are common engineering materials of
culinary appliances, aerospace, and thermal power. Up to now, there have been no
pool boiling experimental studies for those aforementioned three families of alloys.
This study provides the comparative analyses for pool boiling performances differ-
ences of "with-family" and "between-family". Besides this, the effect of tubing wall
thickness on pool boiling CHF is experimentally studied using the exact same tubing
materials. Rest of this paper is organized as follows, Section 4.2 describes the ex-
perimental methodologies, facilities and apparatus involved in the pool boiling CHF
experiments, Section 4.3 presents the differences of surface morphological character-
istics including micrographs and wettability before and after the CHF occurrences,
Section 4.4 elaborates the experimental results analyses and discusses their poten-
tial implications, and Section 4.5 lists out key findings and makes some concluding
remarks.

4.2 Experimental Apparatus and Methodologies

4.2.1 Pool Boiling Facility and Test Section

The pool boiling experimental facility is shown in Fig.4-1 and the facility is a 180 ×
150 × 230 mm³ stainless steel boiling chamber. In the pool boiling experiments of AT-
F claddings and other commercial alloys, the tested specimens of cladding materials
are placed horizontally and submerged in the deionized water for CHF experiments.
As shown in Fig.3-1, the pool boiling facility is composed of several key parts: (1)
two power feedthroughs are mounted on the chamber lid to apply direct-current pro-
grammable power to the test section, (2) a copper tube of surface condenser is fitted
upon the chamber lid to cool the vapor steam back to the liquid state of water, (3)
two sight view windows are mounted on the front and rear sides of chamber wall to
capture bubble behaviors on the cladding materials by a high-speed camera, (4) two
400-Watt cartridge heaters are inserted in the pool boiling chamber to heat up the
deionized water up to its saturation temperature, and (5) two 200-Watt wall heaters
are pasted on the left and right sides of chamber wall to balance the thermal loss to the ambient environment.

![Figure 4-1: Pool boiling experimental facility: (a) boiling chamber view and (b) test section view](image)

In this pool boiling CHF experimental study, the working fluid is the deionized water. The pool boiling facility is open to the ambient environment. In Albuquerque, the atmospheric pressure is $\sim 84$ kPa and the saturation temperature of water is $94.5^\circ C$. To minimize the disturbance of dissolved gas to the experimental results, this study adopts a degassing strategy such that the saturation temperature of temperature is kept by two wall heaters working at least until there are no bubbles generated on the test section or chamber walls. The bulk temperature of the water pool is measured by a K-type thermocouple having a 1-mm sheathed diameter inserted through the chamber lid.

The test section is horizontally tightened between the copper clamps using four 6-32 stainless steel screws. The test section is a 3-inch-long tubing with the ef-
fective heated length of 2 inch. A voltage sensor is connected to two terminals of copper clamps to measure the voltage drop across the test section. As shown in Fig.4-2, the test section is made of a metallic tubing, four K-type thermocouples, a high-temperature cement, and a high-temperature alumilite epoxy. The sample configuration of test sections is prepared as follows: (1) the inner surface of cladding tube is rinsed respectively by acetone, achhol and deionized water, (2) four K-type thermocouples are attached uniformly upon the inner surface of effective heated part based on adhesive silica pads, (4) the high-temperature cement (purchased from DAP Products Inc.) is used to fill the hollow space of tubing and both ends of tubing are open to the dry and room-temperature environment for seven days for the complete solidification of cement, (5) both ends of tubing are sealed by the water-proof high-temperature epoxy (purchased from Alumilite Corporation), and (6) the outer surface of test section is also rinsed by acetone, achhol and deionized water. Voltage, current and temperature signals are collected using a data acquisition module from National Instruments Corporation.

Figure 4-2: Test specimen configuration

The nominal uncertainty of thermocouples is ±1 °C according to the user manual of OMEGA Engineering Inc. The uncertainties of voltage and current transducers are 2.65% and 8.47% respectively at their maximum allowable scales. The data sampling frequency is 50 Hz and it means that there are 50 data points of each signal channel per second extracted from the data acquisition system.
It should be noted that the cement filler has an isothermal distribution of temperature under the steady-state condition. This speaks to that the temperature of the cement filler is theoretically same everywhere. And the corresponding analytical proof is given in Appendix A.1. according to the user manual of OMEGA Engineering Inc. The uncertainties of voltage and current transducers are 2.65% and 8.47% respectively at their maximum allowable scales. The steady-state signifier of test section is that two time-wise averaged temperature points have no difference beyond \( \pm 1 \, ^\circ \text{C} \) provided that a time-wise temperature point is averaged over 50 consecutive temperature points from the same signal channel. On the other hand, the absolute difference between two time-wise average temperature points is beyond \( 30 \, ^\circ \text{C} \) or any temperature signal channel reads the inner surface temperature beyond \( 400 \, ^\circ \text{C} \). This implies that CHF occurs on the cladding surface, or the boiling regime transits to the film boiling. Once either CHF or film boiling is detected, the power supply will be automatically shut down.

The test section releases heat flux via the direct joule heating mechanism. To protect the DC programmable power supply, the voltage-based control mode is adopted to approximate the power incremental of test section. Once the steady state of boiling system is reached, such state will be kept at least for 40 seconds and then the voltage across the test section will be increased by a step of 0.01 V. Given that the electrical resistance varies between different cladding materials, the power incremental step \( (\Delta P) \) resulting from the voltage-based control mode should be considered and calculated as follows,

\[
\Delta P \approx \frac{2V}{R_e} \Delta V
\]  

(4.1)

where \( V \) is the voltage drop across the test section, \( \Delta V \) is the voltage incremental step (in this study, it is 0.01 V) and \( R_e \) is the electrical resistance of effective heated test section. The experimental heat flux uncertainty \( \Delta q'' \) can be calculated as follows,

\[
\frac{\Delta q''}{q''} \approx \frac{\Delta q}{q} - \left( \frac{\Delta OD}{OD} + \frac{\Delta HL}{HL} \right) \approx 2 \frac{\Delta U}{U} - \left( \frac{\Delta OD}{OD} + \frac{\Delta HL}{HL} \right)
\]  

(4.2)

where OD is the outer diameter of test specimens, \( \Delta OD \) is the manufacturing toler-
The effective heated length of test specimens, $\Delta HL$ is the manufacturing tolerance of heated length, $U$ and $\Delta U$ are the voltage drop and its uncertainty for the test specimens. Besides the uncertainty contributed by the voltage-based control mode, there also exists the measurement uncertainty of voltage drop across the test specimens during sampling the data points. In light of this, the following criterion is adopted to quantify the uncertainty of test section voltage drop

$$\frac{\Delta U}{U} = \max(\frac{\Delta V}{V}, \frac{\Delta U}{U})$$

where $V$ is the test section voltage when the CHF occurs on the outer surface of test specimens, $\frac{\Delta U}{U}$ is the measurement uncertainty of voltage sensor. The relative uncertainty of heat flux is calculated as follows,

$$\frac{\Delta q''}{q''} \approx \sqrt{4\left(\frac{\Delta U}{U}\right)^2 + \left(\frac{\Delta OD}{OD}\right)^2 + \left(\frac{\Delta HL}{HL}\right)^2}$$

in this study. And also the $\Delta HL/HL$ and $\Delta U/U$ is 0.01 according to the vendors. The $\Delta OD/OD$ and $\Delta V/V$ are respectively provided in Tab. 4.1 based on various test specimens.

### 4.2.2 Tubing Materials of Nuclear Fuel Claddings

In this study, four families of cladding materials are chosen to cross-compare their experimental pool boiling heat transfer coefficients and CHFs. Their brief information is tabulated in Tab.4.1 and the experimental measurement uncertainty of heat flux described herein is also applicable to the flow boiling experiments.

**Zircalloys:** The zirconium-based amorphous alloys have favorable characteristics in neutronics advantage of low thermal neutron absorption, high hardness, ductility, and corrosion resistance including Zircaloy 2, Zircaloy 4, Zirlo and M5. These zircalloys are employed as the fuel cladding materials in the current power fleet of LWRs. However, the biggest flaw of zircalloys is that the zirconium can fiercely react with the hot water steam at the temperature beyond 1230°C. and oxidation of zirconium by
Table 4.1: Brief Information of Test Specimens

<table>
<thead>
<tr>
<th>Alloys</th>
<th>Types</th>
<th>OD</th>
<th>ΔOD</th>
<th>WT</th>
<th>Outer Surface</th>
<th>ΔV/V</th>
<th>Δq''/q''</th>
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<td>FeCrAl</td>
<td>C26M</td>
<td>0.374</td>
<td>0.001</td>
<td>0.015</td>
<td>As-Received</td>
<td>0.005</td>
<td>0.0203</td>
</tr>
<tr>
<td></td>
<td>B126Y</td>
<td>0.373</td>
<td>0.001</td>
<td>0.014</td>
<td>As-Received</td>
<td>0.005</td>
<td>0.0203</td>
</tr>
<tr>
<td></td>
<td>B136Y</td>
<td>0.374</td>
<td>0.001</td>
<td>0.015</td>
<td>As-Received</td>
<td>0.005</td>
<td>0.0203</td>
</tr>
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<td></td>
<td>Zircaloy4</td>
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<td>As-Received</td>
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<td>0.0304</td>
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<tr>
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<td>Zirlo</td>
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<td>0.025</td>
<td>As-Received</td>
<td>0.017</td>
<td>0.0342</td>
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<td>Zr705</td>
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<td>0.02</td>
<td>As-Received</td>
<td>0.02</td>
<td>0.0402</td>
</tr>
<tr>
<td>Inconel400</td>
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<td>0.0005</td>
<td>0.02</td>
<td>As-Received</td>
<td>0.009</td>
<td>0.0202</td>
<td></td>
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<tr>
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<td>0.028</td>
<td>As-Received</td>
<td>0.009</td>
<td>0.0202</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>0.375</td>
<td>0.005</td>
<td>0.035</td>
<td>As-Received</td>
<td>0.013</td>
<td>0.0262</td>
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<td>As-Received</td>
<td>0.002</td>
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<td></td>
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<td>0.015</td>
<td>As-Received</td>
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</tr>
<tr>
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<td>0.028</td>
<td>As-Received</td>
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</tr>
<tr>
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<td>0.375</td>
<td>0.0005</td>
<td>0.035</td>
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<td>0.0202</td>
</tr>
<tr>
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<td></td>
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<td>0.0005</td>
<td>0.01</td>
<td>As-Received</td>
<td>0.003</td>
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</tr>
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<td>0.006</td>
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<td>As-Received</td>
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<td>0.0202</td>
</tr>
<tr>
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<td>SS321</td>
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<td>0.02</td>
<td>As-Received</td>
<td>0.007</td>
<td>0.0202</td>
</tr>
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<td>SS347</td>
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<td>0.02</td>
<td>As-Received</td>
<td>0.005</td>
<td>0.0202</td>
</tr>
<tr>
<td>Stainless Steel</td>
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<td>0.0005</td>
<td>0.01</td>
<td>As-Received</td>
<td>0.005</td>
<td>0.0202</td>
</tr>
<tr>
<td>Titanium</td>
<td>Grade-2</td>
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<td>0.0015</td>
<td>0.035</td>
<td>As-Received</td>
<td>0.025</td>
<td>0.0502</td>
</tr>
<tr>
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<td>Grade-9</td>
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<td>0.0015</td>
<td>0.035</td>
<td>As-Received</td>
<td>0.025</td>
<td>0.0502</td>
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</table>

water is accompanied by release of hydrogen gas and of exothermic heat. Up to now, most of commercial LWRs are still on the use of UO$_2$-Zircaloy fuel-clad systems. The available experimental data of T-H Zircaloy characteristics are still not sufficient to support the comprehensive thermal safety assessment of Zircaloy claddings. FeCrAl Alloys: As alternative candidates to zirconium-based alloys, these FeCrAl alloys have better potentials in resistance to high-temperature steam oxidation, material structural strengths, and the resistances to irradiation and chemical corrosion. However, the FeCrAl alloys have higher absorption cross section of thermal neutrons. This makes the energy spectrum of neutrons hardening, resulting in unwanted outcomes including the reactivity suppression and the higher burnup. The T-H characteristics of FeCrAl alloys are of importance to license the ATF-loaded LWRs in the near-term implementation of ATF. Stainless Steels: Austenitic stainless steels are widely used as in-core and surrounding structural materials in current commercial LWR systems. Moreover, the stainless steels are often used as the reference materials in most of T-H experiments. In the early stage of nuclear energy, the stainless steels were used as claddings to encase the UO$_2$ fuel pellets. This study adopts the stainless steels as
the references basis to compare with T-H characteristics of other cladding materials.

Nickel-Based Alloys: Nickel alloys are used extensively in nuclear reactor applications because they have high strength and good corrosion properties. However, nickel has some unique nuclear properties (including relatively high thermal neutron absorption cross-sections) that limit its use in power reactor cores. The primary application of nickel alloys is to manufacture the steam generator of LWRs. Besides the nuclear energy applications, nickel alloys are also used as the heating elements in the chemical and petrochemical industries. The T-H characteristics of nickel alloys can be transferred from the nuclear power to the chemical-related industries.

4.2.3 Contact Angle Goniometer, High-Speed Camera, Surface Characterizations

The surface roughness and contact angles are measured with a surface profilometer and contact angle Goniometer, respectively. The surface profilometer (Dektak 150, Fig. 4-3a) is a stylus type with 1 mg force to measure surface peaks and valleys. The average roughness, Ra, of each scan is calculated from the surface topology measurements using the Dektak software. The machine can measure samples up to 5 inch in diameter. The system has a 1-millimeter standard vertical range with up to 120,000 data points per scan. The machine is capable of measuring surface flatness, waviness, and radius of curvature as well as characterizing thin-film stress on wafers.

The contact angle goniometer is a device that can measure contact angle, surface tension, and surface energy. The facility is fitted with an automated dispensing system to control the droplet volume and ensure measurement repeatability. The automated tilting base is a fully controlled base to change the specimen tilting angle and measure both advance and receding contact angles, $\theta_a$ and $\theta_r$, respectively. The machine is also equipped with a temperature-controlled hot chamber for conducting measurements at an elevated temperature up to 300°C (Fig. 4-3b).

A gray-scale high-speed optical camera is employed to record the live video of boiling dynamics behaviors on the cladding tube surface (the camera is purchased
Figure 4-3: Dektak 150 surface profilometer (left) & contact angle goniometer (right)

Figure 4-4: High-speed camera for boiling heat transfer behavior analyses from the Fastec Motion Inc., Ts model, See Fig. 4-4). This helps demystify the CHF triggering mechanisms on the horizontally placed tube surface. The micro-scope analysis machines including the optical scan, SEM scan and AFM scan are utilized to characterize the surface morphological features before and after CHF occurrence. This benefits to the understanding of how boiling heat transfer contributes to the surface oxidization/corrosion.
4.3 Cladding Surface Characterization and Bubble Behaviors

4.3.1 Micrographs of SEM and Optical Microscope

As shown in Fig. 4-5, FeCrAl-C36M has a better corrosion resistance than FeCrAl-B126Y and FeCrAl-B136Y to the pool boiling CHF occurrence from the visual appearance of post-CHF surfaces. The evident features on the cladding surfaces of B126Y and B136Y are a slim crack and a cluster of oxidized spots respectively. In comparison with the post-CHF surfaces of three Zircalloys, FeCrAl alloys have better corrosion resistance to the CHF-occurrence. Because the cladding surfaces of three Zircalloys are totally covered by a layer of oxide layer. In comparison with the post-CHF surfaces of three Zircaloy variants (Fig. 4-6), three FeCrAl variants have better corrosion resistance to the surface temperature overshooting of CHF occurrence than three Zircaloy variants. Figs. 4-7, 4-8 and 4-9 respectively show the micro-scale surface appearances before and after the pool boiling CHF experiments for three FeCrAl alloys, C26M, B126Y and B136Y. It is obvious that in comparison with B126Y and B136Y, there are less clusters of oxide deposits on the post-CHF surface of C26M. Besides, the average size of oxide deposits on the FeCrAl-C26M post-CHF surface is smaller than that of B126Y and B136Y. The slim crack on the post-CHF surfaces is located upon the downside of the horizontally placed FeCrAl-B126Y tube. However, the cluster of oxidized spots is observed in the downside of the horizontally placed FeCrAl-B136Y tube. This physically means that the surface oxidization corrosion on the downside is worse than on the upside. The post-CHF surface of FeCrAl-C26M shows the similar observations of oxidization corrosion. These optical micrographs of post-CHF surfaces imply that CHF firstly occurs on the downside of horizontally placed tube and then extends to the upside. Figs. 4-10, 4-11, and 4-12, present the microscale optical scans of Zircaloy-4, Zirlo and Zr705 respectively before and

\[^1\text{Herein, the author connotes the corrosion resistance as the ability of cladding material to withstand the surface temperature overshooting and generate thinner oxide layer.}\]
Figure 4-5: Post-CHF samples of three FeCrAl alloys (C26M, B126Y and B136Y), noting that the some oxidization features are presented in the regions that are marked by a yellow box.

Figure 4-6: Post-CHF samples of three zircalloys (Zircaloy-4, Zirlo, and Zr705)
after pool boiling CHF experiments. From the comparisons between the post-CHF surfaces of Zircaloy-4 and Zirlo, there is no appreciable difference. However, it is observed that there are some unknown crystal features on the post-CHF surface of
Zr705.

(a) As-Received  
(b) Post-CHF  
Figure 4-10: Optical scan of Zircaloy4 before and after boiling at the magnification of 20

(a) As-Received  
(b) Post-CHF  
Figure 4-11: Optical scan of Zirlo before and after boiling at the magnification of 20

(a) As-Received  
(b) Post-CHF  
Figure 4-12: Optical scan of Zr705 before and after boiling at the magnification of 20

Fig.4-13 compares the post-CHF surfaces between Zircaloys and FeCrAl's and presents some typical surface morphological features resulting from the pool boiling
CHF occurrence. From the oxide distributions on the post-CHF surfaces, FeCrAl-C26M has the best resistance ability to the oxidization corrosion at the surface temperature overshooting of CHF occurrence. Among three Zircaloys, the Optimized Zirlo has a better corrosion resistance to the surface oxidization.

Figure 4-13: Optical scan of FeCrAl alloys and Zircaloys for typical micro-scale surface features at the magnification of 50
Figs. 4-14, 4-15 and 4-16 present SEM scan micro-graphs of as-received and post-CHF surfaces respectively for C26M, B126Y and B136Y before and after the pool boiling CHF experiments. As shown in three figures, the oxide layers of both B126Y and B136Y are stripped off from the post-CHF surfaces, which results in the further surface oxidation while the oxide layer of C26M still remains intact to protect cladding surface from the high-temperature steam corrosion. On the other hand, the average size of oxidized features on the post-CHF surface of FeCrAl-C26M is smaller than that of FeCrAl-B126Y and of FeCrAl-B136Y. These micro-scale observations made to post-CHF surfaces of three FeCrAl alloys speak to that FeCrAl-C26M has the best corrosion resistance to the high-temperature water/steam mixture. Besides the comparisons between as-received and post-CHF surfaces, some unique features resulting from pool boiling CHF occurrence are also presented respectively for FeCrAl-C26M (See Fig.4-17), FeCrAl-B126Y (See Fig.4-18), and FeCrAl-B136Y (See Fig.4-19). By cross-comparing the post-CHF surfaces of three FeCrAl variants, it is convinced that FeCrAl-C26M is less susceptible to the corrosion impacts of CHF occurrence than FeCrAl-B126Y and FeCrAl-B136Y. Figs.4-20, 4-21 and 4-22 show SEM scanning micrographs of as-received and post-CHF surfaces respectively for Zircaloy-4, Zirlo and Zr705. Besides the oxide layer covered on the post-CHF surfaces, there are some unique crystal deposits presented on surfaces. The XPS analyses imply that the crystal deposits are zirconium hydrides (See Fig.4-21(b) and Fig.4-22(b)). It was widely accepted that FeCrAl alloys have better corrosion resistance ability to the high temperature steam/water mixture than Zircaloys (Zinkle et al., 2014). The qualitative
analyses by the micro-graphs of post-CHF surface SEM scanning can not provide the overall comparisons of corrosion resistance ability between 6 different nuclear cladding materials. In this study, the post-CHF cladding samples are machined to produce the cross-section view for the SEM scanning measurement of oxide layer thickness. As shown in Fig.4-23, there is a clear boundary between the oxide layer and base ma-
Figure 4-18: SEM scan for a typical feature of FeCrAl-B126Y post-CHF surface

(a) at the magnification of 250
(b) at the magnification of 2000

Figure 4-19: SEM scan for a typical feature of FeCrAl-B136Y post-CHF surface

(a) at the magnification of 250
(b) at the magnification of 2000

Figure 4-20: SEM scan of Zircaloy-4 before and after pool boiling CHF experiment

(a) As-Received
(b) Post-CHF

Material. Given that the boiling behavior difference between the upside and downside of tested samples, the oxide layer thickness of each side is averaged by 18 measurement points respectively. For example, 6 SEM scan locations are picked up on the upside of post-CHF surface and measuring the oxide layer thickness is performed at 3 different points. Then the oxide layer thickness of the tested sample upside is averaged by 18 different measurement points. Provided in Tab.4.2, the oxide layer thickness of
upside is less than that of downside. This confirms that the oxidization corrosion of downside is worse than that of upside, possibly implying CHF may firstly occur on the downside of horizontally placed tube. Also, FeCrAl-C26M has the least oxide layer thickness among six nuclear fuel cladding materials, which gives a direct proof to that FeCrAl-C26M has the best corrosion resistance to the high-temperature surface oxidization. Among three Zircaloys, Zirlo has the thinnest oxide layer, which is in agreement with the results analyses of Fig.4-13.

Table 4.2: Oxide Layer Thicknesses of Zircaloys and FeCrAls Obtained by SEM

<table>
<thead>
<tr>
<th>Claddings</th>
<th>Upside: $\phi = 0^\circ$ (μm)</th>
<th>Downside: $\phi = 180^\circ$ (μm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>FeCrAl-C26M</td>
<td>0.09±0.03</td>
<td>0.27±0.02</td>
</tr>
<tr>
<td>FeCrAl-B126Y</td>
<td>0.29±0.04</td>
<td>0.94±0.05</td>
</tr>
<tr>
<td>FeCrAl-B136Y</td>
<td>0.18±0.06</td>
<td>0.81±0.09</td>
</tr>
<tr>
<td>Zircaloy-4</td>
<td>6.13±0.23</td>
<td>11.22±0.37</td>
</tr>
<tr>
<td>Zirlo</td>
<td>4.18±0.36</td>
<td>9.53±0.62</td>
</tr>
<tr>
<td>Zr705</td>
<td>6.85±0.28</td>
<td>12.64±0.52</td>
</tr>
</tbody>
</table>
4.3.2 Contact Angles on Cylindrical Surfaces

The post-CHF surfaces of FeCrAl alloys become more hydrophilic than their as-received surfaces (See Figs.4-24, 4-25 and 4-26). It is noteworthy that the pool boiling CHF occurrence on the cladding surface can help reduce the surface wettability difference between as-received samples after the boiling experiments. This surface wettability change is also observed in the post-CHF surfaces of Zircaloys (See Figs.4-27,4-28 and 4-29). However, the post-surfaces of six nuclear claddings are more hydrophilic than their counterparts of as-received samples. This results in that the CHF re-occurrence on the post-CHF samples needs more thermal energy to take place, further potentially increasing the safety margins of thermal management system. However, this superiority of pool boiling CHF enhancement could be gradually weaken by the increasing of liquid subcooling. Although the as-received FeCrAl samples are less hydrophilic than the as-received Zircaloy samples, the post-
Figure 4-24: Surface contact angles of FeCrAl-C26M before and after pool boiling CHF experiment

(a) As-Received $\theta_s = 66.53^\circ$  
(b) Post-CHF $\theta_s = 38.53^\circ$

Figure 4-25: Surface contact angles of FeCrAl-B126Y before and after pool boiling CHF experiment

(a) As-Received $\theta_s = 74.86^\circ$  
(b) Post-CHF $\theta_s = 36.16^\circ$

Figure 4-26: Surface contact angles of FeCrAl-B136Y before and after pool boiling CHF experiment

(a) As-Received $\theta_s = 81.84^\circ$  
(b) Post-CHF $\theta_s = 35.31^\circ$

Figure 4-27: Surface contact angles of Zircaloy4 before and after pool boiling CHF experiment

(a) As-Received $\theta_s = 65.71^\circ$  
(b) Post-CHF $\theta_s = 52.17^\circ$
CHF surfaces of FeCrAl samples are more hydrophillic than the post-CHF surfaces of Zircalloys. The surface wettability enhancement by oxide layers in FeCrAl alloys is greater than that of Zircalloys. This further confirms that besides surface morphological features, the chemical constitutions of cladding materials can affect the surface wettability significantly.

Based on Kandlikar’s CHF model (Kandlikar, 2001), the receding contact angle is the study of interest that explains the effect of surface wettability on CHF. The contact angle goniometer (Dektak 150) can measure both advance and receding contact angles by changing the specimen tilting angles and using the automated tilting base. The static contact angle can be used as an indicator of surface wettability of oxidized samples in this study, and it is measured on the as received and post-CHF surfaces. The corresponding measurement procedures are in accordance with the standard of American Society of Testing Materials (ASTM D7490-13, 2013; ASTM D7334-08, 2013). The ambient temperature of contact angle measurement is approximately close to the inlet temperature of water coolant, and the tested specimens are put in
a transparent chamber where the ambient temperature is controlled by the contact angle goniometer. The surrounding gas medium of the transparent chamber is the dry air. To minimize the volume shrinkage of water droplet, five measurements are performed on each sample within 5 secs respectively. Tab.4.3 provides the measured contact angles of tested cladding materials. As shown in Tab.4.3, the receding angle is less than the static contact angle. The results of contact angle can be validated by the Tadmor model of surface wettability (Tadmor, 2004). This model relates the static contact angle with the advancing and receding contact angles based on the equilibrium line energy as the follow three sets of equations,

\[
\theta_s = \arccos\left(\frac{\Gamma_a \theta_a + \Gamma_r \theta_r}{\Gamma_a + \Gamma_r}\right) \quad (4.5a)
\]

\[
\Gamma_a = \left(\frac{\sin^3(\theta_a)}{2 - 3 \cos(\theta_a) + \cos^3(\theta_a)}\right)^{1/3} \quad (4.5b)
\]

\[
\Gamma_r = \left(\frac{\sin^3(\theta_r)}{2 - 3 \cos(\theta_r) + \cos^3(\theta_r)}\right)^{1/3} \quad (4.5c)
\]

The predicted static contact angle based on the Tadmor model is compared with the measured static contact angle for all the tested materials as shown in Fig. 4-30. The 10.6 % model discrepancy possibly results from the measurement uncertainty and the curvature effect that is not incorporated in the Tadmor model. Because the Tadmor model is derived for contact angle relations on the plates instead of tubing.

### 4.3.3 Surface Orientation Angle and Bubble Behaviors

In the pool boiling experiments of horizontally placed tubes, the surface orientation angle with respect to the motion direction of bubble varies from 0° (up-side) to 90° (front/rear-side) to 180° (down-side). The bubble behaviors difference on three sides may result from the surface orientation angle (See Fig.4-31).

Figs.4-32, 4-33 and 4-34 show the bubble behaviors on the horizontally-placed claddings of three FeCrAl and three Zircaloys respectively at the heat flux of 51, 139 and 206 kW/m². As observed in Fig.4-32, the bubble nucleation site density of three FeCrAl alloys is greater than that of three Zircaloys. It is noteworthy that the
Table 4.3: Contact Angles of Tested Samples

<table>
<thead>
<tr>
<th>Alloy Families</th>
<th>Types</th>
<th>Contact Angles (°)</th>
<th>Contact Angles (°)</th>
<th>Contact Angles (°)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>$\theta_s$</td>
<td>$\theta_r$</td>
<td>$\theta_a$</td>
</tr>
<tr>
<td>FeCrAl</td>
<td>C26M</td>
<td>66.53</td>
<td>54.26</td>
<td>94.88</td>
</tr>
<tr>
<td></td>
<td>B126Y</td>
<td>74.86</td>
<td>63.45</td>
<td>97.78</td>
</tr>
<tr>
<td></td>
<td>B136Y</td>
<td>81.84</td>
<td>67.28</td>
<td>104.79</td>
</tr>
<tr>
<td>Zircalloys</td>
<td>Zircaloy4</td>
<td>65.71</td>
<td>49.3</td>
<td>83.4</td>
</tr>
<tr>
<td></td>
<td>Zirlo</td>
<td>63.87</td>
<td>48.34</td>
<td>84.83</td>
</tr>
<tr>
<td></td>
<td>Zr705</td>
<td>57.11</td>
<td>40.62</td>
<td>85.73</td>
</tr>
<tr>
<td></td>
<td>400</td>
<td>67.92</td>
<td>55.7</td>
<td>98.49</td>
</tr>
<tr>
<td>Inconel</td>
<td>600</td>
<td>65.54</td>
<td>55.69</td>
<td>93.23</td>
</tr>
<tr>
<td></td>
<td>625</td>
<td>62.14</td>
<td>50.16</td>
<td>88.23</td>
</tr>
<tr>
<td></td>
<td>304</td>
<td>70.77</td>
<td>63.36</td>
<td>87.16</td>
</tr>
<tr>
<td>Stainless Steel</td>
<td>316</td>
<td>73.7</td>
<td>65.34</td>
<td>91.35</td>
</tr>
<tr>
<td></td>
<td>321</td>
<td>77.62</td>
<td>68.56</td>
<td>92.09</td>
</tr>
<tr>
<td></td>
<td>347</td>
<td>72.14</td>
<td>64.21</td>
<td>89.75</td>
</tr>
<tr>
<td>Titanium</td>
<td>Grade-2</td>
<td>54.12</td>
<td>40.36</td>
<td>72.11</td>
</tr>
<tr>
<td></td>
<td>Grade-9</td>
<td>59.56</td>
<td>47.41</td>
<td>73.59</td>
</tr>
</tbody>
</table>

noting that static angle $\theta_s$, advancing angle $\theta_a$, and receding angle $\theta_r$, all the angles are measured upon the horizontally placed tubings.

Figure 4-30: Measured static contact angle against the predicted static contact angle using the Tadmor model

bubble diameter on the down-side is larger than those on the up-side. One possible phenomenological explanation behind this is that the cladding tube can impede the
bubble departure from the down-side surface. In light of this bubble impedance by the down-side surface, the CHF may more likely occur on the down-side surface of cladding tube. As further increasing heat flux, this bubble impedance by the down-side surface becomes more and more evident. In comparison with the nucleate site density of FeCrAl-B126Y and FeCrAl-B136Y, the FeCrAl-C26M cladding surface has a less nucleate site density and generates larger bubbles. This can help delay the occurrence of pool boiling on the FeCrAl-C26M cladding surface and thus result in the higher pool boiling CHF than FeCrAl-B126Y and FeCrAl-B136Y.

Besides the cladding materials and surface wettability, the surface roughness can have influential impacts on the bubble dynamics behaviors. For the horizontally-placed tubes of Inconel alloys and stainless steels with exactly same outer diameter and wall thickness, the cladding surfaces of tested samples are blasted by the 800-grit sandpapers on the automatic machining machine to generate approximately same surface-roughness scales. As shown in Figs.4-35, 4-36 and 4-37, the Inconel 600 & 625 generate less crowds of larger bubbles than SS 304, 316, 321 & 347 under three different surface heat flux. Given that all cladding surfaces are blasted by the 800-grit sandpapers, the one possible plausible rationale behind this difference of bubble behaviors could result from the surface activation energy that needs to activate nucleate

Figure 4-31: Schematic view of boiling behaviors on a horizontally placed tube
(a) FeCrAl-B126Y  (b) FeCrAl-B136Y  (c) FeCrAl-C26M  (d) Zircaloy4  
(e) Zirlo  (f) Zr705

Figure 4-32: Bubble behaviors of three Zircalloys and three FeCrAl alloys at the heat flux of 51 kW/m$^2$

sites in relation with the thermal-physical properties.

It is observed in Figs.4-32~4-37 that the bubble behaviors are roughly similar on the cladding surfaces of "within families" materials while show significantly different
patterns on the "between families" materials. For example, the bubble dynamics behaviors show no appreciable difference between Inconel 600 and Inconel 625, between FeCrAl alloys, or between Zircaloy from FeCrAl-C26M to FeCrAl-B126Y and FeCrAl-B136Y. These materials exhibit similar bubble behaviors when subjected to the same heat flux of 139 kW/m². The figures illustrate the bubble patterns for each material, demonstrating their unique characteristics. For instance, Zirlo and Zr705 show distinct bubble dynamics compared to the FeCrAl alloys and Zircaloy-4. This detailed observation is crucial for understanding the thermal and mechanical properties of these materials in high-temperature environments.
Figure 4-34: Bubble behaviors of three Zircaloys and three FeCrAl alloys at the heat flux of 206 kW/m²

The bubble behaviors of stainless steels are significantly distinct from those of Inconel alloys, and of FeCrAl alloys. This could be attributed to the thermal-physical properties variations of the "between families" materials. On the other hand, the difference
of surface morphological features between various families of alloys could rationalize the bubble dynamics behaviors from the perspective of nucleate site density theories. This assumption that thermal-physical properties may play a decisive role in the bubble dynamics behaviors will be left for the future experimental validations using the square plate heaters made of various cladding materials. In this study, the horizontally-placed tubing can not support the quantitative and parametric analyses
Figure 4-36: Bubble behaviors of four stainless steels and two Inconels at the heat flux of 139 kW/m\(^2\) (OD=3/8",WT=0.02")

of bubble dynamics behaviors including the bubble departure diameter and frequency because of the curvature effect of cylindrical tubing.

Considering that FeCrAl alloys have a higher likelihood of post-CHF survival and a better resistance to the structural failure resulting from CHF occurrence, the bubble behaviors on the post-CHF surfaces of FeCrAl alloys are also of importance
Figure 4-37: Bubble behaviors of four stainless steels and two Inconels at the heat flux of 206 kW/m² (OD=3/8", WT=0.02")

to the physical understanding to boiling heat transfer mechanisms that are subjected to the impacts of oxide layer. Figs.4-38 and 4-39 demonstrate that the oxidized region of post-CHF samples generates larger bubbles but sparser bubble crowds. The introduction of oxide layer to the boiling heat transfer interface brings variations in two factors affecting CHF triggering mechanisms, surface morphological features
Figure 4-38: Bubble behaviors of as-received and post-CHF FeCrAl-C26M including oxide clusters and micro-scale cracks (See Fig.4-19), and thermal-physical properties alteration cross the triple line of boiling as a result of oxide layer presence.
Figure 4-39: Bubble behaviors of as-received and post-CHF FeCrAl-B126Y

4.4 Results and Discussions

4.4.1 Pool Boiling Experimental Results

The pool boiling curves of three FeCrAl alloys are shown in Fig. 4-40(a) and their heat transfer coefficients are presented in Fig.4-41. As observed, the single-phase
heat transfer coefficient (SPHTC) is approximately same for three different FeCrAl alloys before the onset of nucleate boiling (ONB). However, beyond ONB, the nucleate boiling heat transfer coefficient of (NB-HTC) FeCrAl-B136Y is less than that of FeCrAl-C26M and FeCrAl-B126Y. But there is no appreciable difference of NB-HTC between FeCrAl-C26M and FeCrAl-B126Y. For pool boiling CHF of saturated water, FeCrAl-C26M shows much greater priority over FeCrAl-B126Y, and FeCrAl B136Y. The pool boiling curves and heat transfer coefficients of three zircaloys are respectively presented in Fig. 4-40(b) and Fig. 4-42. Similar to the pool boiling

Figure 4-40: Pool boiling curves of three FeCrAl alloys and three zircaloys in the saturated water

Figure 4-41: Pool boiling heat transfer coefficients of three FeCrAl alloys, C26M, B126Y and B136Y
Figure 4-42: Pool boiling heat transfer coefficients of three zircaloys, Zircaloy-4, Zirlo and Zr705

cases of FeCrAl alloys, there is no appreciable difference of SP-HTC between three zircaloys. However, beyond ONB, NB-HTC of Zirlo is greater than that of Zircaloy-4 and Zr705. It is noteworthy that for Zirlo, the fully-developed nucleate boiling occurs because as shown in Fig. 4-42, the further increasing of heat flux can not enhance the pool boiling NB-HTC of Zirlo. Fig. 4-43 demonstrates that Zirlo has a comparable SP-HTC to that of FeCrAl-C26M while beyond ONB point, FeCrAl-C26M has a higher NB-HTC than Zirlo. It is noteworthy that there is no characteristics of fully-developed nucleate boiling reflected in the HTCs of FeCrAl alloys. One possible explanation behind this is that the fully-developed nucleate boiling heat flux of FeCrAl alloys is suppressed by the hydrodynamics instability occurrence on the horizontally-placed tubing. Tabs. 4.4, 4.5, and 4.6 tabulate the CHF experimental results of horizontally-placed tubes respectively for nuclear claddings, nickel-based alloys and stainless steels under the saturated pool boiling of water. As shown in Tab. 4.4, the pool boiling CHF values of as-received/post-CHF oxidized FeCrAl-C26M are greater than other 5 nuclear cladding materials. However, FeCrAl-B126Y and FeCrAl-B136Y have the pool boiling CHF values lower than that of Zircaloy-4. It should be noted that the as-received samples are less resistant to the boiling
crisis than the post-CHF surface of tested samples that are oxidized by the CHF occurrence. This agrees with the pool boiling CHF experimental results of oxidized sample mentioned in Chapter 3. The mechanistic rationales behind the pool boiling CHF enhancement by oxide layer are attributed to several dominant factors. For example, the metallic oxides give the surface wettability promotion, and the denser nucleate site density results from surface morphological features of oxide layers, and the oxide layer at the boiling heat transfer interface can induce the thermal-physical properties variations, as reported in the literatures. As for which mechanism takes a dominant role, insofar, there is no consensus view, because three aforementioned mechanisms act together up on the CHF re-occurrence on the post-CHF surfaces. Tab. 4.5 lists out the pool boiling experimental CHF values of three Inconel and two titanium alloys. Inconel 600 has a comparable pool boiling CHF with FeCrAl-C26M despite of their material differences. However, Inconel 400 and two titanium alloys have higher pool boiling CHF values than Inconel 600 and 625 because of their rough surfaces sandblasted by the sandpaper (grit 180)\textsuperscript{2}. In Tab. 4.5, it is shown that the increasing of Inconel 600 wall thickness can result in the decreasing of pool boiling

\textsuperscript{2}Titanium alloys and Inconel 400, their outside surfaces have a thin layer of oxide. To improve the heating efficiency of electrical power, this oxide layer is removed by using sandpapers.
CHF. This is also observed in the pool boiling CHF experimental results of SS316 with 5 different wall thickness (See Tab.4.6). However, for experimental results of SS304, the parametric trend of pool boiling CHF with respect to increasing of the wall thickness firstly decreases, then increases and finally decreases. The local maximum of SS 304 pool boiling CHF achieves at the wall thickness of 0.02 inch. The pool boiling CHF results of horizontally placed tubes contradict with that the pool boiling CHF of horizontally-place plates increases over the increasing of plate wall thickness.

Table 4.4: Experimental Pool Boiling CHF of Three Zircaloy and Three FeCrAl Alloys

<table>
<thead>
<tr>
<th>Material</th>
<th>OD/WT(mm)</th>
<th>Surface Morphology</th>
<th>CHF (kW/m²)</th>
<th>ΔCHF (kW/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Zircaloy-4</td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>567.3</td>
<td>±17.25</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Sandblasted by Grit 180</td>
<td>607.89</td>
<td>±18.48</td>
</tr>
<tr>
<td>Zirlo</td>
<td>9.475/0.486</td>
<td>As-Received</td>
<td>419.32</td>
<td>±14.34</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>549.48</td>
<td>±18.79</td>
</tr>
<tr>
<td>Zr705</td>
<td>9.500/0.650</td>
<td>As-Received</td>
<td>417.41</td>
<td>±16.78</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>521.41</td>
<td>±20.96</td>
</tr>
<tr>
<td>FeCrAl-C26M</td>
<td>9.474/0.353</td>
<td>As-Received</td>
<td>648.25</td>
<td>±13.16</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>858.2</td>
<td>±17.42</td>
</tr>
<tr>
<td>FeCrAl-B126Y</td>
<td>9.487/0.386</td>
<td>As-Received</td>
<td>420.55</td>
<td>±8.54</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>499.21</td>
<td>±10.13</td>
</tr>
<tr>
<td>FeCrAl-B136Y</td>
<td>9.499/0.374</td>
<td>As-Received</td>
<td>380.96</td>
<td>±7.73</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>549.5</td>
<td>±11.15</td>
</tr>
</tbody>
</table>

Table 4.5: Experimental Pool Boiling CHF of Inconels and Titaniums

<table>
<thead>
<tr>
<th>Material</th>
<th>OD/WT (mm)</th>
<th>Surface Morphology</th>
<th>CHF (kW/m²)</th>
<th>ΔCHF (kW/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Titanium-2</td>
<td>9.525/0.889</td>
<td>Sandblasted by Grit 180</td>
<td>748.52</td>
<td>±37.58</td>
</tr>
<tr>
<td>Titanium-9</td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>398.18</td>
<td>±19.99</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>611.71</td>
<td>±30.71</td>
</tr>
<tr>
<td>Inconel400</td>
<td>9.525/0.508</td>
<td>Sandblasted by Grit 180</td>
<td>763.86</td>
<td>±15.43</td>
</tr>
<tr>
<td></td>
<td>9.525/0.889</td>
<td>Sandblasted by Grit 180</td>
<td>645.24</td>
<td>±16.91</td>
</tr>
<tr>
<td>Inconel625</td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>588.62</td>
<td>±11.89</td>
</tr>
<tr>
<td>Inconel600</td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>646.04</td>
<td>±13.05</td>
</tr>
<tr>
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<td>9.525/0.7112</td>
<td>As-Received</td>
<td>452.23</td>
<td>±9.14</td>
</tr>
<tr>
<td></td>
<td>9.525/0.889</td>
<td>As-Received</td>
<td>419.32</td>
<td>±10.99</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>571.49</td>
<td>±14.97</td>
</tr>
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</table>
### Table 4.6: Experimental Pool Boiling CHF of Stainless Steels with Various Wall Thicknesses

<table>
<thead>
<tr>
<th>Material</th>
<th>OD/WT (mm)</th>
<th>Surface Morphology</th>
<th>CHF (kW/m²)</th>
<th>ΔCHF (kW/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>SS304</td>
<td>9.525/0.254</td>
<td>As-Received</td>
<td>564.96</td>
<td>±11.41</td>
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<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>723.66</td>
<td>±14.62</td>
</tr>
<tr>
<td></td>
<td>9.525/0.381</td>
<td>As-Received</td>
<td>441.38</td>
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</tr>
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<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>922.25</td>
<td>±18.63</td>
</tr>
<tr>
<td></td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>559.27</td>
<td>±11.30</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
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<td>±12.53</td>
</tr>
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<td>As-Received</td>
<td>525.82</td>
<td>±10.62</td>
</tr>
<tr>
<td></td>
<td>9.525/0.889</td>
<td>As-Received</td>
<td>519.5</td>
<td>±10.50</td>
</tr>
<tr>
<td>SS316</td>
<td>9.525/0.254</td>
<td>As-Received</td>
<td>551.94</td>
<td>±11.50</td>
</tr>
<tr>
<td></td>
<td>9.525/0.381</td>
<td>As-Received</td>
<td>518.93</td>
<td>±10.48</td>
</tr>
<tr>
<td></td>
<td>9.525/0.508</td>
<td>As-Received</td>
<td>485.51</td>
<td>±9.82</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>673.27</td>
<td>±13.60</td>
</tr>
<tr>
<td></td>
<td>9.525/0.7112</td>
<td>As-Received</td>
<td>460.54</td>
<td>±9.30</td>
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<td>9.525/0.889</td>
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<td>427.9</td>
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<tr>
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<td>As-Received</td>
<td>556.65</td>
<td>±11.24</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>751.47</td>
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<tr>
<td>SS347</td>
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<td>434.63</td>
<td>±8.78</td>
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<tr>
<td></td>
<td></td>
<td>Post-CHF Oxidized</td>
<td>564.03</td>
<td>±11.39</td>
</tr>
</tbody>
</table>

#### 4.4.2 Effects of Wall Thickness

The heat transfer scheme difference between the cladding tube and the square plate is that the cladding tube can transfer the heat to the ambient water radially at the full view of 360° while for the plate heater, the heat is transferred only from the one side of wall to the surrounding water. This heat flow direction difference may account for why the effect of tubing wall thickness on pool boiling CHF is different from the effect of plate wall thickness.

In this study, this experimental observation behind the effects of tubing wall thickness on pool boiling CHF is rationalized by two heat transfer mechanisms, the thermal capacity related heat conduction against the thermal hydrodynamic instability based heat convection. Thermal hydrodynamic instability is generated in a density stratified medium where the denser regions cool faster than less dense regions. thermal hydrodynamics instability can take place when the equilibrium state of fluid is broken and cannot be recovered due to the instable mechanisms such as the buoyancy effect. Since the tested samples are horizontally placed with the outer diameter fixing at 0.375” and submerged in the saturated water, there is a certain physical contribution of thermal hydrodynamics instability to the effect of tubing wall thickness while
such this hydrodynamics contribution is identical to the cladding materials with same outer diameters. More related details can be found in Refs. (Rao & Andrews, 1976; Lienhard & Eichhorn, 1976; Sun & Lienhard, 1970). Within a bubble ebullition cycle ($T_b \sim 0.01s \sim 0.1s$), there are three important phenomenological mechanisms constituting pool boiling heat convection, i.e., natural convection $q''_c$, quenching $q''_q$, and evaporation $q''_e$ according to the heat flux partitioning model (M. Kim & Kim, 2020), as follows

\[ q'' = q''_e + q''_q + q''_c \]  \hspace{1cm} (4.6a)

\[ q''_e = \frac{5k_l}{2\sqrt{\nu_l}t_d} \Delta T_{\text{wall}} \]  \hspace{1cm} (4.6b)

\[ q''_q = \frac{2k_l}{\sqrt{\pi\alpha_l}t_w} \Delta T_{\text{wall}} \]  \hspace{1cm} (4.6c)

\[ q''_c = \frac{0.14k_l}{L_c} \text{Ra}^{1/3} \Delta T_{\text{wall}} \]  \hspace{1cm} (4.6d)

where $L_c$ is the characteristic length of the geometry, $\text{Ra}$ is the Rayleigh number, $k_l$, $\alpha_l$, and $\nu_l$ are thermal conductivity, thermal diffusivity and kinematic viscosity of working liquid respectively, $t_w$ is the bubble waiting time, and $t_d$ is the bubble departure time.

Speaking from the heat conduction perspective, the effect of tubing wall thickness on pool boiling CHF might be explained from the role of thermal capacity in heat conduction. To further support the analyses of heat conduction, the thermally equivalent diameter ($D_t$) is proposed based on the isothermal distribution of high-temperature tolerant cement filler

\[ D_t = 2\sqrt{\text{ODWT} - \text{WT}^2} \]  \hspace{1cm} (4.7)

where $D_t$ is interpreted as the diameter of a fictitious wire heater releasing the same amount of heat as the tested tubing samples.

**Thermal Penetration Depth:** In analogy to the transient heat conduction between semi-infinite bodies, this study adopts the concept of thermal penetration (diffusion) depth to characterize the effects of wall thickness on the pool boiling CHF, defined
as follows,

\[ \delta(t_w) = 3.64 \sqrt{\alpha_s t_w} \]  

(4.8)

where \( t_w \) is the bubble waiting time, and \( \alpha_s \) is the thermal diffusivity of cladding material. This new physic indicator quantifies the depth to which bubble effects propagate within a solid medium. \( t_w \) can be calculated by the empirical correlations. Using the thermal diffusivity at the cladding surface temperature before CHF occurrence, the thermal diffusion length can be calculated, for SS 304, \( \delta(t_w)_{SS304} = 0.059 \) inch and for SS 316, \( \delta(t_w)_{SS316} = 0.064 \) inch. According to the mechanistic definition of thermal diffusion length Eq.4.8, it is not a valid approach to treat the cladding materials as semi-infinite sloid if the wall thickness is less than the thermal diffusion length. As pointed by Bruder et al. (2017), the wall thickness increasing leads to better thermal activity of cladding material and further results in higher CHF. This is validated with the pool boiling CHF experimental results of square plate heater. However, on the other hand, the wall thickness is comparable with the thermal diffusion length, the impacts of wall thickness on the transient heat transfer across the wall thickness can not be neglected within a complete bubble ebullition cycle, the boiling fourier number \( Fo^* \),

\[ Fo^* = \frac{4\alpha_s t_d}{(D_t)^2} \]  

(4.9)

and the transient surface heat flux across the wall thickness can be simply approximated by the transient heat transfer of constant heat flux, such that

\[ q'' \propto g(D_t) = \frac{0.5 \sqrt{\frac{F_o}{Fo^*}} + \frac{\pi}{8}}{D_t} \]  

(4.10)

, Eq.4.10 is valid because \( Fo^* \) within the scope of interest is less than 0.2. Otherwise, other correct form shall replace \( g(D_t) \). Fig.4-44 clearly demonstrates that under influential impacts of bubble boiling behaviors, the progressive increasing of wall thickness can deteriorate surface heat flux, also have negative influential impacts on pool boiling CHF. This competes with the thermal activity enhancement by the wall thickness to the pool boiling CHF. Those two competing mechanisms explain our
4.4.3 Effects of Thermal-physical Properties

Although temporal-spatial variations of working fluid phase change are a pronounced mechanism of nucleate boiling heat transfer before dry spots, the interfacial temperature between the solid region and the vapor region could be physically explained by the suddenly quenching phenomenon of two bodies, one of which undergoes the phase change (Loulou & Delaunay, 1997). Loulou & Delaunay (1997) derived the analytical expression of the interfacial temperature between two semi-infinite bodies as follows,

$$\frac{T_1 - T_s}{T_s - T_2} = \frac{\sqrt{(k \rho c_p)_2}}{\sqrt{(k \rho c_p)_1}} + 1,$$

where $T_s$ is the interfacial temperature, $T_1$ and $T_2$ are the initial temperatures of the body 1 and the body 2 respectively before contacting, $\sqrt{k \rho c_p}$ is called the thermal effusivity. The physical interpretation of thermal effusivity is referred to as thermal inertia representing the material ability of exchanging heat with its surroundings. A higher thermal effusivity can allow nucleation sites of a material to be thermally activated in a faster manner. At the occurrence moment of CHF, the heat flow is severely impeded by the vapor blanket of boundary layer separation model, the bubbly layer of bubble crowding model, the dry patch of sublayer dryout model, or the dry
wall of interfacial lift-off model (Konishi et al., 2013). Besides, the average thickness of these vapor obstacles is at the small scale and the temperature drop across the thickness of tested tube could be negligible.

Basu et al. (2002) indicated that the nucleation site density is proportional to the exponent of wall superheat. Under the saturated flow boiling, the wall superheat is the temperature drop between the wall and the working fluid. From this standpoint, the total heat flux across the wall could be partially dependent on the difference of thermal effusivity between the working fluid and the solid wall. Therefore, CHF could be correlated with thermal thermal effusivities of working fluid and wall material as follows,

$$q''_{cr} \sim \left( \sqrt{\frac{(k \rho c_p)_\text{wall}}{(k \rho c_p)_\text{fluid}}} + 1 \right). \quad (4.12)$$

The primary rationale behind the surface temperature overshoot is the local variation of the surface temperature. This transient temperature change can be characterized by the thermal diffusivities of wall material and working fluid because the importance of thermal diffusivity quantifies the heat transfer rate of a material from hot spots to cold spots. A wall material of higher thermal diffusivity can promote greater time-wise variation of surface temperature around nucleation sites, resulting in an earlier occurrence of CHF. On the other hand, the working fluid with higher thermal diffusivity such as nanofluids can lead to better heat dissipation from hot spots. Therefore, CHF is dependent on the relative heat dissipation between working fluid and heat surface material as follows,

$$q''_{cr} \sim \frac{(k \rho c_p)_\text{fluid}}{(k \rho c_p)_\text{wall}}. \quad (4.13)$$

Another evident interaction at the interface between the solid and the liquid layer is the hydraulic attachment of working fluid to the solid because of the tradeoff balance between adhesive force and cohesive force, which is the surface wettability as a result from the intermolecular interactions. The surface wettability was experimentally proven the influential impacts on CHF of pool boiling (G. H. Seo et al., 2016a) and
flow boiling (Jeong et al., 2008). The effects of surface material wettability on CHF are often quantitatively and qualitatively analyzed by the measure of the dynamic receding contact angle \( \theta_r \) (Kandlikar, 2001),

\[
q''_{cr} \sim (1 + \cos \theta_r)(\frac{2}{\pi} + \frac{\pi}{4}(1 + \cos \theta_r))^{0.5}.
\] (4.14)

It should be noted that the contact angle is also determined by surface morphologies including roughness and microstructure. To minimize the effect of the surface morphological characteristics on CHF, the heat transfer surface of tested material should be smooth and plain.

The discussions above support that CHF is affected by thermal-physical properties and hydraulic behaviors of materials. This study proposes a new parameter to define the nucleate boiling surface stability (NBSS) of heat surface material, defined as follows,

\[
\text{NBSS} = \left( \frac{\sqrt{(kp\rho c_p)_{wall}}}{\sqrt{(kp\rho c_p)_{fluid}}} + 1 \right) \times \frac{(\frac{k}{\rho c_p})_{fluid}}{(\frac{k}{\rho c_p})_{wall}} \times (1 + \cos \theta)(\frac{2}{\pi} + \frac{\pi}{4}(1 + \cos \theta))^{0.5}.
\] (4.15)

The physical interpretation of nucleate boiling surface stability is to quantify the ability that the material of heat transfer surface can sustain the nucleate boiling of working fluid.

Although thermal-physical material properties of tested tubes vary with the increasing of temperature, many metallic and nonmetallic materials show the monotonic parametric trends of those thermal-physical properties with respect to temperature within the temperature range of nucleate boiling regime. Characterization at the saturation temperature of working fluid could still provide some insights on impacts of thermal-physical material properties on CHF. To conveniently elaborate the impacts of thermal-physical material properties on CHF, the right hand sides of Eq. 4.12, Eq. 4.13 and Eq. 4.14 are respectively defined as the thermal effusivity ratio, the thermal diffusivity ratio and the wettability. Experimental conditions of flow boiling and geometrical dimensions of tested materials should be kept same and consistent.
in CHF experiments.

Effects of thermal-physical material properties on pool boiling CHF are more pronounced without the impacts of forced convection (Raghupathi & Kandlikar, 2017; Ho et al., 2016). This study investigates pool boiling CHF experiments of water and FC-72 on 10mm×10mm heat plates made of different tested materials. The

![Figure 4-45: Water pool boiling CHF as a function of thermal-mechanical properties of common materials, (a) thermal diffusivity, (b) thermal effusivity, (c) wettability, and (d) nucleate boiling surface stability.](image)

general trend of CHF shows an increasing parametric behavior with respect to thermal diffusivity ratio, as can be observed in Fig. 4-45(a) and Fig. 4-46(a). This further verifies that a higher thermal diffusivity of material results in a lower CHF. But the thermal diffusivity cannot fully explain the effects of heater materials on CHF alone because CHF can still vary significantly on the materials with similar thermal diffusivities (Fig. 4-45(a)) and vice versa (Fig. 4-46(a)). The parametric behaviors of CHF with respect to the thermal effusivity ratio demonstrate the similar patterns in pool boiling of water (Fig. 4-45(b)) and FC-72 (Fig. 4-46(b)). This suggests that
the way of heater materials affecting the occurrence of CHF should behave similarly in different working fluids. Despite of the similar effects of heater materials on pool boiling CHF of different working fluids, the parametric trend of CHF with respect to thermal effusivity ratio shows highly nonlinear behaviors. As for the effect of material wettability on CHF, the CHF model proposed by Kandlikar suggested CHF could be enhanced by the heater materials with higher wettability. However the experimental results in Fig. 4-45(c) and Fig. 4-46(c) contradicted the predicted parametric trend of CHF against material wettability in the Kandlikar’s model. It is because the effects of material wettability on CHF was correlated and incorporated in the Kandlikar’s model while the influences of thermal diffusivity and thermal effusivity were not evaluated to the triggering mechanisms of CHF.

As the aforementioned discussions, thermal diffusivity, thermal effusivity and material wettability are incapable of addressing the effects of heater materials on CHF.

Figure 4-46: FC-72 pool boiling CHF as a function of thermal-mechanical properties of common materials, (a) thermal diffusivity, (b) thermal effusivity, (c) wettability, and (d) nucleate boiling surface stability.
The new proposed thermal-mechanical material parameter, "nucleate boiling surface stability", aggregates their contributions to the triggering mechanisms of CHF. As can be observed in Fig. 4-45(d) and Fig. 4-46(d), the nucleate boiling surface stability of heater material can render a satisfactory rationale to the obvious CHF difference between different tested materials with an apparent increasing relation. It should be noted that both pool boiling experimental CHF results in Figs. 4-45 and 4-46 are obtained upon the horizontally-placed square heaters.

However, the proposed nucleate boiling surface stability is incapable of parameterizing the pool boiling CHF results of cladding tube materials (See Fig. 4-47). One feasible explanation behind this is that besides heat conduction between cladding solid and water/steam competing with heat convection mechanism of wall heat flux...
partition model, the heat radiation across the vapor micro-layer might have a noticeable contribution to the boiling heat transfer in the horizontally-placed tubing because the large bubbles slide upward on the outer surface cladding tube and small local hot patches are distributed on the cladding surfaces prior to the irreversible dry patch formation.

4.5 Conclusions

In the pool boiling experimental investigations of FeCrAl alloys, Zircalloys, Inconel alloys and stainless steels, the experimental results demonstrate that FeCrAl-C26M has higher pool boiling CHF and NB-HTC than other cladding samples. This speaks to the thermal superpriority of FeCrAl-C26M in the LWR core loading. However, the effect of tubing wall thickness on pool boiling CHF covered in this study contradicts with the effect of plate wall thickness on CHF. This could result from the geometrical scheme of heat transfer mechanism of the tubing heater. On the other hand, the parametric trend of pool boiling CHF with respect to the tubing wall thickness agrees with the experimental results of pool boiling CHF on the wire heaters with different wire diameters.

More importantly, the pool boiling experimental CHF results obtained from various cladding materials also confirm that material thermal-physical properties can contribute significantly to the pool boiling CHF & NB-HTC difference. But how the thermal-physical properties mechanistically affect the pool boiling heat transfer still remain vague even though several physical indicators of material thermal-physical properties reported in literatures are incapable of parameterizing the pool boiling CHF behaviors. Besides pool boiling CHF & NB-HTC, the material thermal-physical properties can also play a critical role in the bubble dynamics behaviors. On the cladding surfaces with similar surface roughness scales, Inconel 600 & 625 generate larger bubbles and have a smaller number of bubble crowds than SS304 & 314.

In this pool boiling CHF study of various cladding materials, it is confirmed that the post-CHF samples have higher pool boiling CHF values than their as-received
counterparts. But such this pool boiling CHF enhancement by oxide layer cannot be mechanistically rationalized by the present boiling theories reported in literatures. By comparing the oxide layer thickness on different cladding materials, it is found that FeCrAl-C26M has the thinnest one among all the tested materials. This implies that FeCrAl-C26M has the best resistance to the oxidization corrosion resulting from pool boiling CHF occurrence.
Chapter 5

Steady-State Flow Boiling
Experiments of ATF and Traditional Cladding Materials

In this chapter, the steady-state flow boiling experimental investigations were performed on a wide variety of metallic alloys including Zircaloys, ATF FeCrAl alloys, Inconel alloys and etc. Their T-H characteristics including ONB, CHF (DNB) and HTCs were experimentally probed across a wide range of mass flux and inlet subcooling.

5.1 Prologue

In LWRs, the forced convection nucleate boiling of pressurized water is the primary heat removal strategy to cool down the fuel assemblies of nuclear reactor core and transfer the thermal energy generated in the fuel pins to the steam turbine for the electricity conversion (Aksan, 2019). To exploit the phase-change heat absorption and improve the thermal transfer efficiencies, the nucleate boiling regime is preferred in the normal operations of LWRs. In the early stage of LWRs, the T-H characteristics were obtained from most of flow boiling experimental studies on the stainless steels and/or Inconel alloys. However, there are a limited number of the flow boiling
experiments on Zircaloy claddings. It is because in the past, the material thermal-
physical properties impacts on flow boiling were not paid with a great attention until
the advent of ATF concepts. The recent pool boiling experiments of ATF cladding
candidates showed that the thermal-physical properties of cladding materials might
be the primary contributors to the thermal performances differences between various
claddings and Chapter 3 presented the detailed discussions on the role of material
properties in the boiling heat transfer. Also the contributions of material-side factors
to the boiling performances difference are still not clear even though several attempts
have been made to rationalize and quantify the role of material thermal-physical prop-
erties in boiling heat transfer, especially for CHF (DNB) such as, volumetric heat ca-
pacity\(^1\) \((\rho C_p)\) (Raghupathi & Kandlikar, 2017), thermal effusivity\(^2\) \((\sqrt[3]{\rho C_p k})\)(Golobič
& Bergles, 1997; Arik & Bar-Cohen, 2003), thermal activity \(\delta_{th}\sqrt[3]{\rho C_p k}\)(Jo et al.,
2019a; Yeom et al., 2020), surface thermal economy parameter (S. K. Lee et al., 2019;
S. K. Lee, Liu, et al., 2020) . Because in a wide variety of metallic alloys, these
artificial parameters derived from material thermal-physical properties can not give a
clear mechanistic picture to how the boiling heat transfer is conjugated with material.
In light of this, the experimental scope of boiling heat transfer should be extended to
different heater materials as much as possible.

Although Chapter 3 outlines a general description to how the system boiling
conditions (mass flux and liquid subcooling) affect MC-BHT from the standpoint
of heat transfer mechanisms, it is still in need to further confirm the heat convection
dominance over the material-side heat conduction by increasing mass flux and/or
liquid subcooling. Compared with the pool boiling experiments of ATF claddings,
the flow boiling experiments of ATF claddings are still too limited to sufficiently
support the ATF-loaded LWRs. The purpose of this chapter is to experimentally
investigate the T-H characteristics of FeCrAl alloys as well as other cladding materials
and look at their responses to the system boiling conditions. Then the obtained lab-
scale experimental results will be utilized to numerically assess the T-H performances

\(^1\)it is also called thermal mass.
\(^2\)it is also called thermal inertia in some references.
of rod-bundle assembly at the component scale in the future extended study of this dissertation. The rest of this chapter is organized as follows: Section 5.2 gives a brief description to the experimental methodologies of steady-state flow boiling of various cladding materials. Section 5.3 elaborates the closures of nucleate flow boiling from ONB to CHF in details and addresses the roles of material thermal-physical properties. Section 5.4 presents the experimental results of ATF claddings and of other commercial alloys regarding to their ONB and NB-HTC characteristics and compares experimental results with the predicted results of empirical correlations, the experimental results of flow boiling CHF across a wide variety of metallic alloys are discussed and analyzed to clarify how the material thermal-physical properties affect the CHF from the viewpoints of heat flux partitioning in Section 5.5, also in Section 5.5 it is provided the experimental confirmation to the influence of mass flux & liquid subcooling to the material-dependent CHF for nuclear T-H communities, Section 5.6 analyzes the effects of T-H footprints on flow boiling CHF including post-CHF and post-nucleate boiling footprints, in Section 5.7, the implications of the experimental results in this chapter are made to support the near-term ATF loading licensing and assessments from the steady-state T-H safeties, and the concluding remarks of this chapter are summarized in Section 5.8.

5.2 Experimental Methodologies

The steady-state flow boiling experiments across a wide variety of cladding tube materials are performed on the UNM flow boiling loop (See Fig.5-1). Regarding to the components constituting the flow boiling loop, more details and explanations are provided in Section 2.2.2 of Chapter 2. To make sure that the deionized water is degassed so that there is no air bubble generated on the cladding surface, the working fluid of deionized water is kept at the saturation temperature of 95°C at least for 30 minutes. Then the temperature of working fluid is adjusted automatically by chiller and preheater down to the specified inlet temperature.

For the signifier of the steady-state condition, this experimental study adopts the
following criterion such that the temperature difference between two consecutive mean temperatures measured at an axial point is less than 0.5 °C, the mean temperature at an axial point is averaged by a time series of 30 measured temperatures points and all eight measured points are supposed to have the temperature difference less than 0.5 °C. The present steady state is maintained for 1 minute prior to jumping forward to the next steady-state cycle. When the temperature difference between two consecutive time-wise sampled data is greater than 30 °C, CHF occurs at this snapshot moment and then the DC power supply unit is automatically shut down immediately.

The power incremental strategy for steady-state flow boiling is to adopt the voltage-based control mode of DC programmable power supply unit at a small discrete step, $V(n + 1) = V(n) + \Delta V$ (5.1)

where $V(n)$ is the voltage of DC power supply at the present cycle of steady-state, $v(n + 1)$ is the power supply voltage at the next cycle, and $\Delta V$ is the minimum allowable voltage incremental step ($\sim 0.01$ Volt).

A circular tube with a length of 21.5 inches and an outer diameter of 0.375 inches is vertically mounted upon two orifice fittings. The detailed schematic view of test section is shown in Fig.5-2. The entrance length should be greater than 15 inches to ensure the fully developed water flow based on the correlation (Olson & Sparrow,
The test section is uniformly heated in the direct joule heating manner that is powered by a direct current power supply. As observed in Fig.5-2, the actual heated length is 2 inches due to the limited power capacity. The actual heated part of test section is wrapped by a thick pad of thermal insulation foam to minimize the heat loss. Two T-type thermocouples and two pressure transducers are installed respectively upon two orifice tube fittings to measure pressures and temperatures for both inlet and outlet. Eight K-type thermocouples are placed at the outer tube surface in pairs to measure the outer surface temperatures axially. To minimize the contact resistance of current at the junction from power terminals to the tested tube, a very high electrically conductive epoxy is spread all over the contacting surfaces. The corresponding measurement uncertainty of heat flux can be found in Section 4.2.2 for the tested cladding materials. The parametric range of flow boiling conditions are tabulated in Tab.5.1. It is assumed that the heat loss could be negligible compared with the heat flux transferred to upward flow. It is noted that all the tested specimens have the exactly same wall thickness of 0.02 inch for eliminating the effects of wall thickness on flow boiling HTC. The measurement of heat flux $q''$ is calculated as

$$q'' = \frac{\Delta T \cdot \frac{d}{d'} \cdot \frac{1}{\rho \cdot c_p}}{\frac{1}{2} \cdot \frac{2}{\pi}}$$
Table 5.1: Experimental Conditions of Steady-State Flow Boiling

<table>
<thead>
<tr>
<th>parameter</th>
<th>range</th>
<th>uncertainty</th>
</tr>
</thead>
<tbody>
<tr>
<td>pressure</td>
<td>84 kPa</td>
<td>N/A</td>
</tr>
<tr>
<td>mass flux- (G)</td>
<td>(200 \sim 3000) kg/(m(^2\cdot)s)</td>
<td>(\pm 10) kg/(m(^2\cdot)s)</td>
</tr>
<tr>
<td>inlet subcooling- (\Delta T_{in,sub})</td>
<td>(0 \sim 50^\circ)C</td>
<td>(\pm 1^\circ)C</td>
</tr>
</tbody>
</table>

follows,

\[ q'' = \frac{UI}{A} \tag{5.2} \]

where \(U\), \(I\) and \(A\) are measurement data of voltage, current and heat transfer area respectively. In this experimental study, the flow boiling HTC is calculated based on the hottest point of tested specimens, that is, the surface temperature at the outlet of tested section \(T_s\). The bulk temperature of fluid is approximated by the averaged temperature between the inlet and outlet temperatures \(T_{ave}\). Then, the flow boiling HTC is calculated by Eq. 5.3

\[ \text{HTC} = \frac{q''}{T_s - T_{ave}} \tag{5.3} \]

From Eq.5.3, the measurement uncertainties of HTC can be calculated.

### 5.3 Closures of Nucleate Flow Boiling

Most thermal fluid management systems are preferred to work at the nucleate boiling regime for making use of phase-change latent heat absorption. The nucleate flow boiling regime is characterized by three different T-H parameters, ONB deciding the lower limit of heat flux to activate the phase change of working fluid, CHF characterizing the sudden surface temperature overshooting from nucleate boiling to film boiling and demarcating the upper power limit of thermal two-phase fluid system, NB-HTC physically representing how efficiently the boiling heat transfers from material solids to working liquids by the means of phase change latent heat evaporation. ONB, NB-HTC and CHF consist of a complete closure form of nucleate flow boiling regime. The corresponding analyses on these three T-H parameters are expanded in
the following three subsections.

5.3.1 ONB

To ultimately explore the advantage of latent heat absorption of liquid resulting from the phase change, thermal energy systems are preferably expected to operate within the nucleate boiling regime. As the lower limit of heat flux to activate the nucleate boiling, ONB is characterized phenomenologically by slow detachment of spare vapor bubbles from cladding surface. The far-field factors including pressure, liquid subcooling and mass flux exert substantial impacts on the ONB occurrence. Besides, the near-field factors including cladding materials, surface morphological features and geometrical dimensions play crucial roles in the boiling incipience. The enlarging accumulation of flow boiling experimental database has underpinned various empirical correlations and supported mechanistic models to predict the ONB across a wide variety of test conditions (Okawa, 2012). Regarding to the experimental investigations of ONB obtained on various cladding materials, more details can be found in the next chapter. In this chapter, the ONB of ATF and traditional cladding materials is briefly discussed together with the NB-HTC.

5.3.2 NB-HTC

NB-HTC is of almost importance to thermal energy system management because of its application to the thermal efficiency/design limit assessments of flow boiling channel. For instance, one of primary thermal safety concerns in the nominal operations of LWRs is to study PCT margins of nuclear fuel assembly with the assist of NB-HTC correlations. Insofar, there have been a plethora of NB-HTC experimental datasets across a wide range of pressure, mass flux, and liquid subcooling using various working liquids and cladding materials. These experimental datasets have sufficiently unveiled the physical rationales of far-field/near-field factors behind their influential impacts on NB-HTC. Besides, the nucleate boiling itself has pronounced effects on NB-HTC from the bubble-induced turbulence enhancement mixed with
the latent heat absorption. Although the existing experimental datasets of NB-HTC have covered boiling behaviors of various liquids on many cladding materials across a wide range of flow boiling conditions, such these experimental investigations are not performed in a systematic manner. Moreover, experimental studies on the flow boiling thermal characteristics of ATF claddings are still too limited to support the near-term ATF deployment in LWRs. Besides, the comparative experiments of the within-family/between-family materials are still in need to facilitate the theoretical understanding of the material-dependent flow boiling and thus provide better guidelines to development of better thermal engineering materials including HEAs. The following subsection also demonstrates how the cladding wall thickness affects the NB-HTC across a wide range of mass flux and inlet subcooling. Regarding to the impacts of local liquid subcooling, the corresponding experiments are presented in the next chapter.

5.3.3 CHF (DNB, Dryout, Burnout)

ONB decides the lower heat flux limit of nucleate boiling while CHF demarcates the upper limit of power in thermal energy system. CHF is phenologically characterized by the irreversible formation of vapor patch/layer on cladding surface, thus leading to the sudden transition from nucleate boiling to film boiling with an abrupt surface temperature overshooting. The severe HTC deterioration and surface temperature overshooting make the thermal stress beyond the material limit, break down the structural integrity of claddings, even result in thermal shock damages. As pointed out in Ref.(Bruder et al., 2017), the triggering mechanisms behind flow boiling CHF occurrence are diverse according to the vapor blanket layer/dry patch behaviors. In the subcooled flow boiling of PWR, CHF is more considered as DNB because of the irreversible formation vapor dry patch while the saturated flow boiling of BWR is more likely to have dryout(burnout) occurred on cladding surfaces due to the vapor blanket layer. Although there have been substantial amount of experimental efforts on CHF and their experimental CHF results have been tabulated for thermal safety margin assessments including the 1995/2006 CHF LUTs (Groeneveld et al., 2007),
the 1999 CHF LUTs (Mudawar & Bowers, 1999; Hall & Mudawar, 1999) and the 2000 CHF LUT (H. C. Kim et al., 2000), there have been a few experiments reported for the systematic investigations on how mass flux and inlet subcooling affect the flow boiling CHF difference gaps between various cladding materials. In this chapter, the systematic experimental studies are performed to investigate the flow boiling CHF difference gap between various cladding materials and the impacts of mass flux and of inlet subcooling on this difference gap.

5.4 The Impacts of Cladding Materials on NB-HTC: Studies at Two Regimes

This section starts revisiting the physics contributions of mass flux, inlet subcooling and heat flux on the HTCs of FeCrAl.

5.4.1 Impacts of Mass Flux, Inlet Subcooling and Heat Flux

As Fig. 5-3 shown, the flow boiling regime can be divided into the single-phase and two-phase stages. The SP-HTC characterizes the effects of mass flux, liquid

Figure 5-3: Parametric trend of HTC with respect to heat flux for FeCrAl-C26M at $G = 300$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ$C
subcooling and thermal-physical properties on heat convection while the two-phase heat transfer coefficient (usually called NB-HTC) reflects the enhancement by bubble-induced turbulence. The transition point of boiling regime is the onset of nucleate boiling (ONB) at which bubbles are phenomenologically activated on wall surface. Beyond ONB, the increasing of heat flux allows more bubbles generated on wall surface to further promote the turbulence enhancement on NB-HTC.

In comparison with NB-HTC, SP-HTC can more mechanistically reflects the contributions of mass flux, liquid subcooling and thermal-physical properties to heat convection because of bubble-induced turbulence. At $G=300 \text{ kg/(m}^2\cdot\text{s})$, the flow boiling HTC of FeCrAl-C26M under the inlet subcooling varying from $10^\circ\text{C}$ to $50^\circ\text{C}$ is shown in Fig. 5-4. It is obviously illustrated that ONB shows the increasing behavior while SP-HTC decreases respectively with respect to the increasing of inlet subcooling. This is quite reasonable, because higher inlet subcooling needs more thermal energy to heat up coolant before the bubble activation, and results in thicker boundary layer further deteriorating SP-HTC. At the nucleate boiling regime, NB-HTC of FeCrAl-C26M decreases over the increasing of inlet subcooling. This could be also attributed to the thicker boundary layer resulting from higher inlet subcooling. It is clearly evident that the difference gap of NB-HTC between different inlet

Figure 5-4: Parametric trend of HTC with respect to heat flux for FeCrAl-C26M at $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and under 5 different inlet subcoolings
subcoolings is exaggerated as over the increasing of heat flux. This speaks the possibility that the higher inlet subcooling can suppress the bubble-induced turbulence. Because it can be rationalized by the experiment results (Goel et al., 2018) that the increasing of liquid subcooling can result in the smaller bubble diameters including bubble departure diameter, maximum bubble diameter and bubble lift-off diameter. Intuitively speaking, the larger bubbles could possibly result in the more turbulent bubbly flow. In the FeCrAl-C26M flow boiling HTC case of $G = 300 \text{ kg/(m}^2\text{s)}$ and $\Delta T_{in,sub} = 10^\circ \text{C}$, there is a sudden decrement of NB-HTC with respect to the heat flux being reflected in Fig.5-4. This could be mechanistically explained by the abrupt increasing of active nucleation site density if the wall superheat is beyond a certain value, especially when surface heat flux gets approach to CHF. This abrupt increasing of site density was experimentally reported by Zeng & Klausner (1993). In light of this, the abrupt increasing of active nucleation site density can result in more populous and denser bubbles generating on the cladding surfaces. This further leads to the deterioration of NB-HTC. Fig. 5-5 shows experimental the flow boiling HTC of FeCrAl-C26M across a wide range of mass flux. As expected, both SP-HTC and ONB increase over the increasing of mass flux. However, beyond the ONB point, NB-HTC

![Figure 5-5: Parametric trend of HTC with respect to heat flux for FeCrAl-C26M at $\Delta T_{in,sub} = 10^\circ \text{C}$ and under 7 different mass fluxes](image)
is gradually enhanced by the increasing of heat flux and the bubble-induced turbulence tends to dominate over the effect of mass flux on heat convection mechanism. As Fig. 5-5 shown, NB-HTCs between 300 kg/(m²·s) and 500 kg/(m²·s) converge to the same parametric trend with respect to heat flux, and so are NB-HTC between 1000 kg/(m²·s) and 1500 kg/(m²·s), and NB-HTC between 2500 kg/(m²·s) and 3000 kg/(m²·s). This implies that the effect of heat flux on NB-HTC can be more significant than that of mass flux. More importantly, the bubble-induced turbulence can not be neglected when heat flux is high.

5.4.2 Wall Thickness Impacts

There are several experimental studies on the effects of wall thickness on CHF including this dissertation. However, the experimental studies on impacts of wall thickness on HTCs were never reported. In this dissertation, this missing gap is closed by SS 304 & 316 tubes with three different wall thickness (0.010 in, 0.015 in, and 0.020 in).

As shown in Fig.5-6, the 0.015-thick SS 304 tube has higher NB-HTC than SS 304 tubes with other two thicknesses. However, the difference gap of SS 304 NB-HTC is gradually closed by the increasing of inlet subcooling and even there is no appreciable difference between three SS 304 tubes under the inlet subcooling of 50°C (See Fig.5-6(f)). The increasing of mass flux can exert similar effects on the difference gap of SS 304 NB-HTC between three different thick tubes (See Figure 5-7). However, the further increasing of mass flux doesn’t close the difference gap of SS 304 NB-HTC when mass flux is beyond 2000 kg/(m²·s). The experimental results shown in Fig.5-7 demonstrate that the 0.015-thick SS 304 tube has higher flow boiling NB-HTC than the other two cases. However, when mass flux is beyond 2000 kg/(m²·s), the flow boiling NB-HTC superiority of 0.015-thick SS 304 tube is superseded by the 0.020-thick SS 304 tube. Therefore, the effect of tubing wall thickness is insignificant when the system is either under the high liquid subcooling or at the immediate mass flux. Generally speaking, the ONB of the 0.015-thick SS 304 tube is higher than SS 304 tubes with other two wall thicknesses.

Similarly, the higher NB-HTC of the 0.015-thick SS 316 can be also observed than
Figure 5-6: Flow boiling HTCs of SS 304 tubes with three wall thicknesses at the mass flux of 300 kg/(m$^2$·s) and the SS 316 tubes with two other thicknesses (See Fig.5-8(a)). And also the increasing of inlet subcooling can gradually close the difference gap of NB-HTC between three different thick SS 316 tubes as shown in Fig.5-8. Similar to the effects of mass flux on the SS 316 tubes with two other thicknesses (See Fig.5-8(a)). And also the increasing of inlet subcooling can gradually close the difference gap of NB-HTC between three different thick SS 316 tubes as shown in Fig.5-8. Similar to the effects of mass flux on
the SS 316 NB-HTC sensitivity to tubing wall thickness, the increasing of mass flux can gradually reduce the difference gap (See Fig.5-8(a) and Figs.5-9(a)∼5-9(c)). Also when the mass flux reaches up to 2000 kg/(m²·s), the further increasing of mass...
flux starts exaggerating the difference gap and enables the higher NB-HTC of the 0.010-thick SS 316 than other two SS 316 tubes. But for the case of SS 304 (Fig.5-7), the 0.020-thick SS 304 has higher NB-HTC than other two SS 304 tubes if mass flux is over 2000 kg/(m$^2$·s). Experimental studies of both SS 304 and 316 tubes to the impacts of wall thickness on NB-HTC speak to that there might be the optimal wall thickness of cladding material maximizing the NB-HTC for the weak heat convection regime of low inlet subcooling and low mass flux.

Speaking from the physics standpoint of MC-BHT, the high inlet subcooling and/or mass flux make the heat convection contributed by far-field mechanisms dominate over the near-field mechanisms related with the material-side factors. The literature review studies in Chapter 3 explicitly rationalizes how MC-BHT is affected by the increasing of mass flux and/or inlet subcooling. However, the theoretical bases of MC-BHT are still too weak to explain the effects of tubing wall thickness on NB-HTC.

### 5.4.3 Within-Family Materials’ Impacts

The subsection above gives a clear experimental evidence to that effect of tubing wall thickness can be appreciable under the weak heat convection regime. Besides the tube wall thickness, several reported experimental studies have shown that the cladding materials can also play noticeable roles in MC-BHT especially for CHF and NB-HTC. In light of this, this chapter qualitatively studies how the cladding materials affect the NB-HTC from two perspectives, within-family and between-family materials.

The within-family materials are defined as a similar series of cladding materials, which share same chemical elements but vary in terms of their respective concentrations, for example, in the family of SS, there are at least 10 variants including 304, 310, 316, 321 and 347, and in the family of nickel alloys, Inconels 200, C-276, 400, 600, 625 and 700 are often used as the thermal engineering materials in heat exchangers, structural elements and steam components in LWRs, the traditional cladlings covering from Zr705, Zirlo, Zircaloy 2/4 to M5 are used to encase nuclear fuel pellets as well as the recent FeCrAl concepts of ATF cladding.
In this subsection, the comparative experiments of within-family materials are performed to study their difference gaps under the same flow boiling conditions. Experimental NB-HTCs of SS 304, 316, 321 and 347 under different inlet subcoolings
at the mass flux of 300 kg/(m²·s) are presented in Fig.5-10 and show that the SS 347 tube has higher NB-HTCs than other three SS tubes. Insofar, it is difficult to identify whether the thermal-physical properties or the surface roughness enhances

Figure 5-9: Flow boiling HTCs of SS 316 tubes with three wall thicknesses at the inlet subcooling of $\Delta T_{in,sub} = 10^\circ C$
Figure 5-10: Flow boiling HTCs of SS 304, 316, 321 & 347 tubes at the mass flux of 300 kg/(m²·s) (OD 0.375”, WT 0.020”)

(a) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 10^\circ\text{C}\)

(b) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 15^\circ\text{C}\)

(c) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 20^\circ\text{C}\)

(d) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 30^\circ\text{C}\)

(e) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 40^\circ\text{C}\)

(f) \(G = 300 \text{ kg/(m}^2\text{·s)}\) and \(\Delta T_{in, sub} = 50^\circ\text{C}\)

SS-347 NB-HTC, or even both. Because the material-side factors of four SS 304 tabulated in Tab.5.2 can infer that both the thermal activity and Ra of SS-347 are higher than the other three SS tubes. As expected, the difference gaps of SP-HTC,
NB-HTC and ONB between four variants of SS tubes can be reduced by the increasing of inlet subcooling (See Fig.5-10). Besides the inlet subcooling, the mass flux can also exert similar impacts on the difference gaps of flow boiling HTCs between four SS variants (See Fig.5-11).

Table 5.2: Properties of Four SS Tubes

<table>
<thead>
<tr>
<th>SS</th>
<th>$\rho$ (kg/m$^3$)</th>
<th>$k$ (W/(m·K))</th>
<th>$C_p$ (J/(kg·K))</th>
<th>Ra ($\mu$m)</th>
<th>Vendor</th>
</tr>
</thead>
<tbody>
<tr>
<td>304</td>
<td>7869.12</td>
<td>14.15</td>
<td>502.08</td>
<td>0.38</td>
<td>McMaster-Carr</td>
</tr>
<tr>
<td>316</td>
<td>7921.80</td>
<td>15.11</td>
<td>508.86</td>
<td>0.66</td>
<td>McMaster-Carr</td>
</tr>
<tr>
<td>321</td>
<td>7927.52</td>
<td>15.14</td>
<td>500.34</td>
<td>0.56</td>
<td>McMaster-Carr</td>
</tr>
<tr>
<td>347</td>
<td>7960.62</td>
<td>21.40</td>
<td>510.45</td>
<td>2.56</td>
<td>TW-Metals</td>
</tr>
</tbody>
</table>

Thermal-physical properties of four SS tubes are evaluated at 100°C

Apart from the stainless steels, Inconel 600 & 625 were considered as the mainstream engineering materials of PWR steam generator, however recent days, they have been conceptualized for the fuel cladding materials in some supercritical water reactor designs (German & Lin, 2021) because of their preferable corrosion resistance in the high-temperature/high-pressure water. However, the influences of mass fluxes and of inlet subcoolings on Inconel 600 & 625 HTCs are different from the cases of SS variants (See Fig.5-12 and Fig.5-13). Experimental results in Fig.5-12 and Fig.5-13 directly demonstrate the NB-HTC of Inconel 600 is higher than that of Inconel 625 while there is no appreciable difference of ONB or SP-HTC between two variants of Inconel across a wide range of mass flux and inlet subcooling. Although higher inlet subcooling can close the difference gaps of HTCs between Inconel 600 & 625, the immediate inlet subcooling exaggerates the difference gap. On the other hand, the increasing of mass flux doesn’t alleviate the impacts of cladding materials on the NB-HTC difference gap especially when heat flux is dominant enough to generate the bubble turbulence.

Recent days, DOE has been advancing the ATF concepts including the novel fuel pellets and cladding materials. One of near-term candidates is the FeCrAl alloy family including dozens of potential variants. In the experimental studies of flow boiling HTCs, three FeCrAl variants including FeCrAl-B126Y, FeCrAl-B136Y and
Figure 5-11: Flow boiling HTCs of SS 304, 316, 321 & 347 tubes under the inlet subcooling of 10°C (OD 0.375”, WT 0.020”)

FeCrAl-C26M are employed to understand how mass flux and inlet subcooling affect the difference gap. The experimental HTCs results across a wide range of flow boiling conditions (See Fig.5-14 and Fig.5-15) demonstrate that FeCrAl-B136Y has higher
HTCs than other two FeCrAl variants. This is similar to the pool boiling HTC results of three FeCrAl variants in Chapter 4. It is also observed that in Fig.5-14 the increasing of inlet subcooling gradually suppresses the difference gaps of flow boiling HTCs and ONB between three FeCrAl variants. On the contrary, Fig.5-15 illustrates
that the increasing of mass flux does close such difference gaps of ONB and HTCs, and on the other hand, the further increasing of mass flux exaggerates difference gaps of ONB and NB-HTC while reduces that gap of SP-HTC. These experimental cases
of three FeCrAls flow boiling HTCs are analogous to those of four stainless steels.

(a) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 10^\circ\text{C}$ (b) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 15^\circ\text{C}$

(c) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 20^\circ\text{C}$ (d) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 30^\circ\text{C}$

(e) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 40^\circ\text{C}$ (f) $G = 300 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 50^\circ\text{C}$

Figure 5-14: Flow boiling HTCs of three FeCrAl variants at mass flux of 300 kg/(m$^2\cdot$s)

Insofar, before the large-scale ATF deployment in the LWRs, the mainstream fuel-clad entities are still the oxide(UO$_2$)-Zircalloys occupying a large proportion of LWR in the current nuclear energy fleet of USA. In this dissertation study, three zircalloys
Figure 5-15: Flow boiling HTCs of three FeCrAl variants under inlet subcooling of $\Delta T_{in,sub} = 10^\circ C$

(a) $G = 500 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

(b) $G = 1000 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

(c) $G = 1500 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

(d) $G = 2000 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

(e) $G = 2500 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

(f) $G = 3000 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10^\circ C$

are experimentally investigated for their flow boiling HTCs including Zirlo, Zircaloy-4, and Zr705. In these Zircaloys flow boiling HTC studies, the experimental results in Figs.5-16 and 5-17, Zirlo has higher HTCs and ONB than Zr705 and Zircaloy-4.
Although the effects of inlet subcooling on NB-HTCs are quite obvious, these inlet subcooling effects compete with the bubble-induced turbulence effects. Because as heat flux reaches beyond some certain level, NB-HTC behaviors of three Zircaloys start varying. However, as the mass flux increases, the difference gaps between three
Figure 5-17: Flow boiling HTCs of three zircalloys under inlet subcooling of $\Delta T_{in,sub} = 10^\circ C$.

(a) $G = 300$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$ (b) $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$

(c) $G = 1000$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$ (d) $G = 1500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$

(e) $G = 2000$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$ (f) $G = 2500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ C$

zircalloys are exaggerated instead. Moreover, the experimental results of Zircalloys flow boiling HTC in Fig.5-17 also show that the bubble-induced turbulence resulting from heat flux competes with the physical contributions of mass flux. Besides these, the higher heat flux can significantly improve the mechanistic role of solid-liquid heat.
conduction in the heat transfer across the triple-phase interface. Because higher solid temperature enhanced by the increasing of heat flux leads to the increasing of thermal-physical properties in thermal conductivity and specific heat capacity. According to the heat conduction between two semif-infinite bodies (See Eq.4.11), the thermal effusivity could be a decisive indicator of the solid/liquid heat conduction. More mechanistic explanations are elaborated in Part 5.4.5 based on the existing boiling heat transfer knowledge.

5.4.4 Between-Family Materials’ Impacts

In general, FeCrAl claddings have higher flow boiling HTCs than Zircaloys. However, this superiority of FeCrAl cladding is appreciable under the low mass flux and low inlet subcooling.

5.4.5 Results Discussions and Implications

To compare the mechanistic contributions resulting from mass flux, inlet subcooling and cladding thermal-physical properties, this study herein adopts the material-conjugated heat flux partitioning model to assess their respective roles in material-dependent flow boiling HTCs.

**Forced Convection:** the heat convection is mainly promoted by the flow shear force driven by pump. The present empirical correlations of subcooled flow boiling HTCs $N_u$ are based on mass flux $Re$, thermal-properties of liquid $Pr$, inlet subcooling $Ja^*$ and heat flux $Bo$, as follows,

$$N_u = \frac{hD_{in}}{k_l}$$  \hspace{1cm} (5.4)

where $h$ is the heat transfer coefficient, $D_{in}$ is the inner diameter of cladding tube, and $k_l$ is the thermal conductivity of water;

$$Re = \frac{GD_{in}}{\mu_l}$$  \hspace{1cm} (5.5)
Figure 5-18: Flow boiling HTCs of Zircaloy4 and FeCrAl-C26M under $G = 300 \text{ kg/(m}^2\text{·s)}$ where $\mu_l$ is the dynamic viscosity of water;

$$P_T = \frac{C_p \mu_l}{k_l} \quad (5.6)$$
where $C_{pl}$ is the specific heat capacity of water;

$$J\theta^*_{in} = \frac{h_f - h_{in}}{h_g - h_f}$$  \hspace{1cm} (5.7)
where \( h_f \) and \( h_g \) are specific enthalpy energy of water and steam at the saturation temperature respectively, and \( h_{in} \) is the specific enthalpy of water at the inlet;

\[
Bo = \frac{q''}{G(h_g - h_f)} = \frac{q''}{G\Delta h_{fg}}
\]

(5.8)

where \( q'' \) is the surface heat flux, and \( \Delta h_{fg} \) is the latent vaporization heat. Insofar, there are a large number of \( Nu = f(Re, Pr, Ja_{in}^*, Bo) \) empirical correlations proposed for a wide range of flow boiling conditions. The review studies of subcooled flow boiling HTCs (Fang et al., 2017; Str˚ak & Piasecka, 2020; Mohanty & Das, 2020) investigated recent progresses on various empirical correlations applicability to a wide range of flow boiling cases. Based on those literature review studies, the Shah correlation is employed to evaluate experimental flow boiling HTCs in this dissertation study,

\[
Nu_{SP} = 0.023Re^{0.8}Pr^{0.4}
\]

(5.9a)

\[
Nu_{NB} = \psi Nu_{SP}
\]

(5.9b)

where \( Nu_{SP} \) and \( Nu_{NB} \) are \( Nus \) of SP-HTC and NB-HTC respectively, and \( \psi \) is the enhancement factor correlated by the following equation,

\[
\psi = 267Bo^{0.86}(Ja_{in}^*)^{-0.6}Pr^{0.23}
\]

(5.10)

Through Eq.5.4 to Eq.5.10, the predicted cladding surface temperature can be evaluated for the competing behavior of forced convection with the heat conduction in the boiling heat transfer mechanisms.

**Thermal-Physical Properties Effects:** As pointed by the heat flux partitioning model (Amidu et al., 2018; Basu et al., 2005) (See Eq.4.6), the convective partition that is only independent of material-side factors is potentially impacted by the far-field mechanisms contributed by mass flux and inlet subcooling while material-side factors are much involved in the other two partitions, the evaporative heat flux and the quenching heat flux. The quenching heat flux quantifies the heat transfer of re-
versible dry patches by cooling of relative cold liquid supply right after the bubbles detachment. As shown in Tab.5.3, as the progressive increasing of surface heat flux, the evaporative and quenching mechanisms become more dominant over the convective mechanism. Given that the mass flux is 200 kg/(m$^2\cdot$s) and $\Delta T_{in,sub}$ is 3°C, the far-field mechanisms are expected not so dominant as the near-field mechanisms at the higher heat flux. In light of this, this study explains the experimental results from

<table>
<thead>
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<th>Case</th>
<th>$q''_c$</th>
<th>$q''_e$</th>
<th>$q''_q$</th>
<th>total</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>102.52</td>
<td>13.31</td>
<td>49.78</td>
<td>165.61</td>
</tr>
<tr>
<td>2</td>
<td>137.97</td>
<td>53.62</td>
<td>149.73</td>
<td>341.32</td>
</tr>
<tr>
<td>3</td>
<td>159.59</td>
<td>52.27</td>
<td>257.18</td>
<td>469.05</td>
</tr>
<tr>
<td>4</td>
<td>117.86</td>
<td>117.81</td>
<td>356.87</td>
<td>592.54</td>
</tr>
</tbody>
</table>

the material-conjugated heat flux partitioning model in terms of the evaporative and quenching parts. This transient quenching heat transfer is simplified by the heat conduction between two semi-infinite contact bodies in this study, and the quenching heat flux (Eq.4.6c) is modified to incorporate the role of heater solid as follows,

$$q''_q = \frac{(\sqrt{k \rho c_p})_s (\sqrt{k \rho c_p})_l \Delta T_{wall}}{(\sqrt{k \rho c_p})_s + (\sqrt{k \rho c_p})_l \sqrt{\pi l_w}}$$

(5.11)

where $(\sqrt{k \rho c_p})_s$ and $(\sqrt{k \rho c_p})_l$ are thermal effusivities of heater solid and working liquid respectively. In Eq.5.11, one of theoretical assumptions is that the cladding thickness is infinitely thick. However, this assumption can not be met in the practical applications because of the wall thickness limits considering from various parasitic effects such as neutrons shielding. The evaporative heat flux indicates the latent heat vaporization characterized by a large number of bubble crowds growing and detaching from the cladding surfaces,

$$q''_e = N f \rho_v \Delta H_{fg} \pi \frac{D^3}{6}$$

(5.12)

where $N$ is the active nucleation site density, $f$ is the bubble departure frequency...
that is related with \( t_w (t_w = 0.8/f) \), and \( D_{eq} \) is the equivalent diameter of departure bubble. How \( N \), \( f \) and \( D_{eq} \) are dependent on thermal-physical properties of liquid and solid directly determines the impacts of cladding materials on the evaporative heat flux. In the studies of Kant & Weber (1994); Benjamin & Balakrishnan (1997), it was pointed that \( N \) is proportional to the ratio of liquid thermal-effusivity over solid thermal effusivity as follows,

\[
N = 218.8(Pr)^{1.63}\left(\frac{\sqrt{k \rho c_p} \Delta T_{wall}}{\sqrt{k \rho c_p} \Delta T_{wall}}\right)^{0.4}
\]

where \( \omega \) is an indicator of surface roughness contrition to \( N \), as follows,

\[
\omega = 14.5 - 4.5 \frac{RaP}{\sigma} + 0.4\left(\frac{RaP}{\sigma}\right)^2
\]

where \( Ra \) is the arithmetic average roughness, \( \sigma \) is the liquid surface tension, and \( P \) is the external pressure of system. L. Zhang et al. (2021) proposed a new analytical model of bubble departure frequency based on a semi-infinite wall with a convective boundary condition, as follows,

\[
\begin{align*}
& f^* D^* = 1 \quad (5.15a) \\
& D^* = D^{0.5} \quad (5.15b) \\
& f^* = \left(\frac{C_w^2}{\nu l \alpha_l} \right)^2 (\nu l \alpha_l)^{-0.5} \alpha_s^0.5 f \quad (5.15c)
\end{align*}
\]

where \( f^* \) and \( D^* \) are the scaled bubble departure frequency and diameter, \( \nu l \) is the kinematic viscosity, \( \alpha_s \) and \( \alpha_l \) are thermal diffusivities of solid and liquid respectively, \( C_w \) is a constant independent of cladding and fluid properties (\( C_w \approx 4.5 \) is recommended ). Eq.5.15c speaks to that the bubble departure frequency is directly impacted by solid thermal effusivity. On the other hand, bubble departure diameter is subjected to the thermal-physical properties of liquid and the surface contact angle and many correlations have been proposed to nuclear T-H community in recent years. With the assist of those empirical correlations, the material-conjugated evaporative
heat flux is in a complete closure form. In this study, the Cole correlation (L. Zhang et al., 2021) is employed to predict the equivalent bubble departure diameter as follows,

\[ D_{eq} = \theta_r \left( \frac{\sigma}{g(\rho_l - \rho_v)} \right)^{0.5} \frac{\rho_l c_p l T_s}{\rho_v \Delta H_{fg}} \]  

(5.16)

where \( \theta_r \) is the receding contact angle of boiling surface. Those equations discussed above form a complete closure model to assess the effects of thermal-physical properties on boiling heat transfer. Eq.5.15 and Eq.5.16 can be extended to the flow boiling conditions. Similarly, a nusselt number of material-side factor is proposed based on the evaporative and quenching heat flux to cross-compare forced-convection mechanism as follows,

\[ N_u_{material} = \frac{D_{in}}{k_l} \left( \frac{\sqrt{k p c_p}_{s}}{\sqrt{k p c_p}_{l}} + \frac{1}{\sqrt{\pi t_w}} \right) + N f \rho_v \Delta H_{fg} \pi \frac{D_{eq}^3}{6 \Delta T_{wall}} \]  

(5.17)

which defines how material-side factors affect heat transfer coefficients. Comparative analyses between Eq.5.17 and Eq.5.9 can provides us the intrinsic understanding of how the far-field heat convection mechanism competes with the near-field mechanisms that are dominated by thermal-physical properties. Both Fig.5-20 and Fig.5-21 speak to that the progressive increasing of mass flux and/or liquid subcooling can suppress the material evaporative and quenching dominance over the heat convection mechanism, and the far-field mechanisms governed by the mass flow rate and inlet

![Figure 5-20: Suppressing effects of mass flux on \( N_u_{material} \) under \( \Delta T_{in,sub} = 10 ^\circC \)](image)
subcooling increase their contribution proportion in the total heat transfer and result in a significant reduction of near-field mechanisms related with thermal-physical properties. The implications of those results are that under the nominal operations of PWR, it is conservatively safe to assess the flow boiling HTCs of ATF-claddings, otherwise material-dominant quenching and evaporative mechanisms would take over the primary role of heat transfer if ATF-loaded PWR cores are under the progression of DBAs such as LOFA and SBO.

Although the increasing of inlet subcooling deteriorates flow boiling HTCs, it can help suppress effects of material properties on the difference of flow boiling HTCs. High mass flux can reduce the difference of flow boiling HTCs while intermediate mass flux can exaggerate these difference gaps. The increasing of either mass flux or inlet subcooling can weaken the enhancement contribution of bubble-induced turbulence to the heat convection. This further implies that NB-HTC is subjected to the competing mechanisms among system conditions, thermal physical properties of solid/liquid, and heat flux.

5.5 CHF Dependence on Material-Side Factors

The section above studies the HTCs and ONBs from two comparative perspectives across a wide range of flow boiling conditions. This section experimentally charac-
terizes how the materials-side factors affect the flow boiling CHF and how the flow boiling conditions have influential impacts on the material-sensitivity CHF.

5.5.1 Wall Thickness Effects

Fig. 5-22 shows the impacts of cladding wall thickness on SS 304 & 316 flow boiling CHF under the weak heat convection regime. The several past flow boiling CHF experimental results (Bruder et al., 2017) revealed that the wall thickness effects on flow boiling CHF could be similar to that of pool boiling CHF. As shown in Fig. 5-22, the effects of wall thickness on flow boiling CHF are more sophisticated than ever thoughts reported in other experimental studies (Bruder et al., 2017). The parametric trend of SS 316 flow boiling CHF decreases, then increases and finally decreases with respect to the wall thickness. This agrees with the pool boiling CHF of SS 304. However, within the investigated scope of SS 304 wall thickness, the parametric trend of SS 304 flow boiling CHF doesn’t match with that of SS 316. Speaking from the theoretical rationales discussed in Part 4.4.2, the parametric trend of SS 304 flow boiling CHF is supposed to be alike with that of SS 316. The possible reason behind this missing piece is that the thermal-physical properties of SS 304
allow the wall thickness characterized by the SS 304 local minimum flow boiling CHF less than that of SS 316 (0.015" in this study). If there was a 0.007"-thick SS 304, the corresponding CHF would be expected higher than that of the 0.01"-thick SS304 tube. Our experimental results on this topic are not unique. In the flow boiling CHF experimental results procured by G. P. Celata et al. (1997), it was shown that the 0.945mm-thick SS 304 tube has a higher flow boiling CHF than those of the 0.63mm/1.195 mm thick SS 304 tubes. These experimental results imply that thermal-physical properties could exert potential impacts on the parametric trend of CHF with respect to the cladding wall thickness. It should be noted that the thinner cladding tubes would be expected to have lower CHF because of the thermal penetration mechanisms, moreover the thin cladding wall could not tolerate the rapid change of interfacial surface temperature, the induced thermal stress would possibly damage the thin wall of cladding materials.

It is also illustrated in Fig.5-22 that the flow boiling CHF of SS 304 is higher than that of SS 316. This is also in agreement with the pool boiling CHF experimental results of this dissertation. And mechanistic analyses on flow boiling CHF difference are made in the following section.

5.5.2 Thermal-Physical Properties Effects and Their Mechanisms

As observed in Fig.5-23, the flow boiling CHF varies exceptionally divergent in the "within-family" and "between-family" cladding materials. It is shown in Fig.5-23 that FeCrAl-C26M has the highest flow boiling CHF among the investigated scope of various claddings. And even the flow boiling CHF of FeCrAl-C26M is roughly 1 times higher than that of FeCrAl-B126Y. This could be attributed to thermal-physical properties differences between alloys. FeCrAl-C26M/C36M have new additives of Mo/Si in their chemical compositions and show better thermal-physical advantages. Also it is noteworthy that the flow boiling CHFs of FeCrAl-B126Y and FeCrAl-B136Y are less than those of stainless steels, nickel alloys and zircalloys. This experimental
observation confutes that FeCrAl alloys usually render higher safety margins of flow boiling CHF than the traditional claddings. More importantly, the flow boiling CHF difference gap of three FeCrAl alloys is much larger than that of four stainless steels and of three zircalloys while the nickel-based alloys have the least difference gap of flow boiling CHF than other three alloy families.

Regarding to plausible rationales behind the CHF difference between cladding materials, there are two mechanistic understandings to this interesting topic. The one is that the hydrodynamics factors including surface wettability, capillary and spreadability forces and surface roughnesses attribute to such difference (Yeom et al., 2020; Godinez et al., 2021) while other experimental results (Raghupathi & Kandlikar, 2017; Tachibana et al., 1967; Golobič & Bergles, 1997; S. K. Lee, Liu, et al., 2020; S. K. Lee, Lee, et al., 2020; S. K. Lee et al., 2019) including this dissertation viewpoint reported that materials’ thermal-physical properties are primary physics contribution to CHF difference. This is because that surface hydrodynamics-related factors may not be so dominant in the flow boiling as the pool boiling and those hydrodynamics factors can be negligible (S. K. Lee et al., 2019; Hata et al., 2004) owing to the shear stress of flow. Insofar, there are several efforts attempting to correlate the
effect of thermal-physical properties on CHF such as the volumetrical heat capacity \( \sqrt{\rho c_p} \) (Raghupathi & Kandlikar, 2017), thermal activity \( \delta \sqrt{k \rho c_p} \) (Tachibana et al., 1967), thermal economy \( \frac{\sqrt{k \rho c_p}}{\alpha} \) (S. K. Lee et al., 2019) and nucleate boiling surface stability (M. He et al., 2019). Those parameters are obtained by the qualitative analyses and the empirical correlations, which restricts their applicability to a wide variety of cladding materials.

In light of this, the effects of materials’ thermal physical properties on CHF are analytically derived from the heat flux partitioning model. In Eq.4.6, the quenching and evaporative heat fluxes are only dependent of thermal-physical properties. Applying this philosophy to flow boiling CHF, the quenching critical heat flux can be expressed as follows,

\[
q''_{CHF,q} \propto \left( \frac{\sqrt{k \rho c_p}_s (\sqrt{k \rho c_p})_l}{\sqrt{k \rho c_p}_s + (\sqrt{k \rho c_p})_l} \right) \frac{1}{\sqrt{\pi t_w}} \propto \frac{\alpha_s (\sqrt{k \rho c_p})_l}{(\sqrt{k \rho c_p}_s + (\sqrt{k \rho c_p})_l)} \tag{5.18}
\]

and the evaporative critical heat flux can be approximated as follow,

\[
q''_{CHF,e} \propto (\sqrt{k \rho c_p}_s)^{-1.5} \tag{5.19}
\]

, and then the effects of cladding material on flow boiling CHF is correlated by the following semi-analytical equation,

\[
q''_{CHF} = C_1 \frac{\alpha_s (\sqrt{k \rho c_p})_l}{(\sqrt{k \rho c_p}_s + (\sqrt{k \rho c_p})_l)} + C_2 (\sqrt{k \rho c_p}_s)^{-1.5} (q''_{CHF})^{1/3} + C_3 \tag{5.20}
\]

where \( C_1 \) is a suppressing factor of heat convection over heat quenching, \( C_2 \) is a suppressing factor of heat convection over heat evaporation, and \( C_3 \) is the convective heat flux, and these three fitting constants are affected by mass flux and inlet subcooling. It could be anticipated that as the progressive increasing of mass flux and/or inlet subcooling, \( \frac{C_2}{C_3} \) and \( \frac{C_2}{C_5} \) approach to zero because of the dominant heat convection. It should be noted that the thermal physical properties of cladding materials are evaluated at the cladding surface temperature right before surface temperature overshooting. In this study, this important prior parameter to Eq.5.20 is obtained by the
Figure 5-24: Eq.5.20 modelling to flow boiling CHF of various cladding materials (OD =3/8", WT = 0.020") under ΔT_{in,sub} = 10 °C and G = 300 kg/(m²·s)

predicted results of flow boiling CHF against the experimental flow boiling CHF for various cladding materials using Eq.5.20. The maximum prediction error is roughly 11% of Zircaloy-4. This validates the predictability of Eq.5.20 to the effects of cladding materials on CHF. However, the prior knowledge of cladding surface temperature right before CHF occurrence is needed in the proposed model and such this information is usually not accessible when coming to the actual practice in thermal safety assessments. Besides, Eq.5.20 does not incorporate the critical role of cladding wall thickness in the material-dependent CHF. These limitations of Eq.5.20 restrict the applicability of Eq.5.20 to characterizing the effects of thermal-physical properties on CHF.

5.5.3 Material-Sensitivity of CHF Against Flow Boiling Conditions

The experimental results of flow boiling HTCs discussed in Section 5.4 give a clear evidence to that flow boiling HTCs are subjected to influential impacts of thermal-physical properties and wall thickness at the weak heat convection regimes, while the
material-sensitivity of flow boiling HTCs is gradually weakened by the progressive increasing of mass flux and/or inlet subcooling. Such effects of mass flux and of inlet subcooling can be similarly applied to the material-sensitive flow boiling CHF. This is phenomenologically supported by the experimental flow boiling CHF results of various cladding materials being reflected in Fig.5-25. As shown in Fig.5-25(a), the progressive increasing of mass flux can drastically reduce the difference gap of flow boiling CHF between various cladding materials. The similar parametric trend of material-sensitive flow boiling CHF can also be observed in Fig.5-25(b) with respect to the continuous incremental in inlet subcooling. This potentially speaks to that although the convective mechanism governed by mass flux to the flow boiling CHF is utterly distinctive from the mechanistical role of inlet subcooling, both mass flux and inlet subcooling share similar suppressing diminishments in the flow boiling CHF sensitivity to cladding materials. These experimental results presented in Fig.5-25 are obviously in a good agreement with the literature review studies (See Fig.3-9). However, there is a slight difference between our experimental results and other experimental reports, that is, in Fig.5-25(a) the difference gap of flow boiling CHF at $G = 300 \text{ kg/(m}^2\text{s})$ is somehow larger than that of $G = 200 \text{ kg/(m}^2\text{s})$ and Fig.5-25(b) also shows the larger difference gap of $\Delta T_{in,sub} = 10^\circ\text{C}$ than the cases of $\Delta T_{in,sub} = 0^\circ\text{C}$ and $5^\circ\text{C}$. This possibly indicates that the material-dominating mechanisms at the $G = 300 \text{ kg/(m}^2\text{s}) \& \Delta T_{in,sub} = 10^\circ\text{C}$ are different from the situations of $G = 200 \text{ kg/(m}^2\text{s}) \& \Delta T_{in,sub} = 10^\circ\text{C}$ and of $G = 300 \text{ kg/(m}^2\text{s}) \& \Delta T_{in,sub} = 5^\circ\text{C}(0^\circ\text{C})$. One plausible theoretical rationale behind this experimental observation is due to the flow boiling patterns being subjected to mass flux, inlet subcooling and heat flux. The Sobierska correlation (Sobierska et al., 2007) is adopted in this study to determine the flow boiling pattern inside the cladding tubes. According to the flow instability, a relative-long time of vapor bulk contacting with cladding wall could result in the surface temperature overshooting and the likelihood sequence of this vapor contact for four flow patterns is Annular > Churn > Slug > Bubbly. However, in the annular flow, the CHF occurrence is more subjected to the impacts of flow boiling instability than the material-dominated mechanism. As shown in Tab.5.4, it could be safety
Figure 5-25: Material sensitivity of flow boiling CHF to mass flux and inlet subcooling: noting that Zircaloy-4, Inconel-600/625, and SS 304/316/321/347 have the 0.375" OD and the 0.020" WT
thought that the annular flow boiling instability accounts for the relatively-smaller difference gap observed in the cases of $G = 200 \text{ kg/(m}^2\text{s)}$ & $\Delta T_{in,sub} = 10^\circ\text{C}$ and of $G = 300 \text{ kg/(m}^2\text{s)}$ & $\Delta T_{in,sub} = 5^\circ\text{C}(0^\circ\text{C})$.

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It is remarkable that FeCrAl-C26M provides higher flow boiling CHF values than rest of cladding materials across a wide range of boiling conditions. This flow boiling CHF enhancement of FeCrAl-C26M means that the FeCrAl-C26M and $\text{U}_3\text{Si}_2$ fuel entity can have more thermal safety margins and power economy. However, this superiority will disappear when LWRs are under the nominal operations.

### 5.6 Effects of Post-CHF Surfaces on CHF-Reoccurrence

The sections above experimentally investigate how cladding thermal-physical properties affect CHF and HTCs. This section experimentally studies how the presence of oxide layer exerts impacts on CHF-reoccurrence.

#### 5.6.1 A Short Literature Review to Post-CHF Impacts

More recent CHF experiments have shown that the near-field properties, including material compositions and morphologies of heat transfer surface (Morshed et al., 2013), also have influential impacts on CHF. However, the physical rationales behind the effects of material-related factors on CHF remain still controversial and ambiguous. In recent years, experimental investigations on the effects of surface oxidization
on boiling heat transfer have gained more and more research attention in thermal management systems because of their implications to safety risk assessments, especially in nuclear reactor cores.

During the fuel cycle of LWRs, cladding materials are exposed to high temperature water/steam mixture and radiation doses. This definitely results in surface characteristics changes of cladding materials such as formation of oxide layers (Cheng et al., 2012) and surface wettability enhancement (Ali et al., 2018). Some preliminary experimental studies have already revealed that the presence of an oxide layer has significant impacts on CHF. For instance, the oxidized zircaloy-4 has about 40% enhancement of pool boiling CHF compared to the non-oxidized surface (C. Y. Lee et al., 2015), while the oxide layer of SA508 steel alloy showed an adverse effect on pool boiling CHF (H. H. Son, Jeong, Seo, & Kim, 2016). Their theoretical rationales behind CHF enhancement and deterioration are completely different. Lee et al. (2015) considered the better surface wettability of oxidized sample as the primary contributor to CHF enhancement. On the contrary, H. H. Son, Jeong, Seo, & Kim (2016) thought that the presence of magnetite layer might deteriorate lateral heat dissipation near CHF and result in adverse impacts on CHF. Besides pool boiling study, Trojer et al. (2018) experimentally proved that the oxide layer of low-carbon steel would make boiling surfaces super-hydrophilic and thus be more capable of wicking water, further rendering 70% enhancement of flow boiling CHF in comparison to SS-316. K. Wang et al. (2020b) also showed that the increase of boiling cycle would result in higher flow boiling CHF of carbon steel for the oxidation effect on the cooling ability of reactor pressure vessel downward-faced wall. Those studies imply that the evolutions of reactor core materials may contribute to variations of safety margin at the different stages during the LWR fuel cycle. Although the thickness of oxide layer varies from several microns to several dozens of microns, their significant impacts on boiling heat transfer are evidently obvious. This cannot be sufficiently resolved by the present mechanistic views, including the hydrodynamics and nucleate site theories. Thus, the experimental investigations on such effects seem the only feasible solution to analyzing the potential thermal risks resulting from surface oxidization. Yeom et
al. (2020) measured the static contact angles of four different tested materials with three surface finish conditions, and their results showed that the tested samples of SiC and Zircaloy-4 had greater contact angles after pool boiling CHF oxidization while the contact angles of oxidized FeCrAl and Cr-coated samples decrease in comparison with their as-prepared counterparts. Umretiya et al. (2020) also demonstrated that Cr-coated Zircaloy-4 samples that have been exposed in water/air show the increasing behavior of contact angle as the increasing of the exposure time. Those experimental results imply that the oxidized samples can not 100% guarantee the decreasing of the contact angle or enhance the surface wettability. Not only there are controversial points on the surface wettability change on oxidized surfaces, but also the physical mechanisms behind the enhancement/deterioration of oxide layer on pool boiling CHF still remain ambiguous and critically-debated in the boiling community. Because experimental results and proposed rationales reported in various studies conflict with each other. For example, J. Lee & Chang (2012); Mei et al. (2018) reported that the presence of oxide layer contributed to the pool boiling CHF of SA 508, and the enhancement effect was progressively augmented as the increase of the oxidization time. However, H. H. Son, Jeong, Seo, & Kim (2016) found that the pool boiling CHF of SA 508 was deteriorated due to the surface oxidization of SA 508, and the deterioration effect was not enhanced over the increasing of oxidization time. In light of the results, the mechanistic rationales behind the enhancement of oxide layer on flow boiling CHF of SS 316 are not evidently clear because it is difficult to separate multiple physical mechanisms that act on the triggering of CHF occurrence, such as the variations of material thermal-physical properties, surface roughness, capillary wickability, surface wettabiliy, and porous microstructures.

5.6.2 Experimental CHF-Reoccurrence Results of Post-CHF Surfaces

In this study, the temperature difference between the surface temperature at the outlet location and the liquid temperature at the inlet is adopted to present the
boiling curves under different flow boiling conditions. Two technical terms are clarified herein to help elaborate experimental results more easily, the fresh tubes are the as-received tubes that are only blasted by the sandpaper for the surface consistency without undergoing the flow boiling heat transfer tests, and the oxidized tubes are those tested tubes on which CHF occurred before at least once.

It can be noted from Fig. 5-26 that the flow boiling CHF of oxidized SS 316 tube is significantly improved in comparison with that of fresh SS 316 tube under the $G = 300 \text{ kg/} (\text{m}^2 \cdot \text{s})$ and $\Delta T_{in,sub} = 0 \degree C$. In addition, the ONB of the oxidized SS 316 tube could be enhanced in comparison that of the fresh SS 316 tube. These two important points marked by the nucleate boiling stage are greatly promoted, further implying that the thermal economy of nucleate boiling heat transfer can be reinforced. The experimental observations of intensifications of ONB and CHF can be universally found in other flow boiling cases, as shown in Figs. 4, 5, and 6. The contributions by mass flux and inlet subcooling to ONB and CHF can be explained by the far-field mechanistic views of mass, momentum, and energy. However, the physical rationales behind the CHF enhancements on the used tubes that were oxidized by CHF occurrence could be attributed to the formation of an oxide layer on the tube surfaces. This could be

![Flow boiling curve of fresh and oxidized SS 316 tubes at $G = 300 \text{ kg/} (\text{m}^2 \cdot \text{s})$ and $\Delta T_{in,sub} = 0 \degree C$](image)

Figure 5-26: Flow boiling curve of fresh and oxidized SS 316 tubes at $G = 300 \text{ kg/} (\text{m}^2 \cdot \text{s})$ and $\Delta T_{in,sub} = 0 \degree C$
Figure 5-27: Flow boiling curve of fresh and oxidized SS 316 tubes at $G = 2000$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 0^\circ$C

Figure 5-28: Flow boiling curve of fresh and oxidized SS 316 tubes at $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ$C

seen from previous CHF experiments of pool boiling (Kam et al., 2018; C. Y. Lee et al., 2015; H. H. Son, Jeong, Seo, & Kim, 2016) that demonstrated the oxide layer formed on the heater could improve CHF. The oxidized surface superheat of ONB and CHF showing in Fig. 5-29 is smaller than that of fresh tube while this is reversed in the cases as shown in Figs. 5-26, 5-27 and 5-28. However, the various flow boiling conditions may attribute to the mechanistic rationales behind the superheat difference.
Generally, in the experimental results presented in Figs. 5-26 to 5-29, the flow boiling curves of oxidized specimens shift rightward in comparison with those of their non-oxidized counterparts. This implies that the presence of oxide layer deteriorates the boiling heat transfer and results in the higher wall superheat. Although the presence of oxide layer can result in the CHF enhancement, it also brings a deterioration of boiling heat transfer coefficient by adding a thermal resistance to the surface. Similar experimental observations were also reported in the flow boiling CHF experiments of carbon steel (K. Wang et al., 2020a,b). It was reported in several CHF experiments of oxidized samples (Find details in Chapter 3) that although the oxidized surface can enhance flow boiling CHF, the CHF occurrence superheat of oxidized samples is usually higher than that of their non-oxidized counterparts. One postulated explanation is that the presence of oxide layer devastated the heat dissipation ability of the triple-phase surface from the heater solid to the water-vapor mixture because of the degraded thermal conductivity of oxides. This is also in confirmation with our experimental results (Figs. 5-26, 5-27 and 5-28). However, the results presented in Fig. 5-29 contradict with the previous experimental findings. This speaks to that
surfaces that are oxidized by the surface temperature overshooting of CHF occurrence can shift the flow boiling curve upward and leftward, and the related mechanisms are more complicated than ever thought. The results presented in Fig. 5-29 might imply that there are at least two different competing mechanisms resulting from the oxide layer, surface wettability enhancing nucleate boiling heat transfer coefficient while the oxide layer deteriorating it. This interesting phenomenon will be further investigated in our future studies.

The flow boiling experiments, as shown in Fig. 5-30, have the $\Delta T_{in,sub}$ of 0°C and the dryout type CHF occurs on the SS 316 tubes. The results indicate that the dryout gain by increasing mass flux is limited in the fresh SS 316 tubes, while the oxide layer can magnify the dryout gain resulting from higher mass fluxes. For the DNB of flow boiling in Fig. 5-31, the DNB of oxidized tubes is augmented at least 15% compared with that of fresh tubes. Different from the dryout enhancement, the enhancement ratio of DNB by oxide layer is weak under both low and high mass fluxes but is more pronounced at the intermediate mass fluxes. As can be seen in Fig. 5-32, the higher inlet subcooling, the higher the enhancement effect on flow boiling CHF of oxidized tubes. It is noteworthy that in Fig. 5-31, the difference gap of flow boiling CHF between SS 316 fresh and post-CHF samples is firstly exaggerated while

![Figure 5-30: Flow boiling CHF of fresh and oxidized SS 316 tubes with $\Delta T_{in,sub} = 0°C$ across different mass fluxes](image)
Figure 5-31: Flow boiling CHF of fresh and oxidized SS 316 tubes with $\Delta T_{in, sub} = 10^\circ C$ across different mass fluxes

Figure 5-32: Flow boiling CHF of fresh and oxidized SS 316 tubes with $G = 500$ kg/(m$^2$·s) across inlet subcooling

then is reduced over the progressive increasing of mass flux. Based on the discussions made in Chapter 3, the far-field mechanism contributed by mass flux competes with near-field mechanism related with the material-side factors.

However, as shown in Fig.5-33, the flow boiling CHF of oxidized Inconel 600 is smaller than that of fresh Inconel 600 sample. Also, the flow boiling curves of oxidized Inconel 600 shift leftward in comparison with the flow boiling curve of the non-oxidized
Inconel 600. The experimental results of Inconel 600 contradict those of SS 316. This speaks to the possibility that the effects of oxide layers on flow boiling depends on the tested sample substrate materials and the microstructure of the formed oxide layer. It should be noted that the flow boiling CHF of Inconel 600 is higher than that of SS 316 due to the dominant effect of material properties. Because of the low mass flux and high inlet temperature, the effects of material thermal-physical properties on flow boiling CHF could be dominant. Similar experimental investigations on flow boiling CHF difference between various materials can be found in Chapter 3. It is noted that the fresh tube is the as-received sample that is sandblasted by the sand paper, the first oxidized tube is the fresh sample that underwent the surface temperature overshooting of CHF occurrence only once, and the second oxidized tube is the first oxidized sample with CHF-reoccurrence on the post-CHF surface.

![Flow boiling curve of fresh Inconel 600 and its oxidized tubes](image)

Figure 5-33: Flow boiling curve of fresh Inconel 600 and its oxidized tubes at $G = 500 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 0 \degree C$

5.6.3 SEM Analyses of Post-CHF Surface Morphologies

Fig. 5-34 shows the SEM micrographs of both fresh and oxidized tube surfaces. It is expected that the non-heated fresh SS 316 tube shows no obvious characteristics, as shown in Fig. 5-34(a). Fig.5-34(b) presents the morphological evidence of surface
cracks but no clear formation of oxide layer for the flow boiling case of $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 0^\circ$C. However, as progressively increasing the inlet subcooling showing in Figs. 5-34(c) and (d), the layers of surface oxides are formed on the inner surfaces of SS 316 specimens. Comparing the morphological structures of oxide crystals of various heated samples in Figs. 5-34(c) and (d) illustrates that the oxide crystal structure becomes finer by increasing inlet subcooling. This implies that inlet subcooling can affect the formation of oxide crystals and the morphological structures of oxide layer. There are some areas that are not covered by oxide crystals presenting on the inner surfaces of oxidized samples, which is due to the fact that the intense mechanical vibrations chopped off some oxide layers from the surfaces during sample preparation. On the other hand, Fig. 5-35 demonstrates differences in surface morphologies between oxidized specimens under different mass flux but the

Figure 5-34: SS 316 specimen: (a) as-received, (b) $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 0^\circ$C, (c) $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 10^\circ$C, and (d) $G = 500$ kg/(m$^2$·s) and $\Delta T_{in,sub} = 20^\circ$C

surface morphologies between oxidized specimens under different mass flux but the
same subcooling. Fig. 5-35 shows that the morphological structure sizes of surface oxide crystals become finer, and the distribution of oxide clusters is more densely compacted as mass flux increases. This indicates that mass flux can contribute to the oxidized surface morphological patterns formation.

For the steady-state flow boiling, there are two distinct segments in a tested tube, non-oxidized and oxidized, as shown in Fig. 5-36, and flow boiling CHF usually occurs at proximity close to the outlet because of vapor quality. Surface morphologies of four different axial locations (respectively marked from a to d in Fig. 5-36) are also investigated on the same oxidized test specimen of the SS 316 tube. Fig. 5-37 evidently illustrates that surface morphological patterns along the axial direction of the tested specimen could be divided into four distinct genres based on the structures of oxide crystals. At point (a), the oxides are sparsely distributed on the surface,
Figure 5-36: Illustration of the oxidized SS 316 tube under $G = 500 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{\text{in,sub}} = 30^\circ\text{C}$

Figure 5-37: Specimens at four different axial locations in a SS 316 tube oxidized by CHF of $G = 500 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{\text{in,sub}} = 30^\circ\text{C}$: (a) growth, (b) compaction, (c) clustering, and (d) formation

as can be seen in Fig. 5-37(a), where the structural size of oxides is too small to be compacted together to cover the surface implying that oxides are still under the growth stage. As the vapor quality increases axially along the tested tube, surface oxides gradually build up and densely compact together to cover the surfaces, as shown
in Fig.5-37(b). The compacted surface oxides cluster together to form bigger oxide crystalline structures, which can be seen in Fig. 5-37(c). The further progressively-increased vapor quality makes these densely-clustered crystalline structures to form a thin layer of oxide on the surface, as observed in Fig.5-37(d).

Different from SS 316 tubes that are oxidized by the flow boiling CHF, the surface temperature overshooting does not drastically modify the surface morphologies of Inconel 600 surface, or make the oxide layer fractured into micro-scale structured features. Moreover, the only difference incurred by the surface temperature overshooting of CHF occurrence is a different presence of surficial textures, as shown in Fig.5-38. In comparisons with the post-CHF surfaces of SS 316, there are neither oxide-crystal clustered nor oxide-layer fractured micro-structured reflected in the post-CHF surface of Inconel 600 (See Fig.5-38(b)). On the other hand, the temperature overshooting of CHF occurrence unavoidably oxidized the boiling heat transfer of the Inconel 600 tested sample. However, given that Inconel-600 is more corrosion-resistant than SS 316, and the Cr$_2$O$_3$ layer is less susceptible to the thermal-fracturing damage of film boiling than the Fe$_2$O$_3$/Fe$_3$O$_4$ layer. The cross-section SEM scans are performed to compare the oxide layer presences on the post CHF surfaces of SS 316 and Inconel-600 respectively. Fig.5-39 clearly supports the rationale why Inconel 600 has better corrosion resistance to surface temperature overshooting. The intrinsic material thermal-mechanical properties of two oxides might rationalize why the Fe$_2$O$_3$/Fe$_3$O$_4$...
layer is more fragile to thermal-fracturing than the Cr₂O₃ layer. It is noted that

Figure 5-39: Radial cross-section SEM scans on the oxide layers of (a) SS 316 and of (b) Inconel 600 at $G = 500 \text{ kg/}(\text{m}^2\cdot\text{s})$ and $\Delta T_{\text{in,sub}} = 0^\circ\text{C}$

the fresh tube is the as-received sample that never undergoes the flow boiling tests, the first oxidized tube is the fresh sample that underwent the surface temperature overshooting of CHF occurrence only once, and the second oxidized tube is the first oxidized sample with CHF-reoccurrence on the post-CHF surface.

5.6.4 Surface Wettability Analyses of Post-CHF Surfaces

The SEM scan analyses of post-CHF surfaces demonstrate that the introduction of oxide layer, as a result of surface temperature shooting, could possibly explain the CHF enhancement/deterioration on the post-CHF surfaces. However, the presence of oxide layer can exert significant impacts on surface wettability, nucleation site densities and micro-structured morphologies. To rule out the surface wettability change on the post-CHF surfaces, the contact angles of tested samples are procured before and after the boiling experiments. To ensure the measurement quality, some standard criteria are followed and practiced to criteria measure contact angles on tested samples. For example ASTM D7334-08 (2013) was followed in the pool boiling experiments using ATF materials (Ali et al., 2018). However, the measurement methodology of contact angles on curved surfaces is not technically available. Guilizzoni proposed a semi-empirical correlation to correct the effect of convex curvature on contact angles (Guilizzoni, 2011). For a convex surface showing in Fig. 5-40, the curvature effects
are minimized by the minimal allowable sized de-ionized water droplet using a goniometer (S. K. Lee et al., 2019). In this study, the measurement method used by S. K. Lee et al. (2019) is adopted due to its simplicity. As observed in Fig. 5-40, at a point of triple phases, two mutually normal vectors are established as a reference frame, and the static contact angle is the one between the vector that points towards the bottom and the tangent vector that passes along the concave direction of a 5 μL water droplet. Based on Kandlikar’s CHF model (Kandlikar, 2001) (See Eq.3.1), the receding contact angle is the study of interest that explains the effect of surface wettability on CHF. The contact angle Goniometer (rame-hart, See Fig.4-3) can measure both advance and receding contact angles by changing the specimen tilting angles and using the automated tilting base. However, such a measurement method is not applicable to the curved surfaces with upwardly-faced curvature due to the displacement of specimen on a tilting base. The static contact angle is used as an indicator of surface wettability of oxidized samples in this study, and it is measured on the as-received and post-CHF surfaces. The corresponding measurement procedures are in accordance with the standard of American Society of Testing Materials (ASTM D7334-08, 2013). The ambient temperature of contact angle measurement is approximately close to the inlet temperature of water coolant, and the tested specimens are put in a transparent chamber where the ambient temperature is controlled by the contact angle Goniometer. The surrounding gas medium of the transparent chamber is the dry air. To minimize the volume shrinkage of water droplet, five measurements
are performed on each sample within 5 secs respectively.

Figs.5-41(a) and (b) present the static contact angle on different post-CHF surfaces that are oxidized under various flow boiling conditions. The post-CHF oxidized surfaces become progressively hydrophobic and have less surface wettability when increasing the inlet subcooling or mass flux. Without incorporating the effect of thermal-physical properties, the hydrophobic boiling surfaces with less surface wettability can deteriorate CHF. Therefore, this physical rationale could not support CHF enhancement on hydrophobic oxidized surfaces of SS 316. The other plausible cause resulting in CHF enhancement may originate from the micro-structured morphologies formation of oxide layer on surfaces and the change in thermal-physical properties.

On the opposite to flow boiling cases using SS 316 material, CHF deteriorates on the post-CHF surfaces of Inconel 600 that have better surface wettability, as shown in Fig. 5-42. This surface wettability enhancement result on the post-CHF surface of Inconel-600 is in agreement with experimental investigations to the effects of oxidation on contact angle (Hong et al., 1994), that is, surface oxidization does decrease the contact angles of liquid droplet, and enhance surface wettability. However, this contradicts with the increasing of post-CHF SS 316 surface contact angles. The micro-structured morphologies of oxide crystals speak to the possible rationale behind this contradictory point because there are no noticeable oxide micro-structures on the Inconel-600 post-CHF surface, and moreover metal oxides are generally hydrophilic owing to metal cations, oxygen anions, and hydroxyl groups on the surface. One of the theoretical explanations behind the surface wettability devastation by the micro-structured surface is that air pockets that are entrapped by the micro-structured gaps repel away the water droplets and reduce the capillary penetration ability of water molecules (See Fig.20). Although the oxide micro-structured morphologies of SS 316 post-CHF surface are not regularly patterned in a specific manner, it is plausible to explain why the contact angles of SS 316 post-CHF surfaces are higher than that of non-oxidized SS 316.
Figure 5-41: Static contact angles of curved SS 316 specimens under various flow boiling conditions: (a) $G = 500 \text{ kg/}(\text{m}^2 \cdot \text{s})$ (b) $\Delta T_{in,sub} = 10 ^\circ \text{C}$

5.6.5 Mechanistic Analyses on CHF Enhancement

After the post-CHF oxidization, the static contact angle of SS316 tested increases, while the Inconel 600 has a smaller static contact angle. This contradicts with most
Figure 5-42: Static contact angle of Inconel 600 specimen.

Figure 5-43: Contact angles on a flat smooth surfaces and three different micro-structured surfaces (in courtesy of Kashaninejad et al. (2012))

experimental investigations to effects of surface oxidization on CHF (C. Y. Lee et al., 2015; H. H. Son, Jeong, Seo, & Kim, 2016; K. Wang et al., 2020b). Different from those studies, the post-CHF surfaces of SS 316 present oxide micro-structured surface morphologies that are produced by the flow boiling instead of pool boiling. This phenomenologically explains why the wettability change of our Inconel-600 post-CHF surface agrees with other experimental results while our SS 316 flow boiling oxidized samples show opposite results. As pointed by S. K. Lee et al. (2019), the wettability change resulting from surface oxidization has limited influences on flow boiling CHF unless the tested materials are extremely either hydrophobic or hydrophilic. These
experimental investigations imply that the surface wettability variation resulting from oxide layers could contribute to the enhancement/deterioration of pool boiling CHF but have limited impacts on flow boiling CHF. This potentially speaks to that the surface wettability change resulting from oxide layers cannot sufficiently explain flow boiling CHF enhancement of SS 316.

In this study, two perspectives are considered to explain the CHF amelioration/deterioration, thermal-physical properties, and micro-structured surface. In several pool boiling experimental studies, CHF difference gaps between claddings are mechanistically by the concept of thermal effusivity. Because this indicator physically quantifies a material's ability to exchange thermal energy between solids and surrounding environment. As shown in Table 5.5, the thermal effusivities of two iron oxides are higher than that of the SS 316. This might partially explain why flow boiling CHF is enhanced on the post-CHF surfaces of SS 316. However, since the oxide layer on SS 316 is much thinner than the wall thickness, the potential impacts of iron oxides thermal effusivity on SS 316 flow boiling CHF enhancement are limited. The most likely enhancement mechanism behind the SS 316 flow boiling CHF in this study is due to oxide micro-structured surface morphologies. As suggested by B. S. Kim et al. (2014), the interfacial wicking velocity is pronouncedly improved by micro-pillars to better spread liquid over hot patches and enhance pool boiling CHF. Similarly, the oxide micro-structured surface morphologies on SS 316 post-CHF samples are to improve the capillary wicking ability of prompting liquid supply to hot patches and prevent them from the irreversible formation of dry patches.

<table>
<thead>
<tr>
<th>Materials</th>
<th>$\rho$ (kg/m$^3$)</th>
<th>$k$ (W/(m·K))</th>
<th>$c_p$ (J/(kg·K))</th>
<th>$\sqrt[3]{k \rho c_p}$ (J/(m$^2$·K·$\sqrt{s}$))</th>
<th>$k/(\rho c_p)$ (mm$^2$/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inconel-600</td>
<td>8470</td>
<td>15.89</td>
<td>464.97</td>
<td>7910.72</td>
<td>4.03</td>
</tr>
<tr>
<td>SS-316</td>
<td>7921</td>
<td>15.11</td>
<td>508.86</td>
<td>7804.07</td>
<td>3.75</td>
</tr>
<tr>
<td>Chromia Cr$_2$O$_3$</td>
<td>5210</td>
<td>8.23</td>
<td>736.88</td>
<td>5621.04</td>
<td>2.14</td>
</tr>
<tr>
<td>Hematite Fe$_2$O$_3$</td>
<td>5302</td>
<td>5.92</td>
<td>10762.12</td>
<td>18379.31</td>
<td>0.11</td>
</tr>
<tr>
<td>Magnetite Fe$_3$O$_4$</td>
<td>5150</td>
<td>3.72</td>
<td>44626.27</td>
<td>37426.22</td>
<td>0.02</td>
</tr>
</tbody>
</table>
Although the layer thickness of Cr$_2$O$_3$ is roughly several microns, the decreasing of thermal-effusivity owing to the oxide could not be sufficiently explained CHF deterioration of Inconel 600. On the other hand, the non-micro-structured oxide layer can have influential impacts on the site density of active nucleation because the presence of oxide layer significantly deteriorates the thermal diffusivity of cladding surface on which bubbles are generated and boiling occurs. Because thermal diffusivity measures the ability of a material to conduct thermal energy relative to its ability to store thermal energy. During the nucleation and growth of bubbles, the water bulks absorb thermal energy conducted by the boiling surface and vaporize in a vapor mini region. The decreasing of boiling surface thermal diffusivity results in a significant reduction of active nucleation sites even though the oxide layer is roughly several microns thick. As pointed out by K. Wang, Gong, et al. (2018), the decreasing of active nucleate site density leads to the possible deterioration of CHF. Without the micro-structure enhanced capillary wicking effect, the oxide layer could explain why pool boiling CHF is deteriorated in some reported experimental studies of oxidized samples (H. H. Son, Jeong, Seo, & Kim, 2016) and of ceramics-coated samples (Kam et al., 2015). As a result of these discussions, the flow boiling CHF deterioration of Inconel-600 post-CHF could be rationalized by the non-microstructured oxide layer. The surface roughness of tested samples has dominant roles in pool boiling CHF (H. H. Son et al., 2020; Yeom et al., 2020), while Hata et al. (2004) found that no significant influence of surface roughness on flow boiling CHF was observed on the tested materials of SS 304. This might imply that the surface roughness change owing to the Cr$_2$O$_3$ layer could not explain the flow boiling differences between the non-oxidized and post-CHF surfaces, neither surface wettability (H. Seo et al., 2015), nor surface wickability (B. S. Kim et al., 2014).

The corresponding physics understanding is not technically ready. This needs the future development and progress of material-conjugated boiling heat transfer. As pointed in the review study of ATF cladding boiling heat transfer experiments in Chapter 3, speaking from the interfacial physics viewpoints, the phase change of nucleate boiling may involve with solid-liquid/solid-vapor heat conduction/radiation
and heat dissipation and absorption capability of the composite substrates of non-oxides and oxides could result in the deciding impacts on the irreversible formation of dry patches. Therefore, in this study, it is difficult to rationalize whether either the hydrodynamics-related factors, including surface capillary forces, surface wettability, and etc., or the thermal-physical factors, including thermal diffusivity, thermal effusivity, thermal activity, and etc., contribute to the CHF amelioration/ deterioration resulting from the presence of oxide layer.

However, in Fig.5-33, the flow boiling CHFs of oxidized Inconel tests are smaller than that of the fresh Inconel sample, while the flow boiling CHF of the twice-oxidized sample is greater than that of the once-oxidized sample. The similar experimental results were also reported on the flow boiling CHF experiments of Inconel 625 (Suk et al., 2020), in which the effect of oxide layer was briefly studied. It should be noted that although the presence of SS-316 oxide layer has a positive contribution to flow boiling CHF, it deteriorates the flow boiling heat transfer coefficients and further results in the PCT. However, this phenomenon is reversed in the flow boiling of Inconel 600. The presence of the Inconel 600 oxide layer can lead to a lower PCT. The implications of experimental results are that (1) the effects of oxide layer on flow boiling CHF are determined by two dominant factors, claddings’ thermal-physical/mechanical properties and the micro-structured surface morphologies of oxides, and (2) the role of surface wettability is insignificant to the contribution of flow boiling CHF enhancement. These experimental results also speak to that the safety margins, thermal-hydraulic performances, mechanical integrities of fuel claddings may demonstrate distinct behaviors due to reactor operation histories and their footprints on claddings.

Although the mechanical integrity of ATF candidates could improve the safety margins for LWRs, the post-accident core materials can very likely present different material compositions of cladding surface and show beyond-estimation safety margins, especially cladding materials. For example, the cladding surface of FeCrAl is covered by a thin layer of Cr$_2$O$_3$ under the normal operations and by a thick layer of Al$_2$O$_3$ during the accidental scenarios (Rebak, 2018; Rebak et al., 2018; Gupta et
Due to the formation of oxide layers on the cladding surface, the mode of boiling heat transfer is very likely conjugated with material compositions of claddings. Thus, the experimental CHF data that are procured on as-received non-heated materials could not support the safety evaluation of core-loaded claddings. Compared with the thermal power safety margin, the thermal concerns of PCT should be more addressed for the corrosion limit of ATF claddings. This experimental study as well as the recent progresses of ATF cladding boiling experiments (Chapter 3) gives us new thinking on the material-conjugated boiling heat transfer by looking at that how the oxide layer affects boiling heat transfer of cladding materials. Our future look on this topic will be comparative experiments on non-oxidized materials, oxide-coating materials and corresponding oxide-ceramics materials, such as Cr-plates/Cr$_2$O$_3$-coating plates/Cr$_2$O$_3$ ceramics-plates, Al plates/Al$_2$O$_3$-coating plates/Al$_2$O$_3$ ceramics-plates, Si/SiO$_2$-coating/Silica, Zr/Zr-coating/ZrO$_2$ ceramics and etc.

5.7 Implications of Experimental Results

FeCrAl-C26M provides better T-H characteristics including flow boiling HTC and CHF than other investigated cladding materials. However, such those superpriorities of FeCrAl-C26M over traditional cladding materials would be only appreciable under the weak heat convection regimes or the progressional scenarios of DBAs while the nominal operations of ATF-loaded LWRs could be conservatively assessed by the present knowledge of boiling heat transfer. This is because the progressive increasing of mass flux and/or inlet subcooling can reduce the difference gaps of T-H characteristics between cladding materials including HTCs and CHF. Besides the LWR core T-H performances improved by better cladding materials, another optimization strategy is to deploy the cladding materials with appropriate wall thicknesses that permit the best T-H performances including HTCs and CHF.

Since the purpose of ATF claddings is to enhance the intrinsic thermal safeties and increase the nuclear fuel burnup, the cladding surfaces are exposed to the extreme environment for a longer time. Therefore, it is unavoidable to have an oxide layer
presented on the cladding surface. Our experimental results onto the effect of oxide layer on CHF showed that the post-CHF oxidized surfaces usually have higher flow boiling CHF than the as-received non-oxidized tube. The potential implication of this experimental result is that the oxidized fuel cladding gives higher thermal safety margins of flow boiling CHF than the freshly-loaded ATF cores of LWRs.

5.8 Conclusions

The recent progresses of ATF T-H experimental investigations have been showing that the material-side factors including cladding wall thickness, and thermal-physical properties can exert appreciable impacts on flow boiling HTCs and CHF. However, the progressive increasing of mass flux and/or inlet subcooling can gradually close such difference gaps of flow boiling HTCs and CHFs between various cladding materials and different wall thicknesses. This could be mechanistically rationalized by the material-conjugated heat flux partitioning model, that is, the convection mechanism governed by mass flux and inlet subcooling competing with the material-dominated quenching and evaporative mechanisms related with the wall thickness, and thermal-physical properties. It is noted that FeCrAl-C26M claddings render better T-H characteristics in terms of flow boiling HTCs and CHF. This T-H priority of FeCrAl-C26M is only appreciable when the system is at the weak heat convection regime or under the progression scenarios of DBAs. One vital implication of these claddings’ experimental results is that the present knowledge of flow boiling including CHF LUTs can be conservatively applied to support the thermal assessments of the near-term ATF deployments in LWRs based on the nominal operation protocols.

This study also presents how the temperature overshooting led by CHF occurrence on as-received tubes can have influential impacts on CHF re-occurrence on post-CHF surfaces. Experimental results of SS 316 tubes reveal that flow boiling CHF on post-CHF surfaces is significantly enhanced. Also, as increasing the mass flux and/or inlet subcooling, the degree of CHF enhancement can be further advanced. The SEM micrographs of post-CHF SS 316 surfaces could support the idea that formation of
oxide layer contributes to CHF enhancement on post-CHF hydrophobic materials with less surface wettability. These post-CHF oxidized surfaces will become more hydrophobic if either mass flux or inlet subcooling is increased. However, the flow boiling experiments of Inconel 600 shows opposite behaviors to that of SS 316: CHF on post-CHF surface has deteriorated, the post-CHF surface is more hydrophilic than the as-received sample, and a non-microstructure layer of oxide is formed on boiling surface. CHF difference between as-received and post-CHF surfaces could be rationalized by the changes of material thermal-physical properties and of surface morphologies. This implies that the evolutionary changes of cladding surfaces can affect the thermal-hydraulic performances during the reactor lifetime. Therefore, it is imperative to keep track of cladding material surface changes during the entire period of core-loading, especially for FeCrAl cladding that can present various surface morphologies and material compositions.
Chapter 6

Power Transient Boiling of Material
Conjugate Heat Transfer in Past Decades: A Retrospective Look

This chapter covers pool and flow boiling heat transfer experiments under power transient conditions and presents potential implications of experimental data on the thermal safety margins for light water reactors LWRs. Four different power transient modes are discussed including exponential escalating, ramp/quadratic increasing, Nordheim-Fuchs RIAAs, and surface temperature surging. These studies consists of a wide variety of liquids being used as working fluids in various experiments including water, organic liquids (FC-72, ethanol and Freon liquids), and cryogenic liquids (nitrogen and helium). Based on the results of these power transient boiling experiments using various liquids, this study presents how the experimental observations for pool boiling conditions are similar to or different from those obtained from flow boiling conditions. Moreover, the implications of the transient boiling experimental results are applied to the thermal safety evaluations of LWRs.

1This chapter is adopted from one of author’s publication: Mingfu He, Ezekiel Villarreal, Heng Ban, and Minghui Chen, Boiling heat transfer experiments under power transients for LWRs: A review study, Progress in Nuclear Energy, 2021, vol. 141, article ID 103952.
6.1 Prologue

Understanding the physical phenomena of transient boiling heat transfer is essential to simulating the thermal-hydraulic responses of LWR cores to DBAs, especially for RIAs. Under RIAs, a LWR core undergoes a very rapid power transient and can even reach its full design limits in a very short period of time. The occurrence of power transients poses potential threats to nuclear fuel cladding and fuel. Although some RIAs are mitigated by cores designed with intrinsic negative feedback, the occurrence of power transients leads to significant variations in the spatiotemporal temperature distribution. This further results in threats due to thermal stress and even thermal shocks to the structural integrity of fuel-cladding (Desquines et al., 2011) as shown in Figure 6-1.

Figure 6-1: Thermomechanical and thermal-hydraulics safeties of transient boiling heat transfer

Since the advent of LWRs, the occurrence of a boiling crisis on cladding material surfaces has been a source of key concern for thermal safety margin evaluations. With the assistance of high-performance computing, the physics-governed models can be used to study the boiling heat transfer and evaluate thermal safety risks for
LWRs. These models are developed and validated based on the behavior of bubble
dynamics as seen through photographic observation (C.-L. Yu & Mesler, 1977). It is
therefore feasible to provide visual evidences for steady-state models through optical
measurement techniques. However, the transient boiling heat transfer may not be
demystified by optically capturing the bubble dynamic behaviors. This is because
the temporal heat input complicates the bubble interaction behaviors before bubbles
depart from the heating surface. From the standpoint of transient heat conduction,
the boiling heat transfer under power transients is more material conjugated than
that for steady-state cases. Thus, the physical rationales and model assumptions
behind the transient boiling heat transfer. The transient boiling heat transfer (In
this study, it only refers to boiling heat transfer under power transients. Thus, other
transients including pressure transients and flow transients are not covered) cannot be
extrapolated from the readily available steady-state boiling heat transfer. Likewise,
in the transient boiling heat transfer, transient CHF and transient NB-HTC are
parameters of importance to the transient boiling curves. Also, the onset of nucleate
boiling (ONB) is a key parameter to determine the incipience of nucleate boiling
and SP-HTC is an essential indicator of conduction and convection without bubble-
induced enhancement. It is expected that the parametric trends of transient CHF and
transient NB-HTC with respect to T-H conditions (i.e., pressure, mass flux and liquid
subcooling) may share similar mechanistic rationales and analogous mathematical
patterns to the steady-state conditions. Besides T-H conditions and material thermal-
physical properties, the heat input mode of the power transient also has significant,
still with unknown influence, on transient boiling heat transfer.

Experimental studies of transient boiling heat transfer have been an active area
in nuclear T-H community since Cole’s transient experiments (Cole, 1956). Many
experimental results and data of transient boiling heat transfer have accumulated to a
noticeable amount and the popularity of relevant experimental studies involving power
transients is increasing recently to enhance the understanding of transient boiling heat
transfer especially for accurate computer simulation of severe reactor core accidents.
In light of this, the focus of this review is to summarize the primary experimental
studies of transient boiling heat transfer. The goals of this review are listed as follows: 1) to evaluate the results in the literature and discuss the implications of these results and 2) to identify the unfilled gaps based on the discussion of past results reported in literature. The rest of the first part is organized as follows: Section 6.2 briefly describes the experimental methodologies of transient boiling heat transfer, Sections 6.3 compiles and discusses the experimental results of transient boiling heat transfer under both the pool and flow boiling conditions respectively, Section 6.4 presents the discussions and implications of recent power transient boiling heat transfer and finally Section 6.5 makes some concluding remarks and presents some future directions of transient boiling heat transfer experiments.

6.2 Investigations of Experimental Methodologies

6.2.1 Simulated Power Excursion Types of Transient Boiling

The power response of a reactor core to a step reactivity insertion can be approximated asymptotically by the exponentially escalating function expressed as follows:

\[ q(t) = q_0 \exp\left(t/\tau_c\right)u(t) \]

(6.1)

where \( q_0 \) is the starting level of transient power, \( \tau_c \) is the exponential period depending on the effective reactivity of reactor, and \( u(t) \) is the heaviside function. The power transient of Eq. 6.1 is adopted to simulate the effects of a power rise resulting from a prompt change of criticality affecting the boiling heat transfer in lab-scale experiments (J. Park et al., 2006a, 2008, 2009, 2010, 2012, 2015, 2017; G. Su et al., 2015; G.-Y. Su et al., 2016b). The intrinsic T-H characteristics of Eq. 6.1 can be experimentally probed under the pool boiling and flow boiling conditions to understand the bubble dynamic behaviors of transient boiling heat transfer.

Considering that the reactivity insertion is mitigated by the negative feedback mechanisms of the fuel and moderator, such as the Doppler effect, moderator temperature effect, void fraction effect, etc., the Nordheim-Fuchs model of system power
is used to approximate the power pulse of a reactor core as follows:

\[ q(t) = \frac{q_m}{\cosh^2\left(\frac{\rho_b}{2\Lambda}(t - t_m)\right)} u(t) \]  \hspace{1cm} (6.2)

where \( q_m \) is the maximum peak power, \( t_m \) is the time at which the maximum of the transient occurs, \( \Lambda \) is the average neutron generation time, and \( \rho_b \) is the parameter of reactivity insertion change. Since the power increase in Eq. 6.2 is electrically simulated by a direct-current (DC) programmable power supply, some similar bell-like power profiles are used in experiments and numerical models, such as the truncated Gaussian function in RELAP5-3D (Folsom et al., 2016) and the sinusoidal function with the upper half wave in the transient flow boiling study (S. K. Lee et al., 2019).

Since most T-H lab-scale experiments utilize joule heating methods on cladding materials to provide the heat source, the slow operating rate available in present DC programmable power supplies may not be able to generate the fast increasing rates analogous to the power transient present in the profile of Eq. 6.1 or Eq. 6.2 as they approach the maximum peak power. For the sake of power supply protection, the following power transient is used in some studies (Tachibana et al., 1968; G. Celata et al., 1987b; Hata & Masuzaki, 2010b)

\[ q(t) = q_0 \left(\frac{t}{\tau}\right)^n u(t) \]  \hspace{1cm} (6.3)

where \( \tau \) is the total time of power increasing and \( n \) is a constant (in the available studies, \( n = 1 \) or \( 2 \)).

Although the three aforementioned power transients simulated by DC programmable supply are frequently adopted in T-H experiments, they can not reflect some power transients in which the reactivity insertion is not fully mitigated by the intrinsic feedback mechanisms, including the step-wise incremental power transient occurring at the stage of reactor core startup. Such an incremental power transient could be approximated as follows,

\[ q(t) = q_o + q_f (1 - \exp(-t/\tau_e)) u(t) \]  \hspace{1cm} (6.4)
where \( q_f \) is the total step-wise power increment due to the reactivity insertion. Eq. 6.4 can provide a physics-close approximation to the power transient behavior of a reactor core between two steady states. This is applicable to the study of step-wise power increments up to the reactor nominal power.

Besides heat flux time-varying control methods based on heater power transients, an alternative experimental scheme in transient boiling is to control the increasing rates of the heater temperature (Derewnicki, 1985; Deng et al., 2003). In comparison with heat flux control methods, the heater surface temperature variation due to fast transients of power can help prevent the occurrence of CHF by controlling the duration of heating pulses.

### 6.2.2 Experimental Setup and Diagnostics of Transient Boiling

In pool boiling experimental studies of power transients, the direct joule heating elements include thin plates (Cole, 1956; Rosenthal, 1957; Tachibana et al., 1968), small-diameter wires of platinum (Sakurai & Shiotsu, 1977b,a; Derewnicki, 1985; Faw et al., 1986), and large-diameter rodlets of platinum (Fukuda et al., 1995, 2000; J. Park et al., 2006a, 2009, 2010). These direct joule heating elements are connected to the power supply at both end terminals. In addition to direct joule heating schemes, an indirect heating method is also adopted to test some materials with extremely low/high electrical resistances including SiC, copper, aluminum, etc. For the indirect joule heating method, the tested materials are attached to another direct joule heating element that adapts to the capacity range of the power supply. The interface between the tested material and the heating element is filled by a thermally conductive, but electrically insulated, epoxy. Another advanced design is the indium tin oxide heater reported in the study G.-Y. Su et al. (2016b,a). In this case, the temperature variation of tested materials is diagnosed and logged by temperature resistance thermometers for the wire/rodlet heaters. This may lead to a temperature signal interference by the transient input of voltage/current and require the separation of multiple signals in
post-processing. For plate-type heaters, the temporal variation of surface temperature is calculated using thermocouple temperatures measured at specific locations on the heaters. With the advancing of thermal instrumentation, the more advanced surface temperature measurement technique of infrared thermometry was reported in Refs. (G.-Y. Su et al., 2016b,a; Kossolapov et al., 2020).

The flow boiling experimental studies of power transients often employ the direct joule heating scheme to heat up the cladding tubes in different flow configurations. For example, Roemer (1969) performed transient boiling experiments using water for external, vertical, upward flow normal to electrically heated, horizontal stainless steel tubes while Hata et al. (2006) performed transient boiling experiments for the internal, vertically upward flow through 304 stainless steel tubes. Besides directly heating up the cladding tubes by DC power supply, thin metallic plates (Johnson, 1971) and the small-diameter wire heaters (Isao et al., 1983) can also serve as the heating elements to power transients in vertical upward flow boiling experiments. It seems that it is better to perform transient boiling experiments on the external flow boiling configuration using cladding tubes and heating wires than that on the internal flow boiling setup even though the internal configuration is less effort and more economical than the external configuration. This is because the internal flow boiling setup with thermal insulation can not guarantee a perfectly adiabatic boundary condition and it is difficult to quantify the heat loss to the ambient environment while the heat generated by the heater is completely absorbed by the working fluid for the external flow. Aside from the single cladding tube setup, the transient boiling heat transfer experiments of rod bundles were reported in Refs. (Zielke & Wilson, 1974; Iwamura et al., 1994; Takiguchi et al., 2018) for LWR applications including both square (Zielke & Wilson, 1974; Takiguchi et al., 2018) and triangular (Iwamura et al., 1994) arrangements.

Besides thermocouples and temperature resistance thermometers, optical high-speed cameras can be mounted on experimental facilities to capture the bubble dynamics behaviors at the solid-liquid interface (Rosenthal, 1957; Sakurai, 2000; Kossolapov et al., 2020). Based on the visual observations of bubble behaviors, the bubble
departure frequency and diameter under power transients can be approximately by empirical correlations (Akiyama et al., 1968; Derewnicki, 1985). Considering the limited information of optical photographs, more advanced imaging techniques including X-ray tomography (Johnson, 1971) and neutron radiography (Iwamura et al., 1994) were utilized to probe the interactive behaviors among a crowd of bubbles and clarify the transient CHF phenomena. In addition, infrared camera/thermometry can provide the visual observations of surface temperature variations and heat flux distributions on special heaters for demystifying transient boiling heat transfer (G.-Y. Su et al., 2016b,a; Kossolapov et al., 2020). It is worth noting that recent advances in high-end stereo cameras (Mira-Hernández et al., 2019) and increasing progress in machine-vision algorithms (Hobold & da Silva, 2018; Ravichandran & Bucci, 2019) can help boost the phonological and mechanistic understanding of boiling heat transfer. With the help of machine-learning assisted techniques and advanced experimental diagnostics, a deep understanding of transient boiling heat transfer could be achieved in the near future.

6.3 Boiling Heat Transfer Experiments of Power Transients

6.3.1 Survey of Transient Pool Boiling Experiments

Since the early LWR designs from 1950s, the issue of fuel element burnout due to large power surge has become an interest of thermal safety. Performing transient pool boiling experiments is a useful tool to understanding of the effects of a fast power surge on fuel claddings. The effects of liquid subcooling and material thermal-physical properties on transient boiling heat transfer could be studied in a separate manner. A brief review of transient pool boiling experiments using water is tabulated in Table 6.1 for these experimental setups.

Cole (1956) conducted transient boiling experiments using Eq. 6.3 \( (n = 1) \) to investigate the effects of increasing power rate on transient CHF and surface tempera-
Table 6.1: Pool Boiling Experiments of Power Transients Using Deionized Water

<table>
<thead>
<tr>
<th>References</th>
<th>Heaters</th>
<th>Experimental Parameters</th>
<th>Transient Nature</th>
<th>Subcooling</th>
<th>Pressure</th>
<th>CBP</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cole (1956)</td>
<td>a nickel ribbon</td>
<td>76.2 x 0.1 x 5.37mm(^3)</td>
<td>high speed camera: 500 fps, high pressure resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
<td>1 atm</td>
</tr>
<tr>
<td>Rosenthal (1957)</td>
<td>two metallic (Ni + Cu) vertically placed</td>
<td>76.2 x 0.1 x 8.623mm(^3)</td>
<td>high speed camera: 1000 fps, temperature resistance thermometer</td>
<td>Eq.6.1.1 to 5 sec</td>
<td>0°F to 22°F</td>
<td>1 atm</td>
</tr>
<tr>
<td>Teilmann et al. (1966)</td>
<td>a stainless steel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Kwantes et al. (1977)</td>
<td>a platinum wire</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Ayoob, Faghih Khorasani, &amp; Tavakoli (2019)</td>
<td>a nickel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>G. Su et al. (2015); G.-Y. Su et al. (2016b)</td>
<td>a nickel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Walunj &amp; Sathyabhama (2018)</td>
<td>a nickel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>J. Park et al. (2006a, 2009, 2010, 2012, 2015, 2017)</td>
<td>a platinum wire</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Ayoobi, Khorasani, et al. (2019)</td>
<td>a nickel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Sakurai &amp; Shiotsu (1977a,b)</td>
<td>a chrome-aluminum-iron wire</td>
<td>horizontally placed</td>
<td>high speed camera: 1000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>25°C to 75°C</td>
</tr>
<tr>
<td>Sakurai et al. (1970)</td>
<td>a nickel ribbon</td>
<td>horizontally placed</td>
<td>high speed camera: 1000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>25°C to 75°C</td>
</tr>
<tr>
<td>Htet et al. (2015, 2016)</td>
<td>three metallic ribbons</td>
<td>vertically placed</td>
<td>high speed camera: 5000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
</tr>
<tr>
<td>Kawamura et al. (1970)</td>
<td>a platinum rod</td>
<td>horizontally placed</td>
<td>high speed camera: 1000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
</tr>
<tr>
<td>Tachiha et al. (1968)</td>
<td>a platinum rod</td>
<td>horizontally placed</td>
<td>high speed camera: 1000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
</tr>
<tr>
<td>Sargentini et al. (2014)</td>
<td>a nickel ribbon</td>
<td>horizontally placed</td>
<td>high speed camera: 1000 fps</td>
<td>temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
</tr>
<tr>
<td>Sharma et al. (2015)</td>
<td>a stainless steel plate</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
<tr>
<td>Nagayoshi et al. (2014)</td>
<td>a copper plate</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
<td>1 atm</td>
</tr>
<tr>
<td>Hsieh et al. (2015, 2016)</td>
<td>a copper plate</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
<td>1 atm</td>
</tr>
<tr>
<td>G. Su et al. (2017); G.-Y. Su et al. (2016b)</td>
<td>a nickel ribbon</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
<td>1 atm</td>
</tr>
<tr>
<td>Y. Li et al. (2017a)</td>
<td>a copper plate</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1.3</td>
<td>5°C to 75°C</td>
<td>1 atm</td>
</tr>
<tr>
<td>Wolanj &amp; Nettickelbaum (2018)</td>
<td>a copper plate</td>
<td>30 x 0.1 x 0.0015 (0.1)mm(^2)</td>
<td>high speed camera: 500 fps, temperature resistance thermometer</td>
<td>Eq.6.1</td>
<td>extracted boiling</td>
<td>1 atm</td>
</tr>
</tbody>
</table>

References:

- Cole (1956)
- Rosenthal (1957)
- Teilmann et al. (1966)
- Kwantes et al. (1977)
- Ayoob, Faghih Khorasani, & Tavakoli (2019)
- G. Su et al. (2015); G.-Y. Su et al. (2016b)
- Walunj & Sathyabhama (2018)
- Ayoobi, Khorasani, et al. (2019)
- Sakurai & Shiotsu (1977a,b)
- Sakurai et al. (1970)
- Htet et al. (2015, 2016)
- Kawamura et al. (1970)
- Tachiha et al. (1968)
- Sargentini et al. (2014)
- Sharma et al. (2015)
- Nagayoshi et al. (2014)
- Hsieh et al. (2015, 2016)
- G. Su et al. (2017); G.-Y. Su et al. (2016b)
- Y. Li et al. (2017a)
- Wolanj & Nettickelbaum (2018)

CBP: Commercial boiling point.

Notes:

- Yes: CBP achieved.
- No: CBP not achieved.
ture variation. The corresponding results demonstrated that transient CHF increased with the increasing of liquid subcooling and the rate of heat generation. As Figure 6-2 shows, it was concluded from the experimental results that the transient CHF of Eq. 6.3 ($n = 1$) increased as the rising rate of transient power increases, while asymptotically decreased to the CHF level of steady-state boiling with the decreasing rate of transient power. The experimental results obtained from heaters with different thicknesses (Tachibana et al., 1968) indicate that the thickness of the heater did not significantly affect transient CHF under transient power conditions as is the case in steady states. Transient pool boiling CHF experiments (Cole, 1956; Tachibana et al., 1968) illustrate that transient CHF asymptotically decreased to the steady-state CHF over the increasing of $\tau$ under the power transient based on Eq. 6.3 ($n = 1$). This implies that CHF triggering mechanisms of a slow power transient may be utterly different from that of fast power transients, while the physics rationales of slow power transient boiling can be approximated by those proposed mechanisms of steady-state boiling. For the power transient of Eq. 6.3 ($n = 2$), the parametric trend of transient CHF with respect to $\tau$ decreases to a local minimal value, and then increases to the CHF level of steady state, as can be seen in Figure 6-3. It is still reasonable to attribute the power transient mode to the significant difference in parametric trends of transient CHF despite the difference in experimental setups. Similar to the transient CHF of Eq. 6.3 ($n=1$) under the slow power transient, Sharma et al. (2013) confirmed
that the transient CHF of Eq. 6.3 \((n = 2)\) under the transient time of \(\tau\) of 100s is closer to the steady-state CHF than other cases with shorter transient times. Other transient boiling experimental results (Ayoobi, Faghih Khorasani, & Tavakoli, 2019; Ayoobi, Khorasani, et al., 2019) demonstrated that the transient NB-HTC of pool boiling under the power transient of Eq. 6.3 \((n = 2)\) increases over the increasing of \(\tau\), as shown in Figure 6-4.

transient NB-HTC of pool boiling under the power transient using Eq. 6.3 \((n = 1)\) decreases over the increasing of \(\tau\), as shown in Figure 6-5. Moreover, the transient NB-HTC of pool boiling under the power transient of Eq. 6.3 \((n = 1)\) is greater than the steady-state NB-HTC of pool boiling as the experimental results, as shown in Figure 6-5.

Considering that the reactor power surge due to the step reactivity insertion with
little or infinitesimal negative feedback is accurately approximated by the power transient of Eq. 6.1, many experimental investigations were performed to probe and simulate the transient boiling heat transfer under the exponential escalating heat input. In the power transient experiments of Eq. 6.1 (Sakurai et al., 1970), it was found that the transient CHF asymptotically decreased over the increasing of $\tau_e$ See Figure 6-6) and increased over the increasing of liquid subcooling. Rosenthal (1957) identified that the liquid subcooling had impacts on the surface temperature response to power transients and proposed that the rising rate of surface temperature was too rapid for natural convection to contribute to heat transfer. Besides this, Rosenthal (1957) showed that transient boiling behaviors of slower power transients with large subcooling was not appreciably different than that for steady-state boiling. Besides
confirming that transient CHF asymptotically decreases over the increasing of $\tau_e$, Sakurai & Shiotsu (1977a,b) observed that the transient ONB increased with the decreasing of $\tau_e$ and the transient HTC-SP asymptotically decreased over the increasing of $\tau_e$. Their experimental results (Sakurai & Shiotsu, 1977a,b) showed that transient NB-HTC was lower than the steady-state NB-HTC and the NB-HTC ratio of transient to steady-state decreased over the increasing of system pressure. In the transient pool boiling tests based on Eq. 6.1, Sakurai et al. (1970); Sakurai, Shiotsu, Hata, & Fukuda (1993) showed that transient CHF increased over the increasing of liquid subcooling and the liquid subcooling affected the parametric trend of transient CHF with respect to $\tau_e$, because the liquid subcooling has influential impacts on the bubble behaviors including the departure frequency and interfacial dynamics through the competition of heat absorption with heat releasing from the solid body. On the top of experimental results (Sakurai & Shiotsu, 1977a,b; Sakurai et al., 1970; Sakurai, Shiotsu, Hata, & Fukuda, 1993), Fukuda et al. (1995, 2000) investigated the effects of surface roughness and system pressure on transient CHF and found that the transient CHF based on Eq. 6.1 with $\tau_e = 10$ ms was less than the steady-state CHF at the system pressure of 1,082 kPa and the liquid subcooling of 40°C. Assuming that the transient transfer process at the non-boiling regime is influenced by the transient conduction, Htet et al. (2015, 2016) divided the parametric trend of transient CHF with respect to $\tau_e$ into three different stages according to Figure 6-7. Moreover, their exper-

![Figure 6-7: Parametric trend of pool boiling CHF of power transient using Eq. 6.1 with respect to $\tau_e$ (Htet et al., 2015, 2016)](image-url)
imental results (Htet et al., 2015, 2016) showed that increasing liquid subcooling can weaken the dependence of transient CHF on pressure. Based on their experimental data, Y. Li et al. (2017a) thought that the competition of transient heat conduction with the natural convection was dominated by the exponential period $\tau_e$ at the single phase non-boiling regime. Different from the study of Htet et al. (2015, 2016), Y. Li et al. (2017a) categorized the transient CHF in two groups over the increasing of $\tau_e$ under the sub-atmosphere pressure of 50 kPa at the liquid subcooling varying from 0°C to 40°C. Walunj & Sathyabhama (2018) showed that transient CHF of Eq. 6.1 was less than steady-state CHF for all their tested samples and decreased gradually with the decreasing of $\tau_e$. This completely contradicts with experimental results aforementioned under the power transient using Eq. 6.1. In the study of (Sargentini et al., 2014; G. Su et al., 2015; G.-Y. Su et al., 2016b), the transient HTC-SP was shown inversely proportional to $\sqrt{\tau_e}$ in the analytical and experimental manner, which is in accordance with Ref. (Sakurai & Shiotsu, 1977a). The transient ONB experimental results of G. Su et al. (2015); G.-Y. Su et al. (2016b) matched with that of Sakurai & Shiotsu (1977a,b). Kawamura et al. (1970) experimentally investigated the thermal responses of stainless steel and nickel plates to power transients using Eqs. 6.1, 6.2, and 6.3 and showed that bubble dynamics behaviors were presented differently depending on the period-level of power transients. Also it was noteworthy that the parametric trend of transient CHF with respect to the heater thickness increases asymptotically to a saturation value. This is in agreement with the case of steady-state CHF (Arik & Bar-Cohen, 2003).

Besides using water as the working fluid in transient boiling tests, the organic liquids including FC-72 and Freon-113 have been used to reduce the power requirements due to their low boiling points and relatively small thermal-physical properties. Through comparing Freon-113 pool boiling curves under both steady-state and transient tests (Veres & Florschuetz, 1971), it could be found that the distinct characteristic of transient boiling curve is the appearance of transition boiling before the temperature overshooting to the film boiling. Instead of simulating the power transients using Eqs. 6.1, 6.2, and 6.3, Okuyama et al. (1988) performed the pool
boiling transient CHF phenomena of R-113 under the large step-wise power generation across a wide range of system pressures. Their experimental results showed that the transient CHF became lower than the steady-state CHF under lower system pressures while higher than the steady-state CHF under high system pressures (See Figure 6-8) assuming that the super-heat energy deposited in the thermal liquid layer became remarkably high at transient ONB. In the Freon-113 transient pool boiling tests under the power transient using Eq. 6.1, Fukuda & Liu (2005) also proposed the concept of the three-stage transient CHF over the increasing of $\tau_e$. This is in agreement with the transient CHF of water pool boiling (Htet et al., 2015, 2016). Fukuda & Liu (2005) thought that the effects of surface morphology on transient CHF may be the same as the steady-state CHF case. On the basis of experimental results (Fukuda & Liu, 2005), a series of transient pool boiling tests using FC-72 on platinum and gold heaters under the power transient based on Eq. 6.1 (Sutopo et al., 2006, 2008, 2007, 2008, 2010) were performed across a wide range of system pressures and liquid subcoolings to reinforce the understanding of transient heat transfer (See Figure 6-9) and the corresponding experimental results of transient CHF were used to re-elaborate the three-stage transient CHF over the increase of $\tau_e$. It was clear that the parametric trend of transient CHF with respect to $\tau_e$ decreases in the short period, then increases in the immediate period and finally remains almost constant in long periods. The direct transition process to film boiling without nucleate boiling

![Figure 6-8: Power transient CHF in comparison with the steady-state CHF under various pressure levels (Okuyama et al., 1988)](image-url)
was observed under the faster power transients with short periods, which was attributed to the explosive-like heterogeneous spontaneous nucleation on heater surface (Sutopo et al., 2006, 2008, 2007, 2010). The similar physics phenomena were also observed in the transient pool boiling experiments using ethanol under the power transient based on Eq. 6.1 (J. Park et al., 2008). This may imply that the physics causes behind different exponential periods vary significantly and don’t depend on the fluid.

Besides using the power transients, the temperature transients in which the rising rate of surface temperature was varied in pool boiling tests were also utilized to study the bubble dynamics behaviors and transient CHF. In transient pool boiling tests using FC-72, Hohl & Auracher (2000); Hohl et al. (2001) used a PID controller to adjust

![Figure 6-9: The three-stage transient CHFs over the increase of $\tau_e$ (Sutopo et al., 2008, 2007).](image)

(a) for gold heater  
(b) for platinum heater

**Table 6.2: Pool Boiling Experiments of Power Transients Using Organic Liquids**

<table>
<thead>
<tr>
<th>References</th>
<th>Heaters and Working Fluids</th>
<th>Required Diagnostics</th>
<th>Transient Nature</th>
<th>Subcooling</th>
<th>Pressure</th>
<th>Conclusion</th>
</tr>
</thead>
<tbody>
<tr>
<td>Iida et al. (1993, 1994)</td>
<td>a film heater of chromium and platinum</td>
<td>temperature resistance thermometer</td>
<td>up to $3 \times 10^7 K/s$</td>
<td>$25^\circ C$</td>
<td>1 atm</td>
<td>No</td>
</tr>
<tr>
<td>Hohl &amp; Auracher (2000); Hohl et al. (2001)</td>
<td>a copper disk</td>
<td>thermocouples</td>
<td>heater temperature up to 50 K/s</td>
<td>saturated boiling</td>
<td>1 atm</td>
<td>Yes</td>
</tr>
<tr>
<td>Auracher &amp; Marquardt (2002)</td>
<td>a copper disk D 18.8 mm, H 8 mm FC-72 &amp; isopropanol</td>
<td>thermocouples</td>
<td>up to 50 K/s</td>
<td>$0^\circ C$ to $16^\circ C$</td>
<td>0.101 MPa to 1 MPa</td>
<td>Yes</td>
</tr>
<tr>
<td>Fukuda &amp; Liu (2005)</td>
<td>a platinum rod horizontally placed D 1 mm &amp; L 20 mm Freon-113</td>
<td>high speed camera: 1000 fps temperature resistance thermometer</td>
<td>Eq. 6.1; $\tau_e$: 20 ms to 20 s</td>
<td>$0^\circ C$ to $120^\circ C$</td>
<td>0.101 MPa to 1013 kPa</td>
<td>Yes</td>
</tr>
<tr>
<td>J. Park et al. (2006a,b, 2008, 2009, 2010, 2012, 2015, 2017)</td>
<td>a platinum rod vertically placed D 1 mm &amp; L 30 mm ethanol &amp; FC-72</td>
<td>high speed camera: 1000 fps temperature resistance thermometer</td>
<td>Eq. 6.1; $\tau_e$: 5 ms to 50 s</td>
<td>$0^\circ C$ to $60^\circ C$</td>
<td>0.101 MPa to 3.672 MPa</td>
<td>Yes</td>
</tr>
<tr>
<td>Sutopo et al. (2006, 2007, 2009, 2010)</td>
<td>two rodlets of platinum &amp; gold horizontally placed D 1 mm &amp; L 30 mm FC-72</td>
<td>high speed camera: 1000 fps temperature resistance thermometer</td>
<td>Eq. 6.1; $\tau_e$: 10 ms to 70 s</td>
<td>$0^\circ C$ to $140^\circ C$</td>
<td>0.101 MPa to 3.578 MPa</td>
<td>Yes</td>
</tr>
</tbody>
</table>
the rising rate of surface temperature. Hohl & Auracher (2000); Hohl et al. (2001) was able to construct the complete boiling curves under both the steady-state and transient conditions and studied the thermal performances of transient pool boiling. Their experimental results (Hohl & Auracher, 2000; Hohl et al., 2001) demonstrated that transient CHF under various rising rates of surface temperature was higher than the steady-state CHF and increasing the heating rate could enhance transient boiling CHF. In the pool boiling tests using FC-72, isopropanol and water (Auracher & Marquardt, 2002, 2004), the fine-tuned control system of surface temperature was adopted to explore the thermal performances of pool boiling under both the steady-state and transient conditions for different boiling regimes. Their studies (Auracher & Marquardt, 2002, 2004) addressed the convective effects in the thermal boundary layer responsible for the difference between transient and steady-state boiling heat transfer.

Comparative transient boiling tests between water and organic liquids under the power transients using Eq. 6.1 can help enhance the upstanding of transient boiling heater transfer affected by the thermal-physical properties of liquid. In a series of transient pool boiling tests (J. Park et al., 2006a, 2009, 2010, 2012, 2015, 2017), water, ethanol, and FC-72 were investigated for their transient CHF responses and bubble dynamics behaviors to the power transient based on Eq. 6.1 across a wide range of system pressures and liquid subcoolings in a systematic manner. Their experimental results of the three studied fluids (J. Park et al., 2006a, 2010, 2012) also confirmed the parametric trend of transient CHF with respect to \( \tau_e \) drawn in the those studies (Htet et al., 2015, 2016; Fukuda & Liu, 2005; Sutopo et al., 2006, 2008, 2007, 2008, 2010) and showed that transient CHF could be lower than the steady-state CHF under the same conditions. It was revealed that in the highly wetting liquids including FC-72 and ethanol, the vapor film behaviors rapidly grew and covered the heater surface during the initial boiling under the power transient using power of Eq. 6.1, which, however, could not be observed in the cases using water (J. Park et al., 2012, 2015, 2017).

Besides selecting the aforementioned liquids as the working fluid of transient boil-
ing tests, the cryogenics liquids, including helium and nitrogen, were used to investigate the thermal management performances of ultra-low temperature systems (Giventer & Smith, 1969; Oker & Merte, 1973; Tsukamoto & Uyemura, 1980; Giarratano & Frederick, 1980). The primary advantage of cryogenics liquids in the transient boiling tests is that the liquid-vapor mixture does not oxidize the boiling surface of heaters. Under the power transient using Eq. 6.3, Pavlov & Babitch (1987) proposed the transient CHF prediction model based on the transient pool boiling experiments of liquid helium II. For the pool boiling experiments of helium I (Sakurai et al., 1989, 1996; Shiotsu et al., 1992), it was presented that both transient CHF and transient ONB asymptotically decreased to a saturation value over the increasing of $\tau_e$ and the transient heat flux range of nucleate boiling was affected by the system pressure. In the pool boiling experiments using liquid nitrogen and under the power transient based on Eqs. 6.1 and 6.3, Sakurai et al. (1992, 1996) showed that transient CHF was lower than the steady-state CHF from 20.7 to 2047 kPa, the parametric trend of transient CHF with respect to $\tau_e$ was in agreement with the experimental results (J. Park et al., 2006a, 2010, 2012; Htet et al., 2015, 2016; Fukuda & Liu, 2005; Sutopo et al., 2006, 2007, 2008, 2010) and the transient ONB also asymptotically decreased to a saturation value over the increasing of $\tau_e$. Moreover, the direct transition from the non-boiling regime to the film boiling regime could occur under lower system pressures in the transient pool boiling experiments (Sakurai et al., 1992, 1996). This implies that the homogeneous spontaneous nucleation might take place under the power transient boiling heat transfer. Comparative transient boiling experiments between water and liquid nitrogen were reported in Ref. (Sakurai et al., 2000) for their significant differences in bubble dynamics behaviors and the boiling heat transfer process. Other experimental investigations were performed to study the boiling crisis of liquid nitrogen and helium under the heat pulse transient (Deev et al., 1992, 2004; Drach & Fricke, 1996; Drach et al., 1996; Duluc et al., 2004; Delov et al., 2020).

Considering the significant differences of thermal-physical properties for water, organic liquids, and cryogenics liquids, two transient pool boiling experimental studies
(Sakurai, Shiotsu, & Hata, 1993; Sakurai, 2000) demonstrated that the parametric trend of transient CHF with respect to $\tau_e$ was similar in cases of water, ethanol and liquid nitrogen and the increasing rate of heat input, the system conditions and the liquid type determine whether the direct transition from non-boiling regime to film boiling regime occurs under the transient boiling heat transfer. Different from the steady-state pool boiling CHF, the occurrence mechanisms of transient CHF were rationalized by neither the hydrodynamics instability nor the homogeneous spontaneous nucleation (Sakurai, 2000).

Bubble dynamics behaviors under the power transients are of importance to development of mechanistic models and understanding of related physics phenomena. To probe the physical response of bubble kinetics to the time-wise increasing of heat flux on the boiling surface of heater solid, the high-speed optical cameras are often employed to capture the spatiotemporal behaviors of bubble crowds and track the vapor formation. Akiyama et al. (1968) firstly reported that the bubble growth rate under the power transient using Eq. 6.1. It was postulated that a thinner heater section with the smaller thermal heat capacity could raise its surface temperature more rapidly than a thicker section, resulting in thinner thermal boundary layer and hence a steeper temperature gradient and smaller stored energy. Derewnicki (1985) showed that the population of water bubbles for the transient heat pulse of square wave was much less than that of the steady-state scenario under the same system conditions. To target at the wall superheats and bubble diameters for the transient ONB at the various rising rates of surface temperature in the pool boiling of water, ethyl alcohol and toluene, Iida et al. (1993, 1994) evidenced that a large number of tiny bubbles generated concurrently on the heater surface at higher heating rates for the three tested liquids, which was obviously different from that for the lower heating rates. This could be the direct evidence that explains the mechanism differences of transient CHFs between various heating rates. The bubble behaviors during the direction transition to film boiling were also reported in Refs. (Asai, 1991; J. Li et al., 2008; Fau et al., 2017) for the tested specimens subjected to heat pulse transients.
6.3.2 Survey of Transient Flow Boiling

Given that it is difficult to apply transient pool boiling knowledge for transient flow boiling, the experimental investigations of flow boiling configuration are of essence to explore the thermal hydraulic response of cladding materials to various power transients. Insofar, dozens of flow boiling tests of power transients have been conducted to assist the thermal safety margin evaluation for LWRs under RIA scenarios. The related flow boiling experiments of power transients are tabulated in Table 6.3 for comparative analyses.

Roemer (1969) used horizontal-placed stainless steel tubes to the power transient based on Eq. 6.4 for the experimental investigations to the transient DNB and bubble dynamics behaviors under flow boiling conditions. Roemer (1969) indicated that transient departure-from-nucleate-boiling (DNB) was the same as with the steady-state DNB for the sufficiently-aged specimens, which was not affected by the wall thickness of the cladding tube and the parameters in power using Eq. 6.4 whereas for the non-aged elements transient DNB could be as low as 20 % of the steady-state value. This directly evidenced that transient DNB may behave differently under various heat inputs and the material-aging effect on thermal performances of cladding material could be significant. Based on the flow boiling experimental results of power transient using Eq. 6.1, Aoki et al. (1976) thought that the transient flow boiling characteristics could be transferred and extrapolated from the steady-state flow boiling curve line. This was one of the physical assumptions for the Serizawa (1983) predictive model of the transient DNB. However, such assumption is not valid when the transient NB-HTC is not the same as the steady-state NB-HTC. The importance of their experimental results lie at the increasing of the transient DNB over the decreasing of $\tau_e$. It is noteworthy that the effects of mass flow rates on the transient DNB could be explained from the physical contributions of forced convection on the steady-state DNB and the difference gap of transient DNB between various $\tau_e$ was gradually closed by the increasing of mass flux, as can be seen in Figure 6-10. Isao et al. (1983) pinpointed the similarities and differences between transient & steady-state
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<th>Heaters and Working Fluids</th>
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<th>Mass Flow Rate</th>
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<tr>
<td>Roemer (1969)</td>
<td>stainless steel tubes OD 0.975 inch, WT 0.175 inch</td>
<td>horizontally placed test section</td>
<td>thermocouples</td>
<td>temperature: 3 °F</td>
<td>1 atm</td>
<td>s: 3.13/s</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Johnson (1971)</td>
<td>metal tubes OD 1.00 to 1.67 inch, Deformation 1/4 inch</td>
<td>vertically immersed in water flows on both sides</td>
<td>thermocouples</td>
<td>temperature: 147.50, 102.12 °F</td>
<td>14.7, 500, 1000, 2000 psia</td>
<td>s: 1.14/s</td>
<td>No</td>
<td></td>
</tr>
<tr>
<td>Zielke &amp; Wilson (1974)</td>
<td>stainless tubes OD 0.401 inch, 0.040 inch thick</td>
<td>vertically immersed in water flows on both sides</td>
<td>X-ray densitometer</td>
<td>temperature: 10.45, 21.36 °F</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Araki et al. (1976)</td>
<td>stainless steel tubes OD 0.5 mm, WT 0.1 mm, HL 0.92 mm water</td>
<td>vertically upward flow of water</td>
<td>temperature: 10, 42, 112 °C</td>
<td>temperature: 2.8, 248 °C</td>
<td>unknown</td>
<td>s: 1–4 m/s</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Iwano et al. (1983)</td>
<td>stainless tubes OD 0.75 or 1.72 mm</td>
<td>horizontally placed tube</td>
<td>temperature: 100 °C</td>
<td>temperature: 90 to 100 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Isao et al. (1983)</td>
<td>stainless steel tubes OD 0.5–1.415 mm HL 0.37, 1.14 ± 0.04 mm</td>
<td>vertically upward flow of water</td>
<td>temperature: 0 °C</td>
<td>temperature: 30 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>G. Celata et al. (1987b)</td>
<td>stainless steel tubes OD 0.5–1.415 mm</td>
<td>vertically upward flow of water</td>
<td>temperature: 1850 °C</td>
<td>temperature: 30 s</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
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<td>Iwamura et al. (1993, 1994)</td>
<td>stainless steel tubes OD 0.5–1.415 mm</td>
<td>vertically upward flow of water</td>
<td>temperature: 177 °C</td>
<td>temperature: 53 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Q. Liu &amp; Fukuda (2002a,b)</td>
<td>stainless steel tubes OD 0.5–1.415 mm</td>
<td>vertically upward flow of water</td>
<td>temperature: 110 °C</td>
<td>temperature: 40 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Y.-T. Kang &amp; Christensen (1997)</td>
<td>stainless steel tubes OD 0.5–1.415 mm</td>
<td>horizontally placed tube</td>
<td>temperature: 75 °C</td>
<td>temperature: 4 0.33 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
<tr>
<td>Kossolapov et al. (2020)</td>
<td>stainless steel tubes OD 0.5–1.415 mm</td>
<td>horizontally placed tube</td>
<td>temperature: 100 °C</td>
<td>temperature: 4 0.33 °C</td>
<td>1 atm</td>
<td>0.19, 0.20, 0.20 psi</td>
<td>Yes</td>
<td></td>
</tr>
</tbody>
</table>

†: This experimental series of power transient flow boiling were led by Dr. Koichi Hata at Kyoto University.
‡: Both G.-Y. Su et al. (2016a) and Kossolapov et al. (2020) performed transient flow boiling on the ITO heater at MIT.
‡: These power transient heat transfer experiments of various gases flow were performed at Kobe University for the T-H studies of high-temperature gas-cooled reactor.
flow boiling heat transfer through a platinum wire undergoing the power transient based on Eq. 6.1. Their experimental results (Isao et al., 1983) indicated that due to the slow rate of heat input, the transient flow boiling with $\tau_e \geq 5$ s could be regarded practically as the steady-state flow boiling and the transient T-H performances including the transient DNB and transient NB-HTC didn’t depend upon $\tau_e$. However, decreasing $\tau_e$ significantly increased the transient DNB. These power transient flow boiling CHF experiments (Aoki et al., 1976; Isao et al., 1983) demonstrated that the parametric trends of power transient DNB with respect to mass flux, inlet subcooling and pressure were similar to the steady-state DNB cases and DNB values were higher for a faster mode during the increasing heat flux of power transient. This was also in agreement with the power transient flow boiling experiments using Freon-113 (Y.-T. Kang & Christensen, 1997). 

In a series of transient flow boiling experiments of water (Hata et al., 2006; Hata & Noda, 2008; Hata & Masuzaki, 2009, 2010c,b,a, 2011b,a; Hata et al., 2011, 2012, 2013a,b, 2014; Shibahara et al., 2016, 2017a,b, 2020; Nakamura et al., 2019a,b), the 304 stainless steel tubes were subjected to the simulated power transients of Eq. 6.1 and Eq. 6.3 across a wide range of pressures, mass flow rates and liquid subcoolings to study the effects of power transients on both the transient DNB and NB-HTC, evaluate their dependence upon the system boiling conditions and develop the em-
empirical correlation of transient DNB in relation with the steady-state DNB. Their experimental results of transient flow boiling (Hata et al., 2006) confirmed the parametric trend of transient DNB with respect to \( \tau_e \) and system boiling conditions (mass flow rates and liquid subcoolings) made by Isao et al. (1983). It was noteworthy that the transient DNB of which \( \tau_e \) was greater than 800 ms could be modeled as the steady-state DNB by their proposed empirical correlation of Hata et al. (2006) within the 15% difference. Based on their experimental data, Hata et al. (2006) proposed a dimensionless quantity to bridge the relation of the transient DNB with the steady-state DNB as,

\[
T_e = \frac{\tau_e u}{\lambda_c}
\]  

(6.5)

where \( u \) is the mass flow velocity and \( \lambda_c \) is the capillary length of fluid. In the experimental studies of (Hata & Noda, 2008; Hata & Masuzaki, 2009), the experiment configuration of the tested section was extended to study the effects on the ratio of heated length over the hydraulic equivalent diameter on the transient DNB and the experimental results demonstrated that such effects could be covered by the steady-state DNB correlation and showed no significant impacts on the relation of the transient DNB with the steady-state DNB. Hata & Masuzaki (2010b) compared the transient DNB responses to three different power transients based on Eq. 6.1, Eq. 6.3, and Eq. 6.4. The implication of their experimental results (Hata & Masuzaki, 2010b) were that (1) the parametric trend of the transient DNBs based on Eqs. 6.3 and 6.4 with respect to the rising rate of heat input is similar to that of using Eq. 6.1 and (2) there was no appreciable difference of the transient DNB between the three power transients. To enhance the thermal performances of flow boiling under the power transient using Eq. 6.1, the 304 stainless steel twisted tape with the coating of alumina thermal spraying was inserted through the tested tube to improve the turbulence of flow (Hata & Masuzaki, 2011b,a; Hata et al., 2012, 2013a, 2014). Besides using stainless steel 304, platinum tubes were also employed as the tested material of the power transient flow boiling experiments (Hata & Masuzaki, 2010c; Hata et al., 2011) due to its sensitive resistance thermometry. Other than the vertical upward
configuration of internal flow, Hata et al. (2013b) also explored the transient CHF based on Eq. 6.1 on the horizontal internal flow through the stainless steel 304 tube for the design verification of helical type divertor plate in nuclear fusion facility. On the basis of experimental results (Hata et al., 2006; Hata & Noda, 2008; Hata & Masuzaki, 2009, 2010c,b,a, 2011b,a; Hata et al., 2011, 2012, 2013a,b, 2014), Shibahara et al. (2016, 2017a,b) studied the T-H performances of flow boiling under the power transient based on Eq. 6.1 on the stainless steel 304 mini-tubes of which diameter was less than 3 mm. As expected, the parametric trends the transient DNB with respect to $\tau_e$, mass flow rate and liquid subcooling for the mini-tubes were observed similar to the transient flow boiling cases for conventional tubes. It is noteworthy that the transient DNB of mini-tubes was shown higher than that of conventional tubes. Nakamura et al. (2019a,b); Shibahara et al. (2020) obtained the similar experimental results Shibahara et al. (2016, 2017a,b) on platinum mini tubes.

Recently, an MIT research team (G.-Y. Su et al., 2016a; Kossolapov et al., 2020) generated a planar heat flux using Eq. 6.1 to probe the forced convection effects on transient boiling heat transfer using water with assist of high speed optical camera and infrared camera. According to the comparison of exponential period $\tau_e$ with the turbulent vortex time scale, G.-Y. Su et al. (2016a) characterized the features of transient boiling heat transfer into three different regimes where various mechanisms dominate and showed the transient HTCs asymptotically decreased over the increasing of $\sqrt{\tau_e}$ in a semi-analytical manner. The transient HTCs of flow boiling obtained by G.-Y. Su et al. (2016a) resembled the transient pool boiling HTCs of Htet et al. (2015, 2016) in the parametric trend with respect to $\tau_e$. This may imply that the effects of power transient using Eq. 6.1 on HTCs of flow boiling are similar to that of pool boiling. Kossolapov et al. (2020) systematically clarified the respective role of mass flow rate, liquid subcooling and $\tau_e$ in the transient DNB. Their experimental results (Kossolapov et al., 2020) confirmed that for the long periods of power transient, the transient DNB was independent of the power escalation period and the transient boiling process can be regarded similarly as the steady-state cases. This is in agreement with the transient CHF of pool boiling under the slow power transients.
Moreover, Kossolapov et al. (2020) demonstrated that for high subcooling condition the parametric trend of the transient DNB decreased monotonically to the steady-state CHF with respect to the increasing of $\tau_e$, which are in agreement with (Hata et al., 2006; Hata & Noda, 2008; Hata & Masuzaki, 2009, 2010c,b). However, for low subcooling, transient DNB first decreased, then increased, and finally decreased again asymptotically to the steady-state value as the $\tau_e$ increased. This agrees with the transient CHF experimental studies of pool boiling in Refs. (Htet et al., 2015, 2016; J. Park et al., 2006a, 2008, 2009, 2010, 2012, 2015, 2017). Both G.-Y. Su et al. (2016a) and Kossolapov et al. (2020) thought that the competition of transient heat conduction with the heat convection resulting from mass flow rate and liquid subcooling was the primary rationale behind the observed phenomena. Even for a very short period of time, the transient heat conduction was so strong that the role of flow could be negligible and such cases coincided with the values obtained in stagnant water. Kossolapov et al. (2020) also reported that in the low subcooling flow experiments for fast transients, the transient ONB was coincident with the occurrence of the transient DNB, which in other words, the heat transfer process directly transits from non-boiling regime to film boiling regime without undergoing the nucleate boiling regime. This was not surprising as such heat transfer phenomenon was also observed in the liquid nitrogen pool boiling experiments of power transient based on Eq. 6.1 (Sakurai et al., 1996). However, Kossolapov et al. (2020) mentioned that this behavior can be avoided by increasing the flow rate because the dominate role of transient heat conduction can be mitigated by enhancing the forced convection. The system boiling conditions can significantly affect the T-H responses of heater to fast power transients.

To facilitate the mechanistic understandings to T-H characteristics of reactor cores, power transients flow boiling experiments of rod bundle configurations (Zielke & Wilson, 1974; Sugawara & Shiba, 1987; Iwamura et al., 1994; Takiguchi et al., 2018) were performed across a wide parametric range of system conditions. In the $3 \times 3$ rod bundle experiments of Zielke & Wilson (1974), it was shown that there was no premature CHF occurrence in the ramp increases of heat flux due to the power transients.
and the steady-state whole bundle CHF correlation could conservatively predict the power transient CHF because of the 5% ramp increase of heat flux. This was later confirmed in which the power transient based on Eq. 6.3 \((n = 1)\) of the mockup bundle of Advanced Test Reactor (ATR) (Sugawara & Shiba, 1987) the power transient experimental CHF could be conservatively correlated by the steady-state CHF correlations. The power transient experimental CHF data of 7 hexagonal rod bundle were compared with the predicted data of the traditional steady-state rod bundle correlations using local instantaneous flow conditions calculated by the COBRA-TF subchannel code (Iwamura et al., 1993, 1994). This quasi-steady-state method could predict the power transient CHF within \(~15\%\). These power transient flow boiling experiments of rod bundle configurations (Zielke & Wilson, 1974; Sugawara & Shiba, 1987; Iwamura et al., 1994; Takiguchi et al., 2018) explored the possibilities of the use of present rod bundle steady-state DNB/dryout correlations to conservatively predict the power transient DNB/dryout of LWRs.

For natural circulation or under the loss-of-coolant accident, the mass flux throughout the reactor core is significantly smaller than that of the nominal condition for LWRs. Vadlamudi et al. (2019) increased the power of heater rod from 0.37 to 3.77 kW in 5 s and observed the occurrence of CHF after 160 s from the start of the transient at the mass flux varying from 0 to 3 kg/m²s. Their experiments demonstrated that the oscillatory flow pattern transition from slug to churn was the primary triggering mechanism behind CHF occurrence under low mass flow rate and the present low-pressure and low-flow CHF correlations were not applicable for predicting CHF under the oscillatory flow pattern transition. This limits the conservative prediction of the present rod bundle steady-state CHF correlations to thermal safety margin evaluations of LWR cores under the loss of coolant accident. Using FeCrAl cladding subjected to the RIA power pulse (Eq. 6.2), S. K. Lee et al. (2019) obtained the complete track of cladding surface temperature responded to single-phase convection, two-phase nucleate boiling convection, power transient CHF, film boiling, and minimum heat flux of film boiling. Besides the higher values of the transient CHF and NB-HTC, their experimental data analyses demonstrated that the experimental HTC
of film boiling agreed well with Bishop-Sandberg-Tong correlation (Bessiron, 2007).

Besides using water, organic liquids such as Freon-12 (G. Celata et al., 1987b,a, 1988, 1989, 1991, 1992) and Freon-113 (Y.-T. Kang & Christensen, 1997) were employed as the working fluid of power transient flow experiments to reduce the capacity requirements of DC programmable power supply. The experimental phenomena observed in the power transient flow boiling experiments of organic fluids were similar to those drawn from the power transient experiments using water. Different from the time-wise heat input control strategies, the time-wise temperature transient experiments using HFE7000 were characterized by a certain increasing rate of surface temperature (Visentini et al., 2014; Scheiff et al., 2019, 2021). The surface temperature transient CHF experimental results of Visentini et al. (2014) bespoke that the higher increasing rate of surface temperature resulted in higher CHF values of flow boiling, which agrees with the surface temperature transient pool boiling experimental results using FC-72 (Hohl & Auracher, 2000; Hohl et al., 2001). Concurrently, Visentini et al. (2014) found that the experimental HTC using HFE7000 film boiling (estimated from 3,000 to 8,000 W/m²K) is one or two orders of magnitude larger than the predictions of the steady-state film boiling regimes. However, the experimental film boiling HTC was about 291 W/m²K in the power transient flow boiling experiments using water S. K. Lee et al. (2019). This significant gap of transient film boiling HTC could possibly result from differences in transient modes and liquid/solid material thermal-physical properties.

6.3.3 Power Transient Timescale versus T-H Timescale

Since the surging rate of power transient has appreciable influences on boiling behaviors of fuel cladding, it is of importance to systematically compare the power transient timescale with T-H timescales during transient boiling and discuss its contributions to transient CHF/HTC enhancements. This study proposes the following definition
to characterize power transient timescale $\tau_{ch}$ as,

$$\frac{1}{\tau_{ch}} = \max \left| \frac{q(t)}{q(t)} \right|$$

where $q(t)$ is the first order derivative of $q(t)$ and the metric unit of $\tau_{ch}$ is second. In the LWR applications, two common power transients are respectively described by Eqs. 6.1 and 6.2 and their corresponding $\tau_{ch}$s are $\tau_\varepsilon$ and $\frac{\Lambda}{\rho_b}$, respectively. The $\Lambda$ of LWRs is around 0.08 s because of the presence of delayed neutrons. This speaks that the $\tau_{ch}$ of LWRs is dependent of the reactivity insertion and can vary across a wide range. For example, upon the TREAT facility, Bess et al. (2019) simulated a RIA transient experiment using Eq. 6.2 at the maximum power of 12,030 MW and the full width at half maximum (FWHM) of 0.119s. A NRC report showed that the FWHM of RIAs varies from 1 ms to 1000 ms in the up-to-date RIA empirical database for LWRs and most of RIA cases fall from 1 ms to 100 ms. This means that the $\tau_{ch}$ of RIAs can range from 0.567 ms to 56.721 ms in LWRs (for the power transient using Eq. 6.2, FWHM = 1.763$\tau_{ch}$).

This study employs the period of bubble ebullition cycle $\tau_d$ to characterize the T-H timescale of boiling heat transfer because $\tau_d$ physically represents a complete ebullition cycle from growth to departure and rationalizes the time consumption of phase change heat absorption, and the bubble departure frequency $1/\tau_d$ physically means the bubble generation rate. It was confirmed in both pool and flow boiling experiments carried out by Goel et al. (2017, 2018) that the bubble departure frequency increases over the increasing of heat flux. In the subcooled pool boiling experiments (Goel et al., 2017), the experimental results evidently supported that increasing the liquid subcooling can enhance the bubble departure frequency. However in the forced-convection subcooled boiling experiments (Euh et al., 2010; Goel et al., 2018), it was observed that the $1/\tau_d$ of subcooled flow decreases over the increasing of inlet subcooling. This is in contradiction with the subcooled pool boiling results (Goel et al., 2017). However, there has been no experimental clarifications behind this point yet. In addition, the forced-convection subcooled boiling experimental results (Euh et al., 2010; Goel et
al., 2018) showed that increasing mass flux could result in a lower $1/\tau_d$ of subcooled flow. This is clarified by the experimental results (Ghazanfari Holagh et al., 2021) for that increasing mass flux can prolong the waiting time more significantly than reduce the growth time in a complete bubble ebullition cycle, thus leading to the decreasing of subcooled flow $1/\tau_d$. In light of this, $1/\tau_d$ could characterize the T-H timescale of pool boiling while it might not be applicable to that of flow boiling. In the power transient flow boiling, G.-Y. Su et al. (2016a) proposed two different T-H timescales to compare with $\tau_e$, that is, the timescale of advection mechanism $\tau_{adv}$ defined by,

$$\tau_{adv} = \frac{2L_h}{5u^*}$$

(6.7)

where $L_h$ is the wall thickness of heater and $u^*$ is the shear stress velocity that is estimated through the McAdams correlation, and the timescale of turbulent vortex $\tau_{vor}$ given by,

$$\tau_{vor} = \frac{D_h}{2u_b}$$

(6.8)

where $D_h$ is the hydraulics equivalent diameter and $u_b$ is the bulk velocity of fluid. In the meanwhile, G.-Y. Su et al. (2016a) addressed $\tau_{vor}$ was more important than $\tau_{adv}$ because $\tau_{vor}$ is one order of magnitude smaller than $\tau_{adv}$ and a smaller T-H timescale results in a higher heat transfer coefficient. Based on their experimental results on the parametric trend of transient HTC with respect to $\tau_{ch}/\tau_{vor}$, G.-Y. Su et al. (2016a) thought that transient conduction between solid and liquid is the dominant heat transfer mechanism when $\tau_{ch}/\tau_{vor}$ is smaller than 0.1, whereas forced convection of heat transfer dominates when $\tau_{ch}/\tau_{vor}$ is larger than 10. When $0.1 < \tau_{ch}/\tau_{vor} < 10$, the effect of forced convection is comparable with that of transient conduction.
6.4 Analyses and Implications of Experimental Results

6.4.1 Discussions of Power Transient Boiling Heat Transfer Experiments

Tables 6.1 and 6.2 tabulate the pool boiling heat transfer experiments of power transients for comparative discussions between various experimental results of power transient pool boiling heat transfer. The experimental phenomena and results observed in the power transient pool boiling experiments are beneficial to understand the behaviors of transient CHFs and HTCs subjected to (1) transient modes, (2) system pressures and liquid subcoolings, (3) surface morphological characteristics, and (4) solid/liquid thermal-physical properties.

- Transient modes: The effects of transient modes on pool boiling heat transfer rationalized from the heterogeneous spontaneous nucleation mechanisms proposed by Sakurai (2000) could not sufficiently resolve the contradictory experimental observations between the steady-state and power transient experiments. For example, the pool boiling CHF and NB-HTC of power transient using Eq. 6.1 were reported higher than or equal to the pool boiling CHF of the steady-state in the studies of J. Park et al. (2006a, 2009, 2010, 2012, 2015, 2017) and of Htet et al. (2015, 2016) while vice versa in the study of Walunj & Sathyabhama (2018). However, J. Park et al. (2006a, 2009, 2010, 2012, 2015, 2017) and Htet et al. (2015, 2016) demonstrated that the faster transient mode resulted in higher values of transient CHF and NB-HTC while Walunj & Sathyabhama (2018) reported the experimental results opposite to J. Park et al. (2006a, 2009, 2010, 2012, 2015, 2017) and Htet et al. (2015, 2016). For power transient using Eq. 6.3, the transient pool boiling CHF increases over the decreasing of \( \tau \) \((n = 1)\) in the studies of Tachibana et al. (1968) while the transient pool boiling CHF decreases, and then increases over the decreasing of \( \tau \) \((n = 2)\), and
can be either greater or less than the steady-state pool boiling CHF in the study of Ayoobi, Faghih Khorasani, & Tavakoli (2019); Ayoobi, Khorasani, et al. (2019). Moreover, Ayoobi, Faghih Khorasani, & Tavakoli (2019); Ayoobi, Khorasani, et al. (2019) showed that the NB-HTC of Eq. 6.3 \((n = 2)\) decreased over the decreasing of \(\tau\), which was opposite to the NB-HTC experimental results using Eq. 6.3 \((n = 1)\) (Tachibana et al., 1968). These contradictory observations between aforementioned experimental studies should be clarified for the mechanistic modeling to the effects of transient modes on boiling heat transfer.

• Pressure & subcooling: Given that the system pressure affects the dynamics bubble behaviors, thermal-physical properties of working fluid, the effects of pressure on transient pool boiling CHF could be significant because within the pressure range reported in the experimental studies (Sakurai & Shiotsu, 1977b,a; Sakurai et al., 1970; Sakurai, Shiotsu, Hata, & Fukuda, 1993; J. Park et al., 2006a, 2009, 2010, 2012, 2015, 2017), it was shown that the power transient CHF increases as over the increasing of pressure.

It was expected that the power transient CHF was enhanced by increasing the liquid subcooling because of the natural-convection enhancement. More importantly, the power transient CHF experiments of water pool boiling across a wide range of liquid subcoolings (Fukuda et al., 1995, 2000; J. Park et al., 2006a, 2009, 2010, 2012, 2015, 2017) showed the increasing of liquid subcooling could reduce the dependence of power transient CHF on the \(\tau_e\) of Eq. 6.1. Based on their subcooled pool boiling experimental CHF data, Sakurai (2000) derived the following empirical correlation to model the effects of liquid subcooling on the CHF difference gap between the power transient and steady-state cases,

\[
\frac{q''_{cr,sub,tr}}{q''_{cr,sub,st}} = \begin{cases} 
1 + 0.21 \frac{\tau_e}{\tau_0} & 0 ^\circ C \leq \Delta T_{sub} \leq 40 ^\circ C \\
1 + 0.023 \frac{\tau_e}{\tau_0} & 60 ^\circ C \leq \Delta T_{sub} \leq 80 ^\circ C 
\end{cases}
\]

(6.9)

where \(q''_{cr,sub,tr}\) and \(q''_{cr,sub,st}\) are the subcooled pool boiling CHF of the power transient and steady state, respectively, and \(\Delta T_{sub}\) is the temperature subcool-
ing of the working fluid. Eq. 6.9 implies that the effect of transient mode on the power transient CHF is affected by the liquid subcooling condition. This implies that there are two different mechanisms competing with each other.

- Surface morphological characteristics: It was clarified in the studies of (Fukuda et al., 1995, 2000; Sakurai, 2000) that the surface conditions including the roughness scales affect the power transient CHF conditionally due to the explosive-like heterogeneous spontaneous nucleation and hydrodynamic instability mechanisms, i.e., the pool boiling CHF of slow power transients (i.e., long $\tau_e$) is independent of surface conditions due to the hydrodynamic instability while the surface conditions affect the pool boiling CHF for fast power transients (i.e., short $\tau_e$). This was also confirmed with experimental studies of J. Park et al. (2006a, 2008); Htet et al. (2015, 2016).

- Solid thermal-physical properties of heater materials: The power transient pool boiling CHF experiments (Tachibana et al., 1968; Kawamura et al., 1970) showed that the power transient CHF had an asymptotically increasing parametric trend with respect to the increasing of test piece thickness. This speaks to the limited effects of heater thickness on the power transient CHF when the heater is thick in the flow direction of heat flux. From the standpoint of transient thermal liquid/solid conduction, the effects of material thermal-physical properties on power transient CHF is more dominant than that on steady-state CHF. From the steady-state CHF experiments with different heater materials (Raghupathi & Kandlikar, 2017; Tachibana et al., 1967), the present mechanistic CHF models can’t resolve the effects of material thermal-physical properties. In addition, the effects of transient modes, the material-dependent power transient boiling heat transfer would be too complicated to be analytically modeled for the thermal safety evaluations for LWRs under RIAs.

The fluid is stagnant for transient pool boiling experiments, which is different for flow boiling experiments. Table 6.3 tabulates the flow boiling heat transfer experiments of power transients to provide comparative analyses between various ex-
perimental configurations and purposes and to facilitate the understanding of the role of the forced convection in the power transient boiling heat transfer.

- Mass flow rates: The increasing of mass flow rates enhances the forced convection of boiling heat transfer, and hence increases the power transient CHF. The power transient flow boiling CHF experiments between various mass flow rates (Aoki et al., 1976; Isao et al., 1983; Hata et al., 2006) provided the obvious experimental evidences to show that increasing the mass flow rates can reduce the flow boiling DNB difference gap between the power transients and the steady-state cases. Based on their 108 experimental data points using SS 304 material, Hata et al. (2006) proposed the following empirical correlation to model the effect of power transient based on Eq. 6.1 on the power transient DNB and the effect of mass flow rates on the DNB difference gap for the power transients and steady states as,

$$\frac{q''_{dnb,sub, tr}}{q''_{dnb,sub, st}} = 1 + 11.4 \left( \frac{\lambda_c}{u \tau_e} \right)^{0.6}$$  \hspace{1cm} (6.10)

where \( q''_{dnb,sub, tr} \) and \( q''_{dnb,sub, st} \) are the subcooled flow boiling DNB of power transient and of steady state, respectively. \( \lambda_c \) is the capillary length of fluid and \( u \) is the mass flow velocity. For water, \( \lambda_c \) is about 2.7 mm. The application range of Eq. 6.10 is that the outlet pressures are from 801 to 1293 kPa, velocities from 4.0 to 13.3 m/s, and outlet temperatures subcooling from 99.3 to 151 °C. Eq. 6.10 speaks to that under either the fast power transients with short periods or the low mass flow rates, the effects of power transient on the flow boiling DNB can be significant.

- Fast/slow transient: The water flow boiling experiments of power transient Eq. 6.1 (Aoki et al., 1976; Isao et al., 1983) clearly gave an obvious experimental evidence that the faster transients with short \( \tau_e \) resulted in the higher values of power transient and the slow transient with longer \( \tau_e \) showed no appreciable difference from the steady-state CHF behaviors. In other words, the power transient flow boiling CHF increases dramatically from the steady-state flow.
boiling CHF over the progressive decreasing of $\tau_e$. This is also observed in the power transient water flow boiling CHF experiments done by Kossolapov et al. (2020) at the inlet subcooling of 50°C and 70°C with a Reynolds number of 35000. However, at the inlet subcooling of 10°C and 25°C there exists a local maximal value and a local minimal value on the parametric curve of power transient flow boiling CHF with respect to $\tau_e$ varying from 1.5 to 500 ms. This experimental observation of local maxima and minima was also reported in the power transient pool boiling experiments using water, ethanol and FC-72 across a wide range of liquid subcoolings (J. Park et al., 2006a, 2009, 2010, 2012, 2015, 2017; Htet et al., 2015, 2016). In the Freon-113 pool boiling CHF experiments based on Eq. 6.1 (Fukuda & Liu, 2005) and under the saturated boiling condition, the parametric curve of 101.3 kPa power transient CHF was similar to the Kossolapov et al. (2020)’s flow boiling CHF cases of 50°C and 70°C inlet subcoolings and the parametric behaviors of 297 and 494 kPa power transient CHF correspond to the Kossolapov et al. (2020)’s cases of 10°C and 25°C inlet subcoolings (Compare Figure 6-11 with Figure 6-12(a) and Figure 6-12(b)). The potential implication of these aforementioned experimental results is that the system boiling conditions (pressure, mass flow rate and liquid subcooling) have uncleared impacts on how the power transients affect the T-H performances.

6.4.2 Bridging Relations from Steady-state CHF to Transient CHF

The power transient pool/flow boiling CHF experiments across a wide range of mass fluxes, pressures and liquid subcoolings showed that the contributions of system conditions to power transient CHF share the similar mechanistic rationales to the steady-state CHF cases. This enables the power transient boiling heat transfer experiments to target at the experimental investigations on the effects of transient mode and of material thermal-physical properties on CHF. The mechanistic/empirical prediction
of the power transient CHF will be technically viable with the assist of the enlarging data bank of the steady-state pool/flow boiling CHFs. Therefore, the bridging relation from the steady-state CHF to power transient CHF is of importance to the prediction models of the power transient CHF.

Mechanistic models: The mechanistic assumptions were derived from the optical observations made to the bubble dynamics behaviors of power transient boiling heat transfer. Serizawa (1983) assumed that the surface temperature history of power transient followed the steady-state nucleate boiling curve. Serizawa (1983) postulated the occurrence of power transient at the complete consumption of local liquid layer
and calculated the time of complete evaporation by account for the time-dependent nature of heat flux based on the energy balance model during the phase transition from the local liquid layer to the local vapor blanket. In the Serizawa (1983)’s mechanistic model of power transient CHF, two transient modes of Eq. 6.1 and Eq. 6.3 were assessed respectively in the following implicit expressions as,

\[
\tau_e[q''_{cr, tr} - q''_{cr, st}(1 + \ln\left(\frac{q''_{cr, tr}}{q''_{cr, st}}\right))] = \rho_l\Delta H_{fg}\delta_s \tag{6.11a}
\]

\[
\tau\left(\frac{K_s}{q_0}\right)^{1/n}\left[\frac{n}{n+1}\left((q''_{cr, tr})^{n+1} - (q''_{cr, st})^{n+1}\right)\right] - q''_{cr, st}\left((q''_{cr, tr})^{1/n} - (q''_{cr, st})^{1/n}\right) = \rho_l\Delta H_{fg}\delta_s \tag{6.11b}
\]

where Eq. 6.11a and Eq. 6.11b are respectively developed for Eq. 6.1 and Eq. 6.3. \(\rho_l\) and \(\Delta H_{fg}\) are the liquid density and latent heat enthalpy of working fluid, respectively. \(K_s\) is the heat transfer area-to-volume ratio of the heater and \(\delta_s\) is the equivalent liquid layer thickness over the vapor projected area (\(\delta_s\) is estimated by empirical correlations of various boiling conditions). Pasamehmetoglu (1986) defined a correction factor to correlate the effect of power transient on CHF as,

\[
\eta = \frac{q''_{cr, tr}}{q''_{cr, st}} \tag{6.12}
\]

The analytical solution to Eq. 6.12 is of utmost importance to predict \(q''_{cr, tr}\). Pasamehmetoglu (1986) assumed that fast evaporation rate due to increasing heat flux competes the hydrodynamics instabilities of bubbles and the power transient CHF is postulated to occur at the switching point when the fast evaporation dominates over the hydrodynamics instability. Pasamehmetoglu (1986) adopted a more mechanistic description of the macrolayer dynamics to the complete depletion of local liquid layer within the hovering period of bubble. For the power transient of Eq. 6.1, its analytical correction factor was proposed as follows,

\[
\eta = (1 + \frac{\tau_d}{\tau_e})(1 - u(\frac{\tau_d}{\tau_e} - 0.5)) + 1.89(\frac{\tau_d}{\tau_e} - 0.5)^3 u\left(\frac{\tau_d}{\tau_e} - 0.5\right) \tag{6.13}
\]

where \(\tau_d\) is the hovering period of bubble. \(\tau_d\) is estimated by the Haramura and Katto’s
correlation (Haramura & Katto, 1983). For other power transients, Pasamehmetoglu (1986) also derived their corresponding $\eta$ expressions.

Both of the models proposed by Serizawa (1983) and Pasamehmetoglu (1986) need the assist of the empirical correlations, such as the local liquid layer thickness and the hovering period of bubble, to predict power transient CHF under pool/flow boiling conditions. This restricts the applicability of their models within a limited parametric range. It is noteworthy that both prediction models of power transient CHF can’t capture the local maxima and minima points on the parametric curve of power transient CHF with respect to $\tau_e$. This implies that the transient CHF triggering mechanisms suggested by Serizawa (1983) and Pasamehmetoglu (1986) can’t reflect the authentical physics phenomena behind transient boiling heat transfer.

**Empirical Correlations:** With the increasing accumulation of power transient CHF experimental data across a wide parametric range, the various empirical correlations were proposed based on the mathematical regression techniques. Different from mechanistic models, these empirical correlations don’t reply on the phenomenological assumptions and physics mechanisms while need the support of reliable and high-quality experimental data. For example, Both Eq. 6.9 and Eq. 6.10 are derived from the regression analyses to the experimental data to correlate the influences of liquid subcooling and of mass flow rate on the power transient CHF. Same to the mechanistic models, the empirical correlations also need the prior information on the steady-state CHF.

If it is generally held that the power transient CHF is higher than the steady-state CHF under the same boiling conditions of LWR systems, the predictive results of the steady-state CHF can be a conservative evaluation to the thermal safety margin of LWRs under the transient accident progressions. As Figure 6-13 shown, a general map to the accurate prediction of power transient CHF requires the correction factors of various mechanisms resulting from contributions of both power transient mode and cladding material thermal-physical properties. Therefore, the large databank of the steady-state CHF is necessary for the thermal safety evaluation transients for LWRs.
6.4.3 Implications of Power Transient Experimental Results to Thermal Safety Evaluations of LWRs

When LWRs undergo the neutronics transient scenarios, the induced T-H responses are of importance to the thermal safety concerns of LWRs. The experimental observations to CHF and HTCs of power transients are implied to the LWR applications. The direct transition from non-nucleate boiling to film boiling is discussed its potential impacts on LWRs safeties.

*Power transient CHF:* According to the S. H. Chang et al. (1989)’s classification criteria, three distinct mechanisms of transient CHF were proposed to clarify the contributions of liquid subcooling and of mass flow rate to the microlayer depletion during power transient, that is, the hydraulic-instability, thermally-limited and liquid-film-limited regimes of CHF. In the thermally-limited regime, the power transient DNB is greater than or equal to the steady-state DNB. However, in the hydraulic-instability regime, the power transient CHF can be much less than that of steady state. In other words, the types of accident progression scenarios have influential impacts on the evaluation strategies of the LWRs’ upper power limits. For instance, it is conservatively safe to use the present knowledge of the steady-state CHF database to assess the potential impacts of RIAs in PWRs. In the contrary, the LOCA occurrence in BWRs would likely turn the reactor state transition from the liquid-film-limited regime to the hydraulic-instability regime where the effects of power transient and of cladding material properties would be dominant.
**Power transient HTCs:** The water pool boiling SP-HTC of power transient using Eq. 6.1 asymptotically decreased to a constant value over the increase of $\tau_e$ (Htet et al., 2016; Y. Li et al., 2017a). For short and intermediate periods, the transient pool boiling SP-HTC showed a linearly decreasing trend with respect to the log-scale of $\tau_e$, which is rationalized by the transient heat conduction between solid and liquid. However, for a longer period of time, the transient pool boiling SP-HTC remained at constant because of the natural convection. This was also in agreement with power transient pool boiling experiments using ethanol and FC-72 (J. Park et al., 2006b; Sutopo et al., 2006, 2007, 2008, 2010). Later, G. Su et al. (2015); G.-Y. Su et al. (2016b) analytically derived that the transient SP-HTC for water pool boiling was inversely proportional to $\sqrt{\tau_e}$ and validated this using their experimental results. It could be anticipated that the transient NB-HTC of pool boiling would show a similar parametric behavior for the transient SP-HTC.

Hata et al. (2011) presented the asymptotically-decreased behaviors of transient HTCs to a constant value of the steady-state over the progressive increasing of $\tau_e$ and derived the empirical correlation of power transient Nusselt number ($N_{utr}$) to model the effect of transient rates as follows,

$$\frac{N_{utr}}{N_{ust}} = 1 + 9.46 \left( \frac{\lambda_c}{u \tau_e} \right)^{0.8}$$

(6.14)

where $N_{ust}$ is the steady-state Nusselt number under the same conditions. Similar to Eq. 6.10, Eq. 6.14 also indicates that it is possible to approximate to the transient HTCs with long periods and/or high flow velocities using the steady-state HTCs. These experimental observations of transient HTCs were also reported similarly in the power transient flow boiling experiments using FC-72 (Y. Li et al., 2017b).

The forced-convection HTCs of power transient were reported greater than or equal to that of steady states in the aforementioned flow boiling experiments. The implications of power transient boiling experiments are made to the conservative assessments of peak cladding temperature (PCT) limit: (1) when the fuel pins of LWRs undergo the power surge using Eq. 6.1, the use of the steady-state HTC correlations leads
to the overestimation of PCT for the conservative safety margins for nuclear fuel claddings because PCT is inversely proportional to HTCs in a partial way, and (2) the power surging rate of fuel pin primarily dominates the increasing rate of PCT during power transient, which further infers that the design limit of maximum allowable PCT demarcates the lower limit of $\tau_e$. Otherwise, the fuel cladding would be damaged.

6.5 Summaries of Previous Experimental Results

The purpose of this section is to review the experimental progresses of power transient boiling heat transfer and discuss the potential implications of the transient boiling experimental results to the conservative evaluations of thermal safeties during the transient scenarios for LWRs. Based on the discussions and analyses, the following concluding remarks could be made to further advance the mechanistic understanding of transient boiling heat transfer.

1. Generally, the T-H performances of power transients including ONB, HTCs, and CHF are greater than or equal to their counterparts of steady-state under the forced-convection boiling conditions. This gives the conservative safety evaluation for the occurrence of power transient in LWRs based on the available steady-state boiling heat transfer.

2. For the pool-type boiling experiments of power transients, CHF and HTCs of power transient can be either greater or less than that of steady states. This contradicts the experimental observations of power transient flow boiling and it can not be explained by the various proposed mechanisms including the heterogeneous spontaneous nucleation.

3. The increasing of mass flow rates and/or liquid subcooling can reduce the dependence of T-H performances on power transient modes. This implies that there are two different competing mechanisms behind the power transient boiling heat transfer.
4. The system boiling conditions can affect the parametric trend of power transient CHF with respect to $\tau_e$ because the pressure, mass flow rate and liquid subcooling have significant contributions to the dominance of transient heat conduction over transient heat convection.

From the standpoint of heat source driven transient transfer, the slow transients with long periods could be approximated by quasi-state heat transfer. The rapid increasing of heat flux due to the fast transient contributes to the significant heat conduction between solid and fluid. This leads to the T-H dependencies of cladding on power transients. However, the heat convection resulting from natural/forced flow mechanisms competes with the power-transient driven heat conduction. The dominance of heat convection over the heat conduction can be enhanced by increasing the mass flow rates and/or liquid subcooling, which rationalizes that increasing mass flow rates and/or liquid subcooling reduces the T-H dependencies of cladding for power transients. In comparison with the steady-state cases, the transient T-H performances of cladding seem more dominated by the materials’ thermal-physical properties.

It is conservatively safe to utilize present steady-state boiling heat transfer knowledge to assess T-H performances for claddings under power transients in LWRs including CHF, HTCs and PCT limit. To better assess the T-H performances during a power transient, analytical models and the empirical correlations require steady-state CHFs and HTCs to assist the predictive models of transient counterparts via correct bridging relations. To meet the safety needs of LWRs, the accurate and mechanistic modeling of power transient boiling heat transfer requires further systematically experimental and analytical investigations on the following topics.

1. The triggering mechanisms behind power transient CHF. For example, heterogeneous spontaneous nucleation can not sufficiently explain the experimental results of power transient boiling heat transfer such as the parametric trend of CHF with respect to $\tau_e$. This needs more advanced experimental techniques to capture the bubble dynamics behaviors on various tested cladding materials across a wide variety of power transient modes under the saturated pool boiling.
conditions.

2. The dependency reduction of T-H performances on power transients due to the increasing of mass flow and/or liquid subcooling needs systematic clarifications on competing mechanisms. This could facilitate better development of models for bridging relation between the power-transient and steady-state cases since a enlarged databank of steady-state CHF can assist the power-transient CHF prediction.

3. Coupled with the thermal safety margins of power transient, the concerns of structure integrity due to thermal stress and thermal shock are also of importance for the safety of a LWR core.
Chapter 7

Power Transient Subcooled Flow Boiling Experimental Studies of Zircalloys, FeCrAlS, Inconels and Stainless Steels

Although the past decades of power-transient experiments have rendered substantial and vital findings beneficial to the nuclear T-H safeties of LWRs, some important gaps are still not closed to support the near-term ATF deployment in LWRs. The power-transient flow boiling experiments in this chapter are performed to compliment the experimental results of various research papers tabulated in Chapter 6 in terms of some missing gaps. In this chapter, the linear/quadriatic increasing power transients are generated in the SS tubes to characterize their potential impacts on T-H responses including flow boiling patterns, power-transient CHF and HTCs. Besides, the Fuchs-RIA power transients are experimentally studied to investigate their potential impacts on T-H responses of ATF and traditional claddings. The experimental results imply that the effects of transient time-scales, thermal-physical properties and cladding wall thicknesses could be suppressed by the increasing of inlet subcooling and/or mass flux. Moreover, in this chapter, a new concept is proposed to re-define the thermal safety
margin of nuclear fuel pin power, maximum heat flux (MHF) owing to the intrinsic but unique feature of power-transient flow boiling, and the flow boiling MHFs of ATF and traditional claddings are tabulated to support the safety licensing of ATF-loaded LWRs.

7.1 Prologues

The time-variant heat characteristics of thermal systems pose more challenging difficulties to their thermal management strategies and engineering design limits due to severe thermal stress(shock) induced by the fast-varying temperature differences. The typical example is that reactor cores of LWRs undergoing unanticipated reactivity transient may result in power excursion, further leading to RIAs (Aksan, 2019). Understanding the T-H responses of cladding to RIAs is one of primary fuel integrity concerns in the nuclear reactor safety analyses of DBAs because the spatiotemporal distribution of cladding temperature strongly influences the rod mechanical resistance against structural failure (Bessiron, 2007). Given that it is sophisticated for the physic-governing models to interpret and predict the transient heat transfer from cladding to core coolant, power transient experiments are key methodologies to probing their physical phenomena of boiling heat transfer and providing the experimental benchmarking data for the system-level codes of severe accident simulations such as RELAP5-3D and MELCOR.

The transient boiling heat transfer experimental studies under power transients have been an active and important research area in the T-H community of nuclear engineering since the advent of LWR. Several different power transient boiling transfer experiments were conducted to understand the T-H behaviors under RIA conditions including the pool boiling of various power transients from the early stages of Generation I LWR (Rosenthal, 1957; Lurie & Johnson, 1962) to the midst of Generation II LWR (Derewnicki, 1985; Faw et al., 1986) to recent years (Sharma et al., 2013; Htet et al., 2015, 2016), the flow boiling of various power transients. Besides using water as the working liquid, other refrigerants including FC-72 (Sutopo et al., 2008), Freon-
113 (Fukuda & Liu, 2005), and liquid helium/nitrogen (Sakurai et al., 1992) were also used in the power transient boiling heat transfer experiments due to their lower CHF. In those transient boiling heat transfer experiments, the important parameters of concern are transient CHF (or DNB) and NB-HTC. Those experimental studies demonstrated that the parametrical trends of transient CHF (transient NB-HTC) with respect to system conditions (i.e., pressure, mass flux, and liquid subcooling) share similar physical philosophies to the steady-state CHF (NB-HTC) cases.

As many steady-state CHF experimental results accumulate gradually to enlarge the databank of steady-state CHF of different heater materials, it is possible to improve the prediction accuracy of predictive methodologies based on either the mechanistic models or empirical correlations by using experimental data across a wide range of parameters of concern. And the large databank of steady state CHF allows the machine-learning based methodologies (M. He & Lee, 2018; X. Zhao et al., 2020) feasible to the accurate prediction towards the untouched regions. In light of the limited amount of transient CHF experimental data, several predictive models of transient CHF (Serizawa, 1983; Pasamehmetoglu, 1986; Y.-H. Zhao et al., 2002) were developed based on different mechanistic assumptions. These assumptions were not validated by the reliable experimental diagnostics techniques. Different from the steady-state boiling, the transient boiling process may present fast time-variant behaviors, complicating the bubble interactions and making it difficult to grasp a clear picture of physics-governing transient boiling heat transfer. With advancing of thermal-hydraulics instrumentations such as X-ray tomography (Furuya et al., 2019) and infrared camera (Kossolapov et al., 2020; Kossolapov, 2018), the physical phenomena behind transient boiling will be demystified to support the safety analysis of RIAs.

Recent experimental results revealed that heater material thermal-physical properties have significant impacts on steady-state CHF. From the standpoint of material-conjugated boiling heat transfer, it can be expected that transient CHF is more dependent of thermal-physical properties than steady-state CHF is. Yet, there has been few experimental studies on the effects of thermal-physical properties on tran-
sient boiling heat transfer. This work aims at quantifying the dependence of CHF and NB-HTC on cladding material properties as well as on various heat input modes. The rest of this paper is organized as follows, Section 7.2 elaborates the experimental methodologies in detail, Section 7.3 describes how to employ the numerical framework of transient heat conduction model to construct transient flow boiling curves from the experimental data, the experimental results are analyzed and discussed in Section 7.4 and some concluding summaries are made in Section 7.5.

7.2 Experimental Methodologies

7.2.1 Experimental Settings and Tested Cladding Materials

The power-transient flow boiling experiments are performed upon the UNM flow boiling test loop (See Fig. 5-1). A circular tube with a length of 21.5 inches and an outer diameter of 0.375 inches is vertically mounted upon two orifice fittings. The detailed schematic view of test section is shown in Fig.5-2. The entrance length should be greater than 15 inches to ensure the fully developed water flow based on the correlation [48]. The test section is uniformly heated in the direct joule heating manner that is powered by a direct current power supply. As observed in Fig. 5-2, the actual heated length is 2 inches due to the power limit. The actual heated part of test section is wrapped by a thick pad of thermal insulation foam to minimize the heat loss. Two T-type thermocouples and two pressure transducers are installed respectively upon two orifice tube fittings to measure pressures and temperatures for both inlet and outlet. Eight K-type thermocouples are placed at the outer tube surface in pairs to measure the outer surface temperatures axially. To minimize the contact resistance of current at the junction from power terminals to the tested tube, a very high electrically conductive epoxy is spread all over the contacting surfaces. Some brief information of tested samples can be found in Tab.4.1.

Before directly applying the power to the test section, the working fluid is heated up to 94 °C at least for 30 minutes with the intention of degassing dissolved air, then the
temperature of working fluid is adjusted automatically by chiller and preheater down to the specified inlet temperature. For the signifier of the steady-state condition, this experimental study adopts the following criterion such that the temperature difference between two consecutive mean temperatures measured at an axial point is less than 0.5°C, the mean temperature at an axial point is averaged by a time series of 30 measured temperatures points and all eight measured points are supposed to have the temperature difference less than 0.5°C. When the temperature difference between two consecutive time-wise sampled data is greater than 30°C, CHF occurs at this snapshot moment and then the DC power supply unit is automatically shut down immediately.

The contributions to CHF enhancement can be categorized into far-field and near-field mechanisms. In the far-field properties, system condition parameters of system pressure $P$, mass flux $G$ and inlet subcooling $\Delta T_{in,sub}$ are pronounced factors behind CHF triggering. In this study, the responses of transient CHF to mass flux and inlet subcooling are respectively investigated under different system conditions. The detailed parameters of flow boiling condition are listed in Tab. 7.1. Due to the power capacity limitations of DC programmable power supply, flow boiling experiments with high mass flux or low inlet temperature are not doable in our present experiment configuration. The measurement uncertainties of mass flux and of inlet subcooling are respectively 10 kg/(m²·s) and 1°C. The fuel cladding materials and other common commercial alloys are studied for the effects of thermal-physics properties on transient boiling heat transfer zircaloy-4/Zirlo, Inconel 600/625, SS 304/316, and FeCrAl alloys.

<table>
<thead>
<tr>
<th>Table 7.1: System Settings of Transient Flow Boiling Experiments</th>
</tr>
</thead>
<tbody>
<tr>
<td>parameter</td>
</tr>
<tr>
<td>pressure</td>
</tr>
<tr>
<td>mass flux-$G$</td>
</tr>
<tr>
<td>inlet subcooling-$\Delta T_{in,sub}$</td>
</tr>
<tr>
<td>Power-Transient Mode</td>
</tr>
</tbody>
</table>
7.2.2 Heat Input Modes and Experimental Procedures

To approximate the Fuchs-RIA power transient Eq. 6.2, this project study adopts the raised cosine function to generate the bell-curve power profile of RIA for the power protection due to its low slew rate,

\[ p(t) = \frac{p_m}{2s} [1 + \cos(\frac{t}{s} - \frac{s}{s})] \]  

where \( s \) is the power-transient time scale of Fuchs-RIA and \( p_m \) is the maximum peak power. The volumetric heat source generated in the cladding tube is calculated based on the assumption of homogenous distribution of electrical resistance as follows,

\[ q''''(t) = \frac{p(t)}{\pi D \delta_{th} HL} \]  

where \( D \) is the outer diameter, \( \delta_{th} \) is the cladding wall thickness, and HL is the effective heated length. In the RIA analyses, the full width at half maximum (FWHM) is often utilized to quantify the time-scale of Fuchs-RIA power transient, and the FWHM of Eq.7.1 is as follows,

\[ \text{FWHM} = \frac{4s}{3} \]  

To prevent the electronics damage of the DC programmable power supply resulting from that the data acquisition system of surface temperatures cannot catch up with the fast-time variant output power, the step-wise incremental strategy is utilized to increase the maximum allowable peak power from the power supply as follows

\[ V_m(k) = V_m(k - 1) + \Delta V \]  

where \( V_m(k) \) is the maximum voltage corresponding to the maximum peak power at the present transient cycle, \( V_m(k - 1) \) is the maximum voltage at the previous transient cycle, and \( \Delta V \) is the incremental step (in this study, \( \Delta V \) is 0.01 volt). The waiting time between two consecutive occurrences of power transient is 2 minutes so
that the tested section can be completely cooled down and reach a steady state to prepare for the sequent transient.

7.3 Computational Scheme of Power Transient Flow Boiling

7.3.1 Transient Heat Conduction in Tested Material

Heat conduction equation in a cylindrical solid body

\[
\rho(T)C_p(T) \frac{dT}{dt} = \frac{1}{r} \frac{\partial}{\partial r} \left( k(T)r \frac{\partial T}{\partial r} \right) + \frac{\partial}{\partial z} \left( k(T) \frac{\partial T}{\partial z} \right) + q''''(r, z, t) \tag{7.5}
\]

where \( \rho \) is the mass density of tubular material, \( C_p \) is the isobaric specific heat capacity, and \( k \) is the thermal conductivity. In Eq.7.5, \( \rho, C_p \) and \( k \) are dependent of temperature. It is notable that mass density dependence of temperature is significantly weak and can be neglected in numerical computation of heat conduction. the dependence of \( C_p \) and \( k \) on temperature \( T \) can be correlated by empirical relations.

The tubular geometry domains of interest are \( R_{In} \leq r \leq R_{Out} \) and \( 0 \leq z \leq H \). \( R_{In} \) and \( R_{Out} \) are respectively the inner and outer radius of tested tube and \( H \) is the actual heated length of tested tube (\( H = 2 \) inch). And the time range of interest is the time of RIA transient power input, starting from the moment of power excursion and ending to the snapshot of CHF occurrence. Because the power supply is automatically shut down once CHF occurrence is detected by the system program. The heat source is the direct joule heat of DC power supply generating within the tested tube,

\[
q'''(r, z, t) = \frac{q(t)}{\pi(R_{Out}^2 - R_{In}^2)H} \tag{7.6}
\]

where \( q(t) \) is the simulated power excursions described in Subsection 6.2.1. The materials’ thermal-mechanical properties (\( k \) and \( C_p \)) are evaluated their temperature dependencies by the empirical correlations and tabulated data (See Subsection 2.3.2).

At the snapshot of CHF occurrence, the heat balance equation is established to
construct the flow boiling curve by the following equation:

\[
q(t) = q''_{Surf}(t)2\pi R_{In}H + q''_{Loss}(t)2\pi R_{Out}H + 
\int_{R_{In}}^{R_{Out}} 2\pi rdr \int_0^H dz\rho C_p(T)\frac{dT(r,z,t)}{dt}
\tag{7.7}
\]

where \(q''_{Surf}\) is the heat flux transferring to the internal fluid upwardly flowing through the tested tube, \(q''_{Loss}\) is the heat flux losing to ambient environment, the integrand term describes the heat deposition in the tested tube. The heat convection equation of transient flow boiling is defined as follows

\[
q''_{Surf} = h(T_{In,s} - T_{ave})
\tag{7.8}
\]

where \(T_{ave}\) is the average temperature of inlet and outlet temperatures of fluid and \(T_{In,s}\) is the inner surface temperature of location where CHF occurs. To prevent the heat loss at outer surface of tested tube, three different layers of thermal isolation pads are warped around the tested tube. In this study, we measure the temperature distributions axially along the tested tube to provide the radial Cauchy boundary condition for Eq.7.5

\[
T(r = R_{Out}, z, t) = T_{Out}(z, t)
\tag{7.9a}
\]
\[
q''(r = R_{Out}, z, t) = 0
\tag{7.9b}
\]

where \(T_{Out}\) is measured by K-type thermocouple. The axial Dirichlet boundary condition is in need for Eq.7.5, such that

\[
T(r, z = H, t) = T_{up}(r, t)
\tag{7.10a}
\]
\[
T(r, z = 0, t) = T_{down}(r, t)
\tag{7.10b}
\]

where \(T_{up}(r, t)\) and \(T_{down}(r, t)\) are the measured radial temperature distributions respectively at the upper and lower side of tested tube. However, it is difficult to measure the radial temperature distributions at both ends of tested tube. Three pri-
mary assumptions are made based on the experimental setup of tested tube, (1) the negligible volumetric heat source at both sides, (2) the effective adiabatic heat condition and (3) the homogeneous distribution of radial temperature at both sides due to a thin wall thickness. Therefore, the measurement equation of Eq.7.10 is simplified as follows

\[ T_{up}(r, t) = T_{outlet}(t) \] (7.11a)
\[ T_{down}(r, t) = T_{inlet}(t) \] (7.11b)

In order to provide an accurate approximation to the initial condition of Eq.7.5, the tested tube should reach the steady state without transient power applied under the adiabatic condition of outer surface. According to the experimental setup, the spatial temperature distribution of initial condition can be approximated as follows

\[ T(r, z, t = 0) = T_{inlet} \] (7.12)

where \( T_{inlet} \) is the inlet temperature of fluid and it can be adjusted for different flow boiling conditions.

### 7.3.2 Numerical Schemes of Heat Balance Models

To construct the transient flow boiling curve and calculate the corresponding CHF, Eq.7.5 and Eq.7.7 are solved in a numerical framework. This study adopts the finite difference method with identical grid cell to develop the numerical framework for the 2D transient heat conduction of Eq.7.5. The following mesh method is adopted to generate the identical grid-size structure. The axial distance step is fixed at \( \Delta z \) and the selection of \( \Delta z \) determines the number of measurement location points axially along the outer surface of tested tube. However, in reality, the wire size of thermocouple gives the upper limit to the number of measurement locations. The radial distance of each mesh grid varies for the purpose of identical grid size as follows \(^1\).

\(^1\)Eq.7.13 is derived from the volume equality of each mesh grid \( \pi(r_{i+1}^2 - r_i^2) \Delta z = \pi(r_{i+1}^2 - r_i^2) \Delta z \)
A proper selection of the initial radial distance $\Delta r_0$ and the iteration update of $r_{i+1}$ based on Eq.7.13 can allow the rightmost edge of grid structure to equalize the outer radius of tested tube.

$$\Delta r_{i-1}(2r_{i-1} + \Delta r_{i-1}) = \Delta r_i(2r_i + \Delta r_i) \quad (7.13)$$

An alternative version of Eq.7.13 is expressed as follows

$$r_i = \sqrt{(i-1)\frac{R_{\text{Out}}^2 - R_{\text{In}}^2}{N-1} + R_{\text{In}}^2}, i = 1, 2, \cdots, N \quad (7.14)$$

where $N$ is the total number of nodes involved in the computation of interest. According to Eq.7.14, the mesh structure with the identical cell volume can be generated. The selection of time step $\Delta t$ is bound by the sampling frequency limit of data acquisition device. Since the selections of time and distance steps are under given experiment limitations, it is indispensable to develop an unconditionally stable numerical scheme to Eq.7.5 and Eq.7.7. The numerical experiments had shown that the Crank–Nicolson scheme is a unconditionally stable method in solving 2D transient heat conduction problem. This allows the independent of numerical stability of the Crank–Nicolson scheme on the selections of temporal and spatial steps. Let the tuple $(n, i, j)$ be the indexes respectively representing the temporal frame and the radial and axial nodes. $T_{i,j}^n$ is the temperature at the radial node $i$ and the axial node $j$ at the time frame $n$. The Crank–Nicolson scheme adopts the average of the explicit scheme at $(n, i, j)$ and the implicit scheme at $(n + 1, i, j)$ to approximate the Laplacian operator to the temperature field. Based on the proposed meshing strategy, the right hand side of Eq.7.5 is discretized in the following manner. The axial term of Eq.7.5 is approximated by the Crank–Nicolson scheme as follows:

$$\frac{\partial}{\partial z}(k(T) \frac{\partial T}{\partial z}) \approx \frac{k(T_{i,j+1}^n)(T_{i,j+1}^n - T_{i,j}^n) - k(T_{i,j-1/2}^n)(T_{i,j}^n - T_{i,j-1})}{2\Delta z^2} + \frac{k(T_{i,j+1/2}^{n+1})(T_{i,j+1}^{n+1} - T_{i,j}^{n+1}) - k(T_{i,j-1/2}^{n+1})(T_{i,j}^{n+1} - T_{i,j-1})}{2\Delta z^2} \quad (7.15)$$
where $T_{i,j \pm 1/2}$ is the average temperature between two axial edges approximated by $(T_{i,j \pm 1} + T_{i,j})/2$ for both the $n$ and $n+1$ time frames individually. The radial term of Eq.7.5 is approximated by the Crank–Nicolson scheme as follows:

$$\frac{\partial}{\partial r}(k(T)T) \approx k(T_{i+1/2,j} - T_{i-1/2,j}) \Delta r_{i} - k(T_{i-1/2,j}) \Delta r_{i-1} + \frac{r' \Delta r_{i-1} + \Delta r_{i}}{r'} \left( \frac{T_{i,j}^{n} - T_{i,j}^{n-1}}{\Delta r_{i}} \right)$$

(7.16)

where $r'$ is the averaged radial distance of two adjacent nodes ($r' = (r_{i-1} + 2r_{i} + r_{i+1})/4$), $T_{i \pm 1/2,j}$ is the average temperature between two radial edges approximated by $(T_{i \pm 1,j} + T_{i,j})/2$ and $r_{i \pm 1/2}$ is the mean radial distance between two edges of each radial node. It should be noted that the Crank–Nicolson scheme is a second-order accurate method of $\max\{o(\Delta r^2), o(\Delta z^2)\}$. The left hand side of Eq.7.5 is discretized as follows

$$\rho C_p(T) \frac{\partial T}{\partial t} \approx \rho C_p(T_{i,j}^{n+1/2}) \frac{T_{i,j}^{n+1} - T_{i,j}^{n}}{2\Delta t} + \rho C_p(T_{i,j}^{n-1/2}) \frac{T_{i,j}^{n} - T_{i,j}^{n-1}}{2\Delta t}$$

(7.17)

Although the Crank–Nicolson predictor-corrector scheme has a second order accuracy of time and space and an unconditional stability (see the stability analyses in Subsection 9.2), the measurement uncertainties of outer surface temperatures can still propagate through the numerical scheme and result in the uncertainties of transient flow boiling curves constructed by the proposed numerical scheme.

### 7.3.3 Numerical Validation to Computational Framework

Given that Eq.7.5 is a nonlinear partial differential equation, its computational framework should be numerically validated to guarantee successful applications to experimental results processing. In this dissertation, an artificial problem of interest is created to derive an analytical solution and verify the proposed computational framework. In this artificial problem, thermal-physical properties are weakly dependent on temperature so that the heat capacity could be approximated by a constant within a
thin slab of interest, as follows,
\[
\frac{\partial \rho c_p(T)}{\partial r} \approx 0 \quad \frac{\partial \rho c_p(T)}{\partial t} \approx 0
\]  (7.18)
and the volumetric heat source is a constant. In light of this, Eq.7.5 could be rewritten in terms of one-dimensional version as follows,
\[
\frac{\partial T}{\partial t} = \frac{\partial}{\partial r} \left( r \alpha_s(T) \frac{\partial T}{\partial r} \right) + s''
\]
\[
= \frac{\alpha_s(T)}{r} \frac{\partial T}{\partial r} + \frac{\partial T}{\partial r} \frac{\partial \alpha_s(T)}{\partial r} + \alpha_s(T) \frac{\partial^2 T}{\partial r^2} + s''
\]  (7.19)
where \( s'' = q''/(\rho c_p(T)) \). And thermal diffusivity shows a roughly linear decreasing trend with respect to the temperature,
\[
\alpha_s(T) \approx aT + b
\]  (7.20)
where \( a \) and \( b \) are fitting constants. To reduce the analytical modelling complexity, the boundary& initial conditions are specified as follows,
\[
q''(r = R_{out}, t) = 0
\]  (7.21a)
\[
T(r = R_{out}, t) = T_s
\]  (7.21b)
\[
T(r, t = 0) = T_i
\]  (7.21c)
where \( T_s \) and \( T_i \) are constants. Applying the Lie symmetry theory to Eq.7.19 yields the similarity variable \( \eta \) as follows,
\[
\eta = \frac{r}{\sqrt{t}}
\]  (7.22a)
\[
\frac{\partial \eta}{\partial t} = -\frac{\eta}{2t}
\]  (7.22b)
\[
\frac{\partial \eta}{\partial r} = \frac{1}{\sqrt{t}}
\]  (7.22c)
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This leads to a nonlinear ordinary differential equation in terms of $\eta$,

$$\frac{\partial}{\eta \partial \eta} (\eta \alpha_s(T) \frac{\partial T}{\partial \eta}) + \eta \frac{\partial T}{2 \partial \eta} + s''' = 0 \quad (7.23)$$

The solution to Eq.7.23 could be approximated by the following form,

$$T(\eta) = V_0 + pV_1 + p^2V_2 + p^3V_3 + \cdots \quad (7.24)$$

In this study, the approximate solution is truncated at the order of 3 under the assumption of Eq.7.20. However, there is no explicit form of Eq.7.24 but exist unconditionally stable solvers including Runge Kutta methods. In the validation cases, the volumetric heat sources are constant and the outer surface has an adiabatic condition, i.e., the heat flux is zero. Such this experimental setting can make the inner surface temperature reach to the steady state again after a while. This agrees with the computational framework proposed in part 7.3.2 and also approximate analytical solution to Eq.7.23. Both comparative validation results (See Fig.7-1 and Fig.7-2) speak to

![Figure 7-1: Numerical results validated by the approximate analytical solution of Eq.7.24 at the constant heat source of 200 W/m$^3$](image)

that the proposed computational framework can yield reliable numerical output of interest.
Figure 7-2: Numerical results validated by the approximate analytical solution of Eq. 7.24 at the constant heat source of 220 W/m³

7.4 Power Transient Flow Boiling CHF Experimental Results

7.4.1 Power Transient Flow Boiling Experimental CHF Results

As shown in Fig. 7-3, the linear ramp power transient is applied to the cladding material of SS316. When the surface heat flux is beyond the CHF point, the surface temperature overshooting of TS-4 would be expected to occur the cladding surface. The thermocouples are damaged and can not record the surface temperatures accurately in the post-CHF boiling regimes because of CHF occurrence. The surface temperature overshooting of T-S-4 suggests that power transient CHF occurs at the location near to the midpoint of test sample. Fig. 7-4 presents how the surface temperature overshooting takes place resulting from the power transient CHF occurrences. The dataset presented in Fig. 7-4 is input to the computational framework proposed in Section 7.2 to calculate the radial distribution of cladding surface temperature right before and after CHF occurrence. The corresponding output results are pre-
Figure 7-3: Linear ramp power transient and outer cladding surface temperature responses: SS 316 OD 0.375”, WT 0.01”, HL 2.00”, $G = 300 \text{ kg/(m}^2\cdot\text{s})$, and $\Delta T_{\text{in,sub}} = 10^\circ\text{C}$, $P = 84 \text{ kPa}$ Noting that there are eight thermocouples axially distributed along the tested sample from the outlet to the inlet.

Presented in Fig.7-5 and Fig.7-6. As observed in Fig.7-5, the heat flux flows from the cladding wall to the water coolant inwardly because of the thermal insolation layer before CHF while the vapor film layer resulting from the CHF occurrence prevents the inward-flow of heat flux to water coolant and leads to the outward flow of heat flux. The power-transient flow boiling curve in Fig.7-6 is constructed by the proposed computational framework based on the input dataset presented in Fig.7-4. One of obvious differences between the power transient and steady-state flow boiling is that the transition boiling appears in the flow boiling curve (See Fig.7-6). A short moment of transition boiling occurrence could be attributed to the thermal energy deposition in the cladding wall because of the fast surging transient of power. In light of this, power-transient CHF that is characterized by the surface temperature overshooting can not be used in the best-estimate of LWRs thermal margin assessments during the power transient. Because Fig.7-6 clearly shows that CHF is less than the peak surface heat flux of this power transient. Considering this obvious distinction of power transient boiling from the steady-state boiling, a new concept is hereby proposed to only determine the upper allowable limit of the LWRs peak power during the power
Figure 7-4: Input datasets of the computational framework: linear power profile and T-S-4 temperature profile

Figure 7-5: Output datasets of the computational framework: the radial distribution of cladding surface temperature before and after CHF occurrence

transient, maximum heat flux (MHF), as follows,

☒ MHF is the maximum heat flux of a power transient characterizing with the transition boiling appearance and the surface temperature overshooting

☒ MHF is at least greater than or equal to CHF when the claddings are subjected to power-transients

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7.4.2 Linear Power-Transient Flow Boiling Instability

This part demonstrates how the linear ramp power transient induces the flow boiling instability on the transient CHF. When the tested specimens of cladding material are subjected to the power transient of linear ramp (See Fig. 7-7(a)), the peak heat flux

![Diagram of transient flow boiling curve](image)

Figure 7-6: Transient flow boiling curve constructed by the computational framework

The peak heat flux of power transient that is not beyond the power transient CHF will not induce the surface temperature overshooting. However, when the peak heat flux is greater than power-transient MHF, the surface temperature overshooting to the film boiling regime will definitely occur on the cladding.

Although the fuel-clad thermal conductivities and heat capacities might be enhanced by the increasing of temperature to accommodate more thermal energy and sustain the transition boiling, the intrinsically instable nature of transition boiling may turn reversible hot patches into irreversible dry patches at any time, and expedite those dry patches to form a vapor blanket layer devastating fuel-clad entities. This could possibly make power-transient CHF fall between MFB-HF and MHF. The thermal safety assessment based on power-transient CHF might be much conservative and deteriorate the nuclear power economy if power-transient CHF was accidentally procured around the MFB-HF point.
of each transient cycle increases step by step. As Fig. 7-7(b) shown below, in the first power transient, a sharp increasing of surface temperature was reflected on surface temperature profiles while no CHF occurrence was detected based on the CHF occurrence criteria. However, in the last power transient, a sudden overshooting of surface temperature was detected, and CHF occurred during the escalating stage of heat flux. However, the transient CHF was less than the peak heat flux of the previous transient cycle. Comparing the first power transient cycle with the last one, the peak heat flux

Figure 7-7: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of 0.2236√t under G = 500 kg/(m²·s) and ∆T_{in, sub} = 0 °C: (a) a sequent series of 7 linear ramp power transients and (b) cladding surface temperature responses to 7 sequent power transients

noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.
difference between two transient cycles are within the measurement uncertainty (i.e., ±5%). However, (1) for the first transient cycle only shows a shortly sharp increasing of surface temperature near the outlet, and (2) in the 5 subsequent transient cycles of higher peak heat flux there is no sharp increasing or no sudden overshooting being observed in surface temperature profiles. But (3) in the last transient cycle, its peak heat flux was supposed greater than that of previous 6 cycles while the power was immediately cut off due to the CHF occurrence. In this project study, it is called the flow boiling instability of power transient CHF that for the experimental observation to the power transient CHF of the current cycle less than the peak heat flux of the previous cycle. Such this flow boiling instability of power-transient CHF is also observed in the linear ramp power transient flow boiling experiments with other different voltage increasing rates (See Figs. 7-8, 7-9,7-10, 7-11 and 7-12).

It is reported that the flow boiling instability can lead to the early occurrence of CHF (Prajapati & Bhandari, 2017). According to the criteria (Prajapati & Bhandari, 2017; Haas et al., 2011; Stoddard et al., 2002), the observed flow boiling instability of power transient CHF can be attributed to the Ledinegg instability affected by heat flux and the thermal oscillation (H. T. Liu et al., 1994; Weatherhead, 1963). Different from the steady-state flow boiling, the surface heat flux increases rapidly and allows the flow boiling regime to transit from the bubbly flow to the slug/churn flow within a short time. During the flow boiling regime transition, large chunks of vapor crowds can touch the wall surface and deteriorate heat transfer coefficients drastically, further resulting in the surface temperature overshooting. The experimental observations that the temporary sharp increasing of surface temperature shows on and off upon the cladding response to power transient give direct evidence to the stochastic likelihood of flow boiling instability. However, the increasing of mass flux and/or inlet subcooling can enhance the bubbly flow and prevent the flow boiling instability from enabling the regime transition from bubbly flow to the slug/churn flow (See Figs. 7-14 ∼ 7-19).

Flow Boiling Instability Resulting from Power-Transient: To further explain the experimental observations of surface temperature overshooting without flow boiling CHF occurrence, the flow boiling pattern map reflected by $X_e$ and $G$ (See Fig.7-13
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3

Figure 7-8: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.25\sqrt{t}$ under $G = 500$ kg/(m²-s) and $\Delta T_{in,sub} = 0^\circ C$: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients

)is employed to assist the phenomenological behaviors of flow boiling instability. As shown in Fig.7-13, the progressive surging of heat flux can turn the bubbly flow to the churn/slug flow, even to the annular flow. The sharp increasing of surface temperature without CHF occurrence observed in Figs.7-8,7-10 and 7-11 could possibly be attributed to a momentary contact of bubble slugs with the cladding surface and the rewetting of he forced-driven liquid supply to caused hot patches owing to the hydrodynamics instability of slug flow and wave prorogation. Still, the mechanistic reason
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

Figure 7-9: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.40\sqrt{t}$ under $G = 500$ kg/(m²·s) and $\Delta T_{in,sub} = 0^\circ C$: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

behind why the power-transient experimental MHF of linear ramp was observed less than the peak heat flux in precursory transient cycles might be rationalized by this postulated assumption. Larger bubble slugs are in contact with the cladding surfaces for long enough so that the film boiling occurs to induce, deformation, ballooning, and even cracks of claddings while in precursory transient cycles, large bubble slugs don’t touch the wall surfaces for long enough. As a result of this, it is a stochastic process that flow boiling CHF occurs right after several unpredictable precursory transient.
Figure 7-10: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.50\sqrt{t}$ under $G = 500$ kg/(m²·s) and $\Delta T_{in,sub} = 0^\circ$C: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

This is also confirmed in our linear ramp power transient flow boiling experiments because the number of precursory transient cycles without triggering CHF is randomly distributed. However, in the bubbly flow regime with high inlet subcooling and/or high mass flux, the hydrodynamics instability is difficult to occur and result in unpredictable precursory transient cycles. This rationalizes why the gradual increasing of mass flux and/or inlet subcooling can diminish weird flow boiling phenomena observed in Fig.7-8 in the experimental results of Figs. 7-14 ~ 7-19.
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

Figure 7-11: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.60\sqrt{t}$ under $G = 500$ kg/(m²·s) and $\Delta T_{in, sub} = 0^\circ$C: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

The implications of the power-transient experimental results above to commercial LWRs are that (1) the flow boiling instability resulting from the power transient could be suppressed by the high mass flux and low inlet temperatures during LWRs nominal operations of coolant pumps and of heat sinks, and (2) the thermal safety margins of power-transient could be conservatively evaluated by the accumulated databases of steady-state boiling heat transfer.

Flow boiling transient MHF of SS 316 increases with respect to the increasing of
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3

Figure 7-12: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $1.00\sqrt{t}$ under $G = 500$ kg/(m²·s) and $\Delta T_{in,sub} = 0^\circ C$: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients

inlet subcooling at the mass flux of 300 kg/(m²·s)(See Tab.7.2). In addition, transient MHF under the investigated power transients is higher than the steady-state CHF. This agrees with the power transient flow boiling experimental results reported in Chapter 6. The transient MHF difference gap between two various heat input modes may be attributed to the thermal response of the tested materials.

The experimental transient MHF data were used to verify the two proposed prediction models of transient MHF (Pasamehmetoglu (Pasamehmetoglu, 1986) and Ser-
Figure 7-13: Flow boiling pattern based on $X_e$ and $G$

Table 7.2: SS 316 Power Transient $q(t) = kt^n$ MHF under the at $G=300$ kg/(m²·s)

<table>
<thead>
<tr>
<th>Inlet Subcooling</th>
<th>Transient $n = 1$</th>
<th>Transient $n = 2$</th>
<th>Steady-State</th>
</tr>
</thead>
<tbody>
<tr>
<td>0 °C</td>
<td>1.6268 MW/m²</td>
<td>1.3417 MW/m²</td>
<td>0.9485 MW/m²</td>
</tr>
<tr>
<td>5 °C</td>
<td>2.0494 MW/m²</td>
<td>1.7947 MW/m²</td>
<td>1.0543 MW/m²</td>
</tr>
<tr>
<td>10 °C</td>
<td>2.5346 MW/m²</td>
<td>2.3058 MW/m²</td>
<td>1.1706 MW/m²</td>
</tr>
<tr>
<td>15 °C</td>
<td>2.9047 MW/m²</td>
<td>2.7607 MW/m²</td>
<td>1.2365 MW/m²</td>
</tr>
<tr>
<td>20 °C</td>
<td>3.3572 MW/m²</td>
<td>3.0843 MW/m²</td>
<td>1.4711 MW/m²</td>
</tr>
</tbody>
</table>

$k$ is a constant of $\sim 8088$. The steady-state flow boiling experimental CHF is used for transient MHF prediction. OD = 0.375", WT = 0.010", HL = 2.00".

izawa (Serizawa, 1983)). The Pasamehmetoglu underestimates transient MHF while the Serizawa over-predicts transient MHF (See Fig. 7-20).

Flow boiling transient MHF of SS 316 increases with respect to the increasing of mass flux at the inlet subcooling of 0 °C (See Table 7.3).

The transient MHF increases and then decreases over the surging rate of power from 8,088 W/s² to 808,800 W/s² for the quadratic increasing power transient. This implies that faster power transient cannot guarantee the higher flow boiling MHF.
7.4.3 Transient MHF of Stainless Steels, Inconels, Zircalloys, FeCrAl Alloys under Fuchs-RIA Heat Input

Figs. 7-21, 7-22 and 7-23 respectively demonstrate how the Fuchs-RIA power transient has the influential impacts on the inner surface temperature of three different cladding materials, FeCrAl- B126Y, FeCrAl-C26M and zircaloy-4. As shown in Figs. 7-21, 7-22 and 7-23, the maximum inner surface temperature of cladding material
Figure 7-15: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of \(0.2236\sqrt{t}\) under \(G = 300\) kg/(m\(^2\)·s) and \(\Delta T_{in,sub} = 15\) °C: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

... delays appearing in the reflected thermal response profile to the Fuchs-RIA power transient in comparison with the peak heat flux of cladding material. This can be mechanistically explained by the transient energy deposition in the cladding material wall.

The lagging-behind of the maximum inner surface temperature can possibly allow the transition boiling to be reflected upon the power transient flow boiling curve (See Fig. 7-24(b)). This also rationalizes why power-transient CHF is lower than the peak...
Figure 7-16: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.2236\sqrt{t}$ under $G = 300$ kg/(m²·s) and $\Delta T_{in,sub} = 20^\circ$C: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

The implications of experimental results shown in Fig. 7-24 are that under the Fuchs-RIA power transient, CHF that is traditionally characterized by the surface temperature overshooting could not be used as the thermal-safety margin of LWRs. Instead, the maximum heat flux (MHF) of Fuchs-RIA could indicate the upper transient power limit of Fuchs-RIA because the MHF occurrence can 100% guarantee the surface temperature overshooting, and result in the further occurrence of CHF. However, if the power-transient
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

Figure 7-17: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.2236\sqrt{t}$ under $G = 300$ kg/(m²·s) and $\Delta T_{in,sub} = 25^\circ$C: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

CHF of Fuchs-RIA demarcates the power limits of LWRs, the surface temperature overshooting will not occur on the cladding surface. But on the other hand, this will reduce the safety tolerance of LWRs to power transients and also deteriorate the power economy of LWRs. Both Fig. 7-25 and Fig. 7-26 speak to that the boiling heat transfer mechanisms of power surging are different from that of power-collapsing within a complete transient cycle of Fuchs-RIA heat input (MHF does not occur on the cladding surface). For the power surging/increasing stage, the SP-HTC and ONB
Figure 7-18: Surface temperatures responses of SS316 (OD 3/8", WT 0.01" and HL 2") to the linear ramp power transients at the voltage rate of $0.2236\sqrt{t}$ under $G = 300$ kg/(m²·s) and $\Delta T_{in,sub} = 30^\circ C$: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

Tab. 7.5 gives a direct experimental proof to that the physics contributions to the power-transient MHF from mass flux and inlet subcooling share the similar mechanistic mechanisms from the perspective of steady-state flow boiling CHF. It is illustrated in Fig. 7-27 the increasing of mass flux and/or inlet subcooling can substantially en-
noting that Location 1 is placed near the outlet, Location 3 is placed at the axial midpoint of tested tube, Location 2 is centered between 1 and 3.

Figure 7-19: Surface temperatures responses of SS316 (OD 3/8”, WT 0.01” and HL 2”) to the linear ramp power transients at the voltage rate of $0.2236\sqrt{t}$ under $G = 500 \text{ kg/(m}^2\text{s})$ and $\Delta T_{in,sub} = 10^\circ C$: (a) a sequent series of linear ramp power transients and (b) cladding surface temperature responses to the sequent power transients.

hance the power-transient MHF. As expected, the faster power transient of Fuchs-RIA can result in the higher flow boiling MHF. In comparison with the practical cases of LWR Fuchs-RIA, their corresponding flow boiling MHF could be much higher than the steady-state flow boiling CHF.

It is noteworthy that the difference gap of power transient flow boiling MHF between four transient FWHMs is small either under the flow boiling condition of low inlet subcooling and low mass flux. If the increasing of inlet subcooling and (or) mass
Figure 7-20: Power-transient experimental MHF results evaluated by two mechanistic models respectively developed in (Pasamehmetoglu, 1986) and Serizawa (Serizawa, 1983)

Table 7.3: SS 316 Power Transient $q(t) = kt^2$ MHF under the $\Delta T_{in,sub} = 0^\circ C$

<table>
<thead>
<tr>
<th>$G$</th>
<th>Transient MHF</th>
</tr>
</thead>
<tbody>
<tr>
<td>250 kg/(m$^2$·s)</td>
<td>1.2914 MW/m$^2$</td>
</tr>
<tr>
<td>300 kg/(m$^2$·s)</td>
<td>1.3417 MW/m$^2$</td>
</tr>
<tr>
<td>350 kg/(m$^2$·s)</td>
<td>1.3981 MW/m$^2$</td>
</tr>
<tr>
<td>400 kg/(m$^2$·s)</td>
<td>1.5303 MW/m$^2$</td>
</tr>
<tr>
<td>450 kg/(m$^2$·s)</td>
<td>1.7939 MW/m$^2$</td>
</tr>
<tr>
<td>500 kg/(m$^2$·s)</td>
<td>1.9661 MW/m$^2$</td>
</tr>
</tbody>
</table>

$k$ is a constant of $\sim 8088$. OD = 0.375", WT = 0.010", HL = 2.00"

Table 7.4: SS 316 Power Transient MHF under the $\Delta T_{in,sub} = 0^\circ C$ and $G=300$ kg/(m$^2$·s)

<table>
<thead>
<tr>
<th>$V = \alpha t$</th>
<th>$q(t) = kt^2$</th>
<th>Transient MHF</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.05$t$</td>
<td>$\sim 8088t^2$</td>
<td>1.3417 MW/m$^2$</td>
</tr>
<tr>
<td>0.08$t$</td>
<td>$\sim 20705t^2$</td>
<td>1.6875 MW/m$^2$</td>
</tr>
<tr>
<td>0.1$t$</td>
<td>$\sim 32352t^2$</td>
<td>1.8385 MW/m$^2$</td>
</tr>
<tr>
<td>0.5$t$</td>
<td>$\sim 808800t^2$</td>
<td>1.6728 MW/m$^2$</td>
</tr>
</tbody>
</table>

OD = 0.375", WT = 0.010", HL = 2.00"

flux reaches the intermediate level, this difference gap can be exaggerated. However, the further increasing of mass flux and inlet subcooling can suppressed such difference gap between different FWHMs. This experimental observation could be rationalized
Figure 7-21: FeCrAl-B126Y cladding subjected to Fuchs-RIA under $G = 200$ kg/(m$^2$·s) and $\Delta T_{in, sub} = 10^\circ$C

Figure 7-22: FeCrAl-C26M cladding subjected to Fuchs-RIA under $G = 200$ kg/(m$^2$·s) and $\Delta T_{in, sub} = 10^\circ$C

by the flow boiling instability and the heat convection dominance.

It is noted that in Part 7.4.2 the flow boiling instability resulting from the power surging of heat input is significant at the weak heat convection regime of low mass flux and of low inlet subcooling. The flow boiling instability of thermal oscillation leads to the premature occurrence of surface temperature overshooting on cladding material surfaces, which is attributed to the small difference gap of power-transient flow boiling MHF at the weak convection regime. On the other hand, the high mass flux and/or high inlet subcooling significantly improve the dominance of heat convection mechanisms over the material-side related heat conduction between solid and
### Figure 7-23: Zircaloy-4 cladding subjected to Fuchs-RIA under $G = 200 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 10^\circ\text{C}$

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Surface Power (kW/m$^2$)</th>
<th>Surface Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1190</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1200</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1210</td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td>1240</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

### Figure 7-24: Power transient MHF occurrence on the zircaloy-4 being subjected to the Fuchs RIA of $s = 8\text{s}$ under $G = 200 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 10^\circ\text{C}$

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Surface Power (kW/m$^2$)</th>
<th>Surface Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1600</td>
<td></td>
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<tr>
<td>1610</td>
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<td>1620</td>
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</tr>
<tr>
<td>1650</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Liquid. This explains why higher mass flux and/or higher inlet subcooling can close the difference gap of power-transient MHF between different FWHMs. Similar trends of power transient flow boiling MHF with respect to the mass flux, inlet subcooling, and FWHM could be observed for FeCrAl- B126Y, zircaloy-4, and Zirlo in Tabs. 7.6, 7.7 and 7.8.

As shown in Tabs. 7.5, 7.6, 7.7 and 7.8, power-transient flow boiling MHF of FeCrAl alloys are generally lower than that of zircalloys under the same experimental settings even though FeCrAl- C26M has a higher pool boiling CHF than zircaloy-4.

Although FeCrAl alloys have favorable advantages of thermal-physical proper-
ties including thermal conductivity, and heat capacity, the tested tubing samples of FeCrAl alloys have thinner wall thickness than zircalloys (See Tab. 3-1). This potentially connotes that the wall thickness of cladding material can exert influential impacts on power-transient flow boiling MHF. This is quite reasonable because the power-transient boiling heat transfer is more dependent of wall thickness of cladding.
Table 7.5: Power Transient Flow Boiling MHF of FeCrAl-C26M Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcooling

<table>
<thead>
<tr>
<th>$G/s/\Delta T_{in,sub}$</th>
<th>10°C</th>
<th>15°C</th>
<th>20°C</th>
<th>25°C</th>
<th>30°C</th>
<th>35°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>200 kg/(m²·s)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>8s</td>
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<td>1075.53</td>
<td>1102.359</td>
<td>1150.843</td>
<td>1193.962</td>
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<td>1083.52</td>
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</tr>
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<td>1165.244</td>
<td>1235.482</td>
<td>1304.409</td>
<td>1418.215</td>
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<td>1165.634</td>
<td>1200.305</td>
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<td>1407.898</td>
<td>N/A</td>
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<td></td>
</tr>
<tr>
<td>8s</td>
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<td>N/A</td>
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<tr>
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<td>N/A</td>
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<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

material than the steady-state boiling from the standpoint of transient heat conduction. Speaking from the cladding mechanical integrity, FeCrAl alloys are more robust to the occurrence of power-transient MHF and maintain the mechanical integrity. Fig.7-28 presents the post-CHF cladding sample outlooks of FeCrAl-C26M, FeCrAl-
Figure 7-27: Power transient MHF of FeCrAl-C26M subjected to four Fuchs-RIA heat inputs: (a) effect of mass flux and (b) effect of inlet subcooling

B126Y and zircaloy-4 respectively being subjected to the same power-transient experimental setting. zircaloy-4 failed at resisting the thermal-stress resulting from the surface temperature overshooting and broke down to crack. FeCrAl-C26M and FeCrAl-B136Y maintain the mechanical integrity of encasing fuel pellets even though the post-CHF claddings show the deforming and ballooning failures.

Figure 7-28: Tube failures of three cladding materials as a result of power-transient surface temperature overshooting

In this dissertation study, Inconel 600, Inconel 625, SS 304, and SS 316 are also subjected to the Fuchs-RIA power transients generated by Eq.7.1 and their flow boil-
Table 7.6: Power Transient Flow Boiling MHF of FeCrAl-B126Y Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

<table>
<thead>
<tr>
<th>$G/s/\Delta T_{in,sub}$</th>
<th>10°C</th>
<th>15°C</th>
<th>20°C</th>
<th>25°C</th>
<th>30°C</th>
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<tbody>
<tr>
<td>200 kg/(m²·s)</td>
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<tr>
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<td>1199.862</td>
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<td>1079.922</td>
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<td>1200.523</td>
<td>1238.921</td>
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<td>1174.086</td>
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<tr>
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</table>

ing MHFs of different system conditions are also tabulated to across-compare those of nuclear fuel claddings (Tab.7.9 for Inconel 600, Tab.7.10 for Inconel 625, Tabs.7.11 and 7.12 for SS 304, Tabs.7.13 and 7.14 for SS 316). As shown in Tabs. 7.9 and 7.10, the experimental MHF of Inconel-600 is higher than that of Inconel-625.

This also agrees with that the steady-state CHF of Inconel-600 is higher than that of Inconel-625 in the saturated pool boiling of water. This power-transient priority of Inconel-600 over Inconel-625 could be attributed to that thermal-physical properties of Inconel-600 including thermal conductivity and specific heat capacity are higher.
Table 7.7: Power Transient Flow Boiling MHF of zircaloy-4 Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

<table>
<thead>
<tr>
<th>$G/s/\Delta T_{in,sub}$</th>
<th>10°C</th>
<th>15°C</th>
<th>20°C</th>
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<tr>
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than those of Inconel-625. The experimental results of Inconel-600/625 confirm again in Tabs. 7.9 and 7.10 that the difference gap of power-transient MHF between four different transient time-scales is closed at the high mass flux and/or high inlet subcooling (See Fig.7-29). However, at the weak heat convection regime ($G = 200 \text{ kg/(m}^2\text{·s)}$ and $\Delta T_{in,sub} = 10{\degree}\text{C}$), there is no appreciable difference gap of Inconel-600 power-transient MHF between four transient time-scales and this result can be similarly observed in the power-transient MHF of FeCrAl-C26M (See Fig.7-27). In light of the weak heat convection regime together with the power transient surging, the
Table 7.8: Power Transient Flow Boiling MHF of Zirlo Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

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Flow boiling instability could be possibly responsible for the insignificant difference gap. Because the flow boiling instability is related with mass flux and thermal equilibrium quality. It is observed in the subcooled flow boiling MHFs of FeCrAl-C26M (See Fig.7-27(b)) and of Inconel-600 (See Fig.7-29(b)) that the parametrical trend of power-transient MHF with respect to the inlet subcooling can be roughly approximated by a linear correlation for the slow transient case of FWHM=8s. This is not surprising because it is concluded in the review study of power transient boiling in Chapter 6 that the slow power-transient boiling heat transfer is almost indifferent.
Table 7.9: Power Transient Flow Boiling MHF of Inconel-600 Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

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Both experimental results of SS 304 (Tabs. 7.11 and 7.12) and SS 316 (Tabs. 7.13 and 7.14) supported that the power-transient flow boiling MHFs of 0.010”-thick SS
Figure 7-29: Power transient MHF of Inconel-600 subjected to four Fuchs-RIA heat inputs: (a) effect of mass flux and (b) effect of inlet subcooling

304 & 316 tubes are higher than that of 0.015"-thick SS 304 & 316 tubes. These power-transient flow boiling MHF results are in agree with the pool boiling CHF cases of various thick SS 304 & 316 tubes tabulated in Chapter 4. This is intuitively contradictory because power transient pool boiling MHF experimental studies reported that the parametric trend of power-transient MHF with respect to the increasing of wall thickness shows an asymptotically increasing behavior in Chapter 6. On the contrary, the experimental investigations to the effects of tubing wall thickness on steady-state CHF/HTCs, and power-transient MHF speak to the possibility that the boiling heat transfer mechanisms on cladding tubes are completely different from those on the plates. This is quite understandable. Because the boiling heat transfer direction on plate heaters is not like the boiling behaviors of cladding tubes.

As expected, the influential impacts of inlet subcooling on MHF difference gaps contributed by wall thickness are physically similar to their effects on gaps resulting from transient time-scales (See Fig.7-30). Similarly, it is reasonably believed that as the progressive increasing of mass flux, the difference gap of power-transient MHF between different wall thicknesses is first exaggerated but then suppressed, and even is not noticed obviously within the measurement uncertainty. This postulation is experimentally verified by the SS 316 MHF results at $\Delta T_{in,sub} = 10^\circ$C under two transient time-scales (See Fig.7-31). From the standpoint of transient heat conduc-
Table 7.11: Power Transient Flow Boiling MHF of SS 304 (WT=0.010") Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

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Table 7.12: Power Transient Flow Boiling MHF of SS 304 (WT=0.015") Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

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tion, the wall thickness of cladding has two roles in the direct joule heating method of this dissertation study, (1) thermal energy deposition owing to the spatiotemporal cladding temperature variation, the thicker the cladding wall is, the more thermal energy is deposited in the cladding, and thus leading to the less thermal energy of power-transient MHF occurrence. This may partially explain why 0.010"-thick SS 304 & 316 tubes have higher MHFs than their 0.015"-thick counterparts. (2) The other role is to reduce the volumetric heat source in transient heat conduction, and therefore reduce the possibility of higher surface heat flux generation. These two coupled mechanistic conduction mechanisms could rationalize why the 0.010"-thick SS 304 & 316 cladding tubes have higher power-transient MHFs than their 0.015"-thick counterparts. Meanwhile, this wall-thickness dominated heat conduction mechanism can be superseded by the heat convection mechanisms enhanced by mass flux and/or liquid subcooling in the power-transient boiling heat transfer.

7.4.4 Discussions and Implications of Power Transient Experimental Results

The experimental power-transient MHF results aforementioned above imply that there are at least three distinct mechanistical mechanisms affecting T-H responses of claddings to power-transients as illustrated in Fig.7-32:
Table 7.13: Power Transient Flow Boiling MHF of SS 316 (WT=0.010") Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

<table>
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<th>G/s/ΔT_{in, sub}</th>
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1. mass flux/inlet subcooling: The mechanical role of mass flux in the heat convection is to improve the shear stress force that acts upon the boundary layer of bubble crowds and to enhance the bubble departure frequencies. This reduces boiling the heat transfer possibility through the heat conduction between solid and the liquid/vapor mixture. That physically explains why the increasing of mass flux can close the difference gaps contributed by power-transients, thermal-physical properties in terms of CHF, MHF, and HTCs. The superheated wall surfaces need to heat up the subcooled liquid before vaporizing it. This leads
Table 7.14: Power Transient Flow Boiling MHF of SS 316 (WT=0.015") Subjected to the Fuchs-RIA Heat Input Under Different Mass Fluxes and Inlet Subcoolings

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<th>15°C</th>
<th>20°C</th>
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To less thermal energy being transferred to the phase change of liquid, and further resulting in smaller bubbles. In other words, an analogy of natural convection in a boundary of subcooled liquid could help explain the effects of liquid subcooling. Although the increasing of inlet subcooling devastates the boiling HTCs, it can diminish the difference gaps of HTCs, MHFs and CHFs associating with material-side factors, transient-scales, and boiling instabilities. This can be reflected in the flow boiling pattern map, the progressive increasing of mass flux/liquid subcooling can transit the annular flow to slug/churn flow.

266
Figure 7-31: Effects of mass flux on the wall-thickness sensitive MHF of SS 316 at $\Delta T_{in,sub} = 10^\circ C$

to bubbly flow (See Fig.7-32).

2. thermal-physical properties and wall thickness: The experimental MHF results of ATF and traditional claddings speak to that the thermal-physical properties and cladding wall thickness can not be negligible at the weak heat convection regimes with low mass flux and/or low liquid subcooling (most cases are annular flow). It is because that heat conduction between solid and the vapor/liquid mixture is comparable with free/forced heat convection, even more dominant especially when the power-transient is so fast that the transient heat conduction is the primary heat transfer mechanism (See Fig.7-32).

3. power-transient modes/surging rates: As illustrated in Fig.7-32, the transient time-scales matter a lot in the T-H responses of cladding materials to power-transients. Because the vapor layer/dry-patch irreversible formation is subjected to interactive behaviors between transient time-scale and T-H time-scale (usually, the bubble departure period, See discussions in Section 6.3). The power-transient mode directly decides the time-wise volumetric heat source profile, and exerts the potential impacts on the spatiotemporal temperature distribution of fuel-clad entity while the power surging rates directly govern how thermal equilibrium quality increases over the time and determines which flow boiling pattern shall be presented to the fuel-clad entity.
Figure 7-32: Flow boiling MHF mechanisms-coupling scheme: transient time-scale, flow boiling pattern, heat convection and cladding materials, flow boiling pattern images in courtesy of L. Chen et al. (2006)

It could be anticipated that the solo RIA occurrence without the concurrence of LO-CA, LOFA or SBO poses no potential threats to thermal safety margins of PWRs because of higher mass flow rates and outlet subcooling. However in comparison with T-H safeties of RIA transients, the induced possible damages to structural integrity of cladding materials are of much more concerns such as cladding deformation, ballooning, and cracks. When coming to the implications of the experimental results herein to BWRs, the power-transient thermal-safety assessments become more sophisticated because of saturated flow boiling, thermal oscillation, relatively weak heat convection.

In comparison with NRC-tabulated reactor accident scenarios of RIA, the experimental scope of FWHM in this study falls in the rod ejection accidents of PWR hot full power. Therefore, experimental results of RIA herein could be safely applicable to the PWR hot full power accident evaluation, other accident scenarios are not
7.5 Conclusions

In this study, the experimental results demonstrate that the physics contributions of mass flux, and inlet subcooling to power-transient flow boiling CHF could be explained from the perspective of steady-state flow boiling CHF. It is confirmed that under the same flow boiling conditions, the power-transient flow boiling CHF is higher than the steady-state flow boiling CHF. This speaks to the possibility of more thermal safety margins under the power-transients of LWRs.

In the linear ramp power-transient, the corresponding experimental results show that the power transient CHF shows the increasing behavior first, then the decreasing trend and finally the increasing pattern with respect to the increasing of power-surging rate. Besides this, the flow boiling instability resulting from the surging surface heat flux and thermal oscillation leads to the possible premature of surface temperature overshooting. However, the increasing of mass flux and/or of inlet subcooling can suppress the flow boiling instability.

Regarding to the FeCrAl and zircaloy cladding materials subjected to Fuchs-RIA power transient with different FWHMs, the peak temperature of inner cladding surface lags the peak power of Fuchs-RIA because of the thermal energy deposition in cladding wall. This experimental phenomenon implies that in power-transient flow boiling of Fuchs-RIA heat input, the transition boiling regime could appear before the sudden jumping to the film boiling. Thus, power-transient flow boiling CHF characterized by surface temperature overshooting cannot be used as the thermal safety margin of LWRs under RIA transients. Instead, the power transient flow boiling MHF mechanistically demarcates the upper power limit of LWR under RIA scenarios because the occurrence of flow boiling MHF makes a positive assurance that the temperature overshooting occurs on the surface of cladding materials. It is noteworthy that the difference gaps of power transient flow boiling MHF between different transient time-scales, cladding materials and wall thicknesses can be suppressed by
higher mass flux and/or inlet subcooling at the dominant heat convection regime. However, at the weak heat convection ($G = 200 \text{ kg/(m}^2\cdot\text{s})$ and $\Delta T_{in,sub} = 10^\circ\text{C}$), these difference gaps of power-transient MHF are not so appreciable as the intermediate heat convection because of the thermal flow boiling instability that is induced by the power-surging of cladding materials. Also, FeCrAl alloys are more capable of resisting the thermal stress resulting from surface temperature overshooting and maintaining mechanical integrity than zircaloy-4.
Chapter 8

Concluding Remarks

8.1 Summaries

In the pool boiling experiments of FeCrAl alloys, zircaloys and other commercial alloys, the experimental results give a direct confirmation to that FeCrAl-C26M has higher CHF and NBHTC than other five nuclear fuel cladding materials including FeCrAl-B126Y and FeCrAl-B136Y. Among three zircaloys, zircaloy-4 has a higher pool boiling CHF while Zirlo gives a better pool boiling NB-HTC. Given that FeCrAl alloys are more capable of sustaining the post-CHF heat transfer than zircaloys, the pool boiling experiments on the post-CHF surface of cladding materials show that the pool boiling CHF can be enhanced more than 32% by the post-CHF surfaces. The surface wettability analyses showed that post-CHF surfaces are more hydrophilic than the as-received surfaces. Although this surface wettability enhancement by the oxide layer partially accounts for the pool boiling CHF promotion, the Kandlikar model cannot resolve the discrepancy between predicted and experimental results. Among three FeCrAl alloys and three zircaloys, it is found that FeCrAl-C26M has the best corrosion resistance to the high-temperature water/steam mixture in the pool boiling CHF experiments because of its thinnest oxide layer measured by SEM scanning. Besides these findings, the pool boiling experiments of horizontally-placed tubes show that the effects of tubing wall thickness on pool boiling CHF contradict the
previous experimental results that were obtained from the square plate heaters and the thermal activity mechanism proposed in literatures cannot explain the parametric trend of pool boiling CHF with respect to the tubing wall thickness. More importantly the thermal-physical properties of cladding material do play a critical role in the material-conjugated boiling heat transfer. However, our experimental study as well as the recent boiling experiments of ATF cladding are still not able to demystify how material thermal-physical properties affect pool boiling CHF and NB-HTC.

The steady-state subcooled flow boiling experiments show us there are also significant impacts of material-side factors on flow boiling CHF and HTCs including surface wettability/roughness, and thermal-physical properties. However, the increasing of mass flux and/or inlet subcooling can gradually close the difference gaps of T-H performances between various cladding materials with different surface morphologies including CHF and HTCs. Compared with the steady-state flow boiling, the power transient flow boiling seems more complicated and insofar are not mechanistically resolved by the present knowledge of boiling heat transfers. For example, the power transient CHF and HTCs are higher than their steady-state counterparts under the same flow boiling settings while the faster surging rates of power-transient can not necessarily assure better T-H performances of cladding materials. But it is proven in our experimental results of power transient flow boiling as well as the previous experimental observations that the heat conduction between cladding solid and the water coolant is obviously significant especially either under the fast power transient with short transient timescales or at the weak heat convection regime of low mass flux and of low inlet subcooling. In comparison with the steady-state flow boiling, the power transient flow boiling is more dependent of cladding material wall thickness and thermal-physical properties. However, the T-H performance dependency of power transient flow boiling on transient timescale, wall thickness and thermal-physical properties can be weakened by the heat convection enhancement by the increasing of mass flux and/or of inlet subcooling. In light of this, the present accumulated knowledge of steady-state flow boiling could be conservatively used to evaluate the thermal safety margin of LWRs under the RIA and other power transient scenarios.
including CHF and HTC but not including PCT. Because the PCT of fuel-clad entity is substantially dependent of reactor power density.

8.2 Future Works

Although some key promising points that are worthy of future further investigations are identified in Subsections 3.5 and 6.5, it is still of utmost importance to address how the material-side factors of near-field mechanism including surface morphologies and thermal-physical properties affect the far-field heat convection mechanisms mainly dominated by mass flux and inlet subcooling. On top of this, it is more imperative to experimentally characterize each influential factor individually using the indirect joule heating method,

In comparison with power transient T-H flow boiling performances, the thermal-mechanical issues resulting from power transient T-H changes is much more of concern including thermal stress/shock and cladding structural integrity. For example, the swelling, deforming, and ballooning of fuel-clad entity seems a far-reaching safety concern resulting from RIA transients (See Fig. 6-31). Because the dislocation or misalignment of fuel pins may prevent the in-housing control rod injection into reactor cores during RIA and other power transients.
Chapter 9
Numerical Frameworks Involved in
this Study

9.1 Steady-State Radial Temperature Distribution
of Test Section

The steady-state heat conduction model of the central high-temperature tolerant filler
can be simplified as a one-dimensional radial version,

\[
\frac{1}{r} \frac{\partial}{\partial r} \left( k_c r \frac{\partial T}{\partial r} \right) = 0, \quad 0 \leq r \leq \frac{OD}{2} - WT
\]  

(9.1)

where \( T \) is the radially spatial distribution of temperature of test section, \( k_c \) is the
cement filler thermal conductivity dependent of \( T \), and \( r \) is the region of interest. An
alternative form of Eq. 9.1 is written as follows

\[
\int_{T_o}^{T_i} k_c(T) dT = C \int_0^{OD - WT} \frac{dr}{r}
\]  

(9.2)

where \( C \) is an unknown constant, \( T_o \) is the centerline temperature of test section,
\( T_i \) is the interface temperature between tubing material and cement filler, and \( k_c(T) \)
is usually given by an empirical correlation. To make the right-hand side of Eq.9.2
mathematically reasonable, the only feasible solution is to allow $C$ to be zero. This also speaks to that the heat flux at the centerline of test section is zero. Therefore, Eq.9.2 can lead to that the temperature of cement filler is uniformly distributed from the centerline to the interface and it implies that the cement filler is isothermal. For a complete cycle of power incremental step, the steady-state heat conduction model of the test specimens could be written as follows,

$$\frac{1}{r} \frac{\partial}{\partial r} \left( k_t r \frac{\partial T}{\partial r} \right) + q''' = 0, \quad \frac{OD}{2} - WT \leq r \leq \frac{OD}{2}$$  \hspace{1cm} (9.3)

where $k_t$ is the tubing material thermal conductivity dependent of $T$, $q'''$ is the volumetric heat source. And the boundary condition of Eq.9.3 at the interface can be given as follows,

$$q'' \left( \frac{OD}{2} - WT \right) = 0$$  \hspace{1cm} (9.4a)

$$T \left( \frac{OD}{2} - WT \right) = T_i$$  \hspace{1cm} (9.4b)

noting that $T_i$ is measured by K-type thermocouples at four different axial locations. Under the assist of the differential transformation method (C.-L. Chen & Liu, 1998), the outer surface temperature of test specimen can be computed based on the interface temperature measurement.
9.2 Stability Analyses of Transient Heat Conduction Solver

9.2.1 Explicit Numerical Form of Net Heat Gain $h_{ex}$

The explicit form of the right hand side of Eq.7.5 is presented as follows:

\[
\frac{\partial}{\partial z}(k(T)\frac{\partial T}{\partial z}) \approx \frac{k(T_{i,j+1/2}^{n})(T_{i,j+1}^{n} - T_{i,j}^{n}) - k(T_{i,j-1/2}^{n})(T_{i,j}^{n} - T_{i,j-1}^{n})}{\Delta z^2} \quad (9.5a)
\]

\[
\frac{\partial}{r \partial r}(k(T)r\frac{\partial T}{\partial r}) \approx 2 \frac{k(T_{i+1/2,j}^{n})(T_{i+1,j}^{n} - T_{i,j}^{n}) - k(T_{i-1/2,j}^{n})(T_{i,j}^{n} - T_{i-1,j}^{n})}{r'(\Delta r_{i-1} + \Delta r_{i})} \quad (9.5b)
\]

\[
q''(t) \approx q''(n\Delta t) \quad (9.5c)
\]

Thus $h_{ex}(T_{i,j}^{n}, n\Delta t)$ is has the following form

\[
h_{ex}(T_{i,j}^{n}, n\Delta t) = 2 \frac{k(T_{i+1/2,j}^{n})(T_{i+1,j}^{n} - T_{i,j}^{n}) - k(T_{i-1/2,j}^{n})(T_{i,j}^{n} - T_{i-1,j}^{n})}{r'(\Delta r_{i-1} + \Delta r_{i})} \frac{(T_{i,j+1}^{n} - T_{i,j}^{n})}{\Delta z^2} \frac{(T_{i,j+1}^{n} - T_{i,j}^{n})}{\Delta z^2} + q''(n\Delta t) \quad (9.6)
\]

9.2.2 Von Neumann Stability Analyses of Proposed Numerical Scheme

In order to reduce the implicit complexity of the Crank–Nicolson scheme, thermal mechanical properties in Eq.7.5 are treated as constants. This can be valid if the Crank–Nicolson scheme is still unconditionally stable in the case of the maximum thermal diffusivity.

The 2D Crank–Nicolson scheme of tubular geometry is respectively decomposed into the infinite 1D-cylindrical and infinite 1D-slab versions. If both 1D-cylindrical and 1D-slab versions are unconditionally stable, then the 2D tubular scheme will be also unconditionally stable. The Crank–Nicolson scheme of 1D slab transient heat
conduction was already proven conditionally stable. Now the numerical stability of the 1D cylindrical Crank–Nicolson scheme is analyzed for the size-variant cell structure as follows

\[
\frac{T_{k+1}^n - T_k^n}{\alpha \Delta t} = \frac{r_{k+1/2}(T_{k+1}^n - T_k^n)}{\Delta r_k} - \frac{r_{k-1/2}(T_k^n - T_{k-1}^n)}{\Delta r_{k-1}} + \frac{r_{k+1/2}(T_{k+1}^{n+1} - T_k^{n+1})}{\Delta r_k} - \frac{r_{k-1/2}(T_k^{n+1} - T_{k-1}^{n+1})}{\Delta r_{k-1}} \tag{9.7}
\]

According to our meshing principles of 1D cylindrical structure, we can derive the following two equations

\[
\begin{align*}
    r_{k+1/2} &= \frac{r_k + r_{k+1}}{2} \quad (9.8a) \\
    \Delta r_{k-1}r_{k-1/2} &= \Delta r_k r_{k+1/2} \quad (9.8b) \\
    2r' &= r_{k-1/2} + r_{k+1/2} \quad (9.8c)
\end{align*}
\]

Based on Eq.9.8,

\[
\begin{align*}
    \frac{r_{k+1/2}(T_{k+1}^n - T_k^n)}{\Delta r_k} - \frac{r_{k-1/2}(T_k^n - T_{k-1}^n)}{\Delta r_{k-1}} &= 2 \frac{r_{k+1/2}^2 T_{k+1}^n + r_{k-1/2}^2 T_{k-1}^n - (r_{k-1/2}^2 + r_{k+1/2}^2) T_k^n}{\Delta r_{k-1/2}^2} \\
    &< \frac{\Delta r_{k-1/2}^2}{\Delta r_{k-1}^2} (1 + \frac{\Delta r_{k-1}}{\Delta r_k})(1 + \frac{\Delta r_k}{\Delta r_{k-1}}) r_{k-1/2}^2 T_{k+1}^n + r_{k-1/2}^2 T_{k-1}^n - (r_{k-1/2}^2 + r_{k+1/2}^2) T_k^n \\
    &< \frac{2\Delta r_{k-1/2}^2}{2\Delta r_{k-1}^2} T_{k+1}^n + T_{k-1}^n - 2T_k^n \tag{9.9}
\end{align*}
\]

In Eq.9.9 above, the first inequality results from \((1 + \frac{\Delta r_{k-1}}{\Delta r_k})(1 + \frac{\Delta r_k}{\Delta r_{k-1}}) > 4\) and the second one results from \(r_{k-1/2} < r_{k+1/2}\) and \(T_{k-1}^n < T_k^n < T_{k+1}^n\). So Eq.9.7 can take the following form of inequality

\[
T_{k+1}^n - T_k^n < \alpha \Delta t \frac{r_{k-1/2}^2}{2\Delta r_{k-1/2}^2} \left( \frac{T_{k+1}^n + T_{k-1}^n - 2T_k^n}{2\Delta r_{k-1/2}^2} + \frac{T_{k+1}^{n+1} + T_{k-1}^{n+1} - 2T_k^{n+1}}{2\Delta r_{k-1/2}^2} \right) \tag{9.10}
\]

It should be noted that the numerical error \(e_{k+1}^n\) of \(T_{k+1}^n\) also has the exact form same
to Eq.9.10. Replacing $T_{n+1}^k$ by $e_{n+1}^k$ in Eq.9.10 and applying Von Neuman analysis on it yields as follows

$$|\frac{\tilde{e}^{n+1}}{\tilde{e}^n}| < \omega(\lambda, \xi) = \frac{1 - 4\lambda \sin^2(\frac{\xi \Delta r_{k-1/2}}{2})}{1 + 4\lambda \sin^2(\frac{\xi \Delta r_{k-1/2}}{2})} \quad (9.11)$$

where $\lambda = \frac{\alpha \Delta t}{\Delta r_{k-1/2}^2}$ and $\xi$ is the wave number of Fourier series. It can be observed from Eq.9.11 that $\omega(\lambda, \xi)$ is always less than 1.0 no matter what values $\lambda$ and $\xi$ have. Accordingly, it is safely concluded that the proposed 2D Crank–Nicolson scheme of tubular geometry is also unconditionally stable.
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A Short Biography

The author of this dissertation, Mingfu He, was born in a small rural village near by a stream, Yibin, Sichuan, PRC. He spent his childhood playing fun in that stream that was usually fully of fishes, crabs and lobsters. That was where Mingfu noticed that water flows faster in narrow corridor paths than the wide ones, and pebbles in the downstream are smaller and smoother than ones in the upstream.

Mingfu went to an engineering oriented university (Chengdu University of Technology) that is professional in the uranium mining, milling, conversion, and enrichment, the front end of nuclear fuel cycle. Mingfu graduated with two Bachelor’s degrees respectively in nuclear engineering and English literature study. After his graduation with Bachelor’s degrees, Mingfu worked in Sichuan radiation detection and protection lab for a year as an electronics technician of neutron detection system. After that, he came to USA and pursued his higher degrees in thermal-hydraulics with department of nuclear engineering at University of New Mexico in Albuquerque, USA.

In 2019, Mingfu got his master degree with distinction under the supervision of Professor Youho Lee. In 2022, Mingfu completed his doctoral training of nuclear thermal hydraulics and obtained his Ph.D under the supervision of Professor Minghui Chen.

The main driving force behind his persistent pursuit to a Ph.D is that Mingfu wants to earn better milk and bread for his family members. Although Mingfu has no dream of making contributions to the progress of nuclear energy, or becoming a reputable nuclear engineer, he is still happy to see a sustainable world full of clean and safe nuclear energy.