TECHNICAL REPORT

Evaluation of DNBR under Operational and Accident Conditions for Double-Flat-Core Type HCLWR

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A double-flat-core type high conversion light water reactor (HCLWR) has been developed at JAERI to improve fuel utilization. Experimental and analytical studies on the departure from nucleate boiling ratio (DNBR) under operational and accident conditions for the HCLWR have been performed. It was found by comparing several critical heat flux (CHF) correlations with the CHF data obtained at JAERI and Bettis Atomic Power Laboratory that the KfK correlation has the most promising features for the application to the triangular tight lattice rod array. The minimum allowable DNBR (MDNBR) for the HCLWR was determined to be 1.28 by comparing the Bettis CHF data with the KfK correlation. The best-estimate code J-TRAC was used for system transient calculations under the primary coolant pump trip and locked rotor accident conditions. The subchannel code COBRA-IV-I was then used to obtain local flow conditions and fuel rod surface heat flux. The analytical results indicated that an enough safety margin was assured under the steady-state operational condition. Under the accident conditions, the evaluated minimum DNBR’s were also above the MDNBR criterion. Therefore, it was clarified that the present HCLWR design is feasible from a view point of the MDNBR criterion.

KEYWORDS: reactor safety, PWR type reactors, critical heat flux experiment, experimental data, departure from nucleate boiling, HCLWR type reactors, double-flat-core, subchannel analysis, accident analysis, thermal-hydraulic design, COBRA code, J-TRAC code, computer codes

I. INTRODUCTION

In recent years the concept of high conversion light water reactor (HCLWR), which is expected to improve fuel utilization with only a minor change in the existing light water reactor (LWR) technology, has been receiving much attention in several countries such as the USA(1), the Federal Republic of Germany(2), France(3), Switzerland(4) and Japan(5). Japan Atomic Energy Research Institute (JAERI) initiated the feasibility studies of the HCLWR in 1985 in the fields of reactor physics(6) and thermal-hydraulics(7). The objectives of the JAERI’s design study are to attain a high conversion ratio, a high discharge burnup and a high safety margin,
simultaneously.

The high conversion ratio can be achieved by hardening the neutron spectrum by reducing the moderator-to-fuel volume ratio \((V_m/V_f)\). As a result, a tight lattice fuel assembly is used in an HCLWR. In the tight lattice core the void reactivity coefficient tends to become higher or even positive. However, it is requested to keep the void reactivity coefficient sufficiently negative and meet the safety criteria under accident conditions. Ishiguro et al. proposed a flat-core concept\(^{(6)}\), in which a negative void reactivity coefficient is attained by neutron leakage through axial direction. Since the thermal output of the single flat-core was too small for an alternative of a current large scale light water reactor, the concept of a double-flat-core has been developed\(^{(7)}\). The concept of the pressure vessel and major design parameters are shown in Fig. 1 and Table 1, respectively. Figure 2 shows the configuration of fuel rod. In this new design two flat cores are piled up with lower, inner and upper axial blankets so that the reactor thermal output is doubled by the efficient use of the pressure vessel space while preserving the basic neutronic characteristics of the single-flat-core. The lengths of each core parts and blanket parts are 0.6 and 0.3 m, respectively. The fuel active length including the core and blanket parts is 2.1 m. The outer diameter of fuel rod is 9.5 mm. Fuel rods are arranged in a triangular lattice with \(p/d\) of 1.23. The core consists of 313 fuel assemblies and 66 radial blanket assemblies. The electrical output, primary coolant flow rate and temperature rise along the core are designed to be approximately the same as those of a conventional 3-loop PWR.

The tight lattice core leads to some thermal-hydraulic problems peculiar to the HCLWR. Among them the prediction of critical heat

![Fig. 1 Pressure vessel of double-flat-core HCLWR](image)

**Table 1 Major parameters of double-flat-core HCLWR**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal output</td>
<td>2,432 MWt</td>
</tr>
<tr>
<td>Electrical output</td>
<td>810 MWe</td>
</tr>
<tr>
<td>Number of primary loops</td>
<td>3</td>
</tr>
<tr>
<td>Core height</td>
<td>2.1 m (active core)</td>
</tr>
<tr>
<td></td>
<td>0.6 m \times 2 (blanket)</td>
</tr>
<tr>
<td></td>
<td>0.3 m \times 3</td>
</tr>
<tr>
<td>Number of fuel assembly in core</td>
<td>313 (control rod assembly 85)</td>
</tr>
<tr>
<td>Number of fuel assembly in radial blanket</td>
<td>66</td>
</tr>
<tr>
<td>Fuel assembly pitch</td>
<td>235.4 mm</td>
</tr>
<tr>
<td>Fuel rod outer diameter</td>
<td>9.5 mm (core)</td>
</tr>
<tr>
<td></td>
<td>9.8 mm (radial blanket)</td>
</tr>
<tr>
<td>Fuel rod pitch</td>
<td>11.7 mm</td>
</tr>
<tr>
<td>Cladding thickness</td>
<td>0.57 mm</td>
</tr>
<tr>
<td>Cladding material</td>
<td>Zircaloy-4</td>
</tr>
<tr>
<td>Control rod thimble diameter</td>
<td>11.0 mm</td>
</tr>
<tr>
<td>Number of fuel rods/assembly</td>
<td>372 (core), 397 (radial blanket)</td>
</tr>
<tr>
<td>Number of control rod thimble</td>
<td>24/assembly</td>
</tr>
<tr>
<td>Number of instrumentation thimble</td>
<td>1/assembly</td>
</tr>
<tr>
<td>Equivalent core diameter</td>
<td>4.373 m</td>
</tr>
<tr>
<td>Inner diameter of core barrel</td>
<td>5.25 m</td>
</tr>
<tr>
<td>Inner diameter of RPV</td>
<td>5.85 m</td>
</tr>
<tr>
<td>Moderator-to-fuel volume ratio</td>
<td>1.06</td>
</tr>
<tr>
<td>Discharge burnup</td>
<td>56 GWd/t</td>
</tr>
<tr>
<td>Fissile Pu enrichment</td>
<td>(~10%)</td>
</tr>
<tr>
<td>Average conversion ratio</td>
<td>0.83</td>
</tr>
</tbody>
</table>
flux (CHF) is one of the most important safety concerns, because the smaller gap width between fuel rods may reduce the CHF. In order to maintain an adequate safety margin related to the CHF, the new design core should satisfy a minimum departure from nucleate boiling ratio (MDNBR) criterion. That is, the MDNBR in a hottest channel should be higher than a statistically specified value under the steady-state and operational transient conditions. Note that the terminologies of CHF and DNB are often used interchangeably under pressurized water reactor (PWR) conditions, though there may be slight differences between these two notations.

In the present report, existing CHF correlations are evaluated by comparing with CHF data for HCLWR configurations. The MDNBR for the HCLWR is determined based on a "95% probability at 95% confidence level" criterion which has been used traditionally in the LWR licensing procedure. Furthermore, DNB analyses under operational and accident conditions for the double-flat-core HCLWR are presented to evaluate the thermal-hydraulic feasibility of the present design.

II. EVALUATION OF CHF CORRELATIONS

1. CHF Correlations

A lot of CHF correlations have been proposed for conventional LWRs. Nevertheless, most of those correlations are not valid for the HCLWR core because of the difference in rod bundle configurations.

The WSC-2 correlation\(^8\) can be applicable to a triangular pitched rod array. However, the correlation has not been especially developed for the tight lattice core of the HCLWR. Dalle Donne & Hame\(^9\) recently proposed a CHF correlation (KfK correlation) valid for tight lattice rod arrays. The KfK correlation was developed by determining new geometry-dependent parameters and spacer parameter of the WSC-2 correlation based on experimental data with triangular lattice rod arrays. Uotinen et al.\(^1\) also developed a CHF correlation (EPRI-B&W correlation) for an HCLWR configuration based on an energy balance consideration. The coefficients in the correlation were optimized using the data obtained from triangular and square pitched rod arrays. Akiyama et al.\(^10\) showed that EPRI-Columbia correlation\(^11\) with minor modification was able to apply to their HCPWR design, though the correlation was originally developed for square lattice cores. Therefore, the WSC-2, KfK, EPRI-B&W and EPRI-Columbia correlations were selected for the comparison with CHF data obtained for HCLWR configurations. The correlations are listed in Table 2.

2. Subchannel Analysis

Local flow conditions such as mass velocity and enthalpy in a hot channel are different from bundle averaged values especially for a rod bundle with non-uniform radial heat flux distribution or a small sized bundle. In such a case, the local flow conditions should be used in the evaluation of the CHF correlations instead of the bundle averaged values. The subchannel analysis code COBRA-IV-I\(^12\) was used to calculate the local flow parameters.

3. Thermal Mixing Experiments and Mixing Coefficients

A sensitivity study suggested that a turbulent mixing coefficient, which is an input parameter of the COBRA-IV-I code, has the most significant effect on the prediction of
Table 2 CHF correlations

(1) **WSC-2 correlation**\(^{(9)}\)

\[
q_v(10^9 \text{Btu/h·ft}^2) = \frac{A + B\Delta H_i}{C + Z Y Y'}
\]

\[
A = \frac{0.25G D_1 F Q_1}{1 + Q_1 \Delta Q \Delta B(Y)^{q_2}} \quad B = 0.25G D
\]

\[
C = \frac{Q_1 F_1 (G D Y')^{q_2}}{D_w^q} 
\]

where \(\Delta H_i\): Inlet subcooling (°Btu/lb), \(Z\): Distance from channel inlet (in.)

\(Y\): Axial heat flux profile parameter = Average cluster heat flux from entry to \(Z\)

\(Y'\): Subchannel imbalance factor = Heat retained in subchannel

\(G\): Mass velocity (10^6 lb/h·ft^2), \(D = F_p D_h\)

\(D_h\): Heated equivalent diameter (in.), \(F_p\): Radial form factor

\(\lambda\): Latent heat of evaporation (Btu/lb)

\(D_w\): Wetted equivalent diameter (in.), \(P_r = 10^{-3} P\): Pressure (psia)

\[F_1 = P_r^{0.585} \exp[1.17(1 - P_r)] \quad F_2 = P_r^{0.645} \exp[1.42(1 - P_r)] \quad F_3 = P_r^{0.485} \exp[1.24(1 - P_r)]\]

Geometry parameters for triangular shape

\(Q_1 = 1.329, \quad Q_2 = 1.372, \quad Q_3 = -1.0, \quad Q_4 = 12.26\)

Grid spacer parameter

\(V = 0.7\) for PWR grid spacer with mixing vane

(2) **KfK correlation**\(^{(9)}\)

\[
q_v(10^9 \text{Btu/h·ft}^2) = \frac{A + B\Delta H_i}{C + Z Y Y'}
\]

The parameters are the same as those of WSC-2 correlation except the geometry parameters and the grid spacer parameter as follows:

Geometry parameters

\(Q_1 = 1.748, \quad Q_2 = 7.540, \quad Q_4 = 8.783\)

Grid spacer parameter

\(V = -0.252 - 2.789 \exp(-3.874G) + 1.915 \exp(-0.234G)\)

(3) **EPRI-B&W correlation**\(^{(1)}\)

\[
q_v(10^9 \text{Btu/h·ft}^2) = A_1 \left[ \frac{DG}{h_x - h_1} \frac{\partial h}{\partial z} \right] + A_2 \left[ \frac{DG}{h_x} \frac{\partial h}{\partial z} \right]
\]

\[
A_1 (\Delta H_i) + \frac{\partial h}{\partial P} (2,000 - P)
\]

where \(D\): Heated equivalent diameter (in.), \(G\): Mass velocity (10^6 lb/h·ft^2)

\(h_x, h_1\): Local and inlet enthalpy (Btu/lb)

\(\frac{\partial h}{\partial z}\): Local enthalpy gradient (Btu/lb·in.)

\(\Delta H_i\): Inlet subcooling (Btu/lb), \(P\): Pressure (psia)

\(\frac{\partial h}{\partial P}\): Slope of saturated liquid enthalpy curve=0.123

\(A_1 \sim A_2\): Constants

\(A_1 = 2.8591, \quad A_2 = 0.31796, \quad A_3 = 0.023018, \quad A_4 = 0.63960, \quad A_5 = 1.2614\)

(4) **EPRI-Columbia correlation**\(^{(11)}\)

\[
q_v(10^9 \text{Btu/h·ft}^2) = \frac{A - X_{1a}}{C_F C_{nu} + \frac{1}{q_1}}
\]

\[
A = P_1 P_2 P_3 G (P_4 + P_5 P_6)
\]

\[
q_1\): Local heat flux (10^6 Btu/h·ft^2), \quad X_{1a}, X_1\): Inlet and local qualities

\(G\): Mass velocity (10^6 lb/h·ft^2), \(P_r\): Reduced pressure (P/P_{critical})

\(P_r\): Constants

\(P_1 = 0.5328, \quad P_2 = 0.1212, \quad P_3 = 1.6151, \quad P_4 = 1.4066, \quad P_5 = 0.3040, \quad P_6 = 0.4843, \quad P_7 = -0.3285, \quad P_8 = -2.0749\)

\(F_g\): Grid spacer factor=1.3 - 0.3C_g

\(C_g\): Grid loss coefficient

\(C_{nu}\): Non-uniform heat flux factor=1+(Y - 1) / (1 + G)

\(Y\): Axial heat flux profile parameter defined in WSC-2 correlation
CHF, while other input parameters such as cross flow resistance, subcooled void model, bulk void model and axial noding have little effect on the CHF prediction.

The turbulent mixing coefficient in the COBRA-IV-I code is defined by
\[ \beta = \frac{W}{SG}, \]  
where \( \beta \): Turbulent mixing coefficient
\( W \): Fluctuating cross flow per unit length (kg/s·m)
\( S \): Gap width (m)
\( G \): Average axial mass velocity (kg/s·m²)

Since the mixing characteristics depend on the rod bundle geometry, thermal mixing experiments were performed under single-phase water flow condition at an atmospheric pressure to obtain mixing coefficients for CHF experiments. Test sections consisting of 36 rods with different \( p/d \)'s were also used in the mixing experiments to obtain the mixing coefficient for a large scale rod array. In the experiments one rod was electrically heated while the other rods were unheated. The temperature rises of water from the inlet to the exit were measured at various subchannels. The COBRA-IV-I calculations with different mixing coefficients were performed using the boundary conditions of the experiments to find the best-estimate coefficient that predicts the measured temperature rises satisfactory. The mixing coefficients obtained for each test section are summarized in Table 3. It is noted that the obtained mixing coefficients are much smaller than the mixing coefficient of 0.02 for a current 17×17 PWR fuel assembly without mixing vanes due to the reduced gap width between rods.

Table 3 Mixing coefficients for 4, 7 and 36 rods experiments

<table>
<thead>
<tr>
<th>Number of rods</th>
<th>Pitch (mm)</th>
<th>Mixing coefficient</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>11.4</td>
<td>0.004</td>
</tr>
<tr>
<td>7</td>
<td>10.7</td>
<td>0.003</td>
</tr>
<tr>
<td>7</td>
<td>11.4</td>
<td>0.003</td>
</tr>
<tr>
<td>36</td>
<td>11.4</td>
<td>0.001</td>
</tr>
<tr>
<td>36</td>
<td>12.6</td>
<td>0.001</td>
</tr>
</tbody>
</table>

The flow mixing among subchannels is expected to be more enhanced in two-phase flow condition. Since mixing coefficients under two-phase flow have not been available for the HCLWR core configuration, an empirical correlation was introduced as a function of void fraction based on Sadatomi's two-phase mixing experimental data\(^\text{[13]}\). The geometry of test section and experimental conditions are shown in Table 4. The ranges of gap width, mass velocity and void fraction include the present HCLWR conditions, while the geometry, pressure and working fluid are not consistent with the HCLWR conditions.

Table 4 Test conditions of Sadatomi's two-phase mixing experiments\(^\text{[13]}\)

<table>
<thead>
<tr>
<th>Test section geometry</th>
</tr>
</thead>
<tbody>
<tr>
<td>(1) Semi-circular subchannel (Ch. 1-J)</td>
</tr>
<tr>
<td>Gap width : 1.1 mm</td>
</tr>
<tr>
<td>(2) Rectangular subchannels (Ch. A-B)</td>
</tr>
<tr>
<td>Gap width : 2, 4, 6 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Test conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid : Air-water two-phase flow</td>
</tr>
<tr>
<td>Pressure : 0.11~0.13 MPa</td>
</tr>
<tr>
<td>Superficial water velocity : 0.1~2.0 m/s</td>
</tr>
<tr>
<td>Superficial air velocity : 0.3~27 m/s</td>
</tr>
<tr>
<td>Inlet temperature : 10~26°C</td>
</tr>
<tr>
<td>Average void fraction : 0~0.89 (Ch. 1-J)</td>
</tr>
</tbody>
</table>

The empirical correlation is given by
\[ \beta / \beta_0 = 1.0 \quad (0.0<\alpha<0.15) \]  
= interpolation \( (0.15<\alpha<0.3) \)  
= 4.0 \( (0.3<\alpha<0.8) \)  
= interpolation \( (0.8<\alpha<1.0) \)  
= 1.0 \( (\alpha=1.0) \),
where \( \beta \): Mixing coefficient under two-phase flow
\( \beta_0 \): Mixing coefficient under single-phase flow
\( \alpha \): Void fraction.

4. CHF Experiments

The JAERI performed steady-state CHF experiments\(^\text{[14]}\) using 4 test sections arranged in a triangular lattice. The configurations and major specifications of the test sections are shown in Fig. 3 and Table 5, respectively.
The simulated fuel rod was a stainless steel tube and uniformly heated electrically with direct current. The heated rod bundles were placed in a ceramic flow shroud for electrical insulation. The heat flux of the center rod of the 7-rod test section was about 20% higher than the average heat flux so as to force the first CHF detection at the center rod which was surrounded by other heater rods. On the other hand, the transverse heat flux distribution was flat in the 4-rod bundles. The rod bundles were supported by grid type spacers. The interval of grid spacer was also shown in Fig. 3. Thermocouples (Chromel-Alumel, 0.5 mm O.D.) were installed at the heater rods. The heating power was automatically shut
off immediately after the first detection of temperature excursion which indicates the onset of DNB.

The experimental ranges are shown in Table 6.

### Table 6 Ranges of CHF experiments at JAERI

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pressure</td>
<td>3.93 MPa</td>
</tr>
<tr>
<td>Inlet subcooling</td>
<td>20~120 K</td>
</tr>
<tr>
<td>Inlet mass velocity</td>
<td>460~4,270 kg/s-m²</td>
</tr>
<tr>
<td>Bundle-averaged exit quality</td>
<td>0.02~0.35</td>
</tr>
<tr>
<td>Number of data</td>
<td>251</td>
</tr>
</tbody>
</table>

5. Comparison of JAERI CHF Data with Correlations

The CHF data are compared with the WSC-2, KfK, EPRI-B&W and EPRI-Columbia correlations. The averages and standard deviations of predicted-to-measured CHF ratios for each test section are shown in Table 7. Note that a subchannel imbalance factor proposed by Bowring(8) was used in the WSC-2 and KfK correlations to account for interchannel enthalpy mixing effect. The results indicate that the KfK correlation shows most excellent agreement with the JAERI's CHF data than the other three correlations.

### Table 7 Predicted-to-measured CHF ratios for 4 and 7 rods experiments at JAERI

<table>
<thead>
<tr>
<th>Test section</th>
<th>CHF correlations</th>
<th>Number of data</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>WSC-2</td>
<td>KfK</td>
</tr>
<tr>
<td>A</td>
<td>1.334±0.057</td>
<td>0.867±0.045</td>
</tr>
<tr>
<td>B</td>
<td>1.513±0.048</td>
<td>0.976±0.084</td>
</tr>
<tr>
<td>C</td>
<td>1.249±0.070</td>
<td>0.963±0.104</td>
</tr>
<tr>
<td>D</td>
<td>1.292±0.052</td>
<td>0.830±0.074</td>
</tr>
<tr>
<td>Average</td>
<td>1.360</td>
<td>0.904</td>
</tr>
</tbody>
</table>

6. Comparison of Bettis Atomic Power Laboratory CHF Data with Correlations

The maximum pressure and number of rods in the CHF experiments at JAERI were limited to 3.9 MPa and 7, respectively. In order to evaluate the predictive capability of the CHF correlations at higher pressure and larger rod array than the present experiments, CHF data obtained at Bettis Atomic Power Laboratory(15) within the framework of the light water breeder reactor development program were also used for comparisons. In the Bettis experiments, 20-rod test sections arranged in a 5x4 triangular pitched array were used. The cross section is shown in Fig. 4. The geometrical data of the test sections and experimental ranges are shown in Tables 8 and 9, respectively. In the Bettis experiments, CHF data were obtained from 14 different test sections with various geometries and heat flux distributions at pressures ranging 2.75~13.8 MPa. In the present study, only the CHF data obtained from the test sections with the uniform axial and radial heat flux distributions at 13.8 MPa were used.

![Fig. 4 Configuration of CHF test section at Bettis Atomic Power Laboratory](image)
ences in mass velocity and enthalpy among subchannels in the large scale rod array with uniform radial heat flux distribution are so small that the effect of subchannel analysis on CHF prediction is considered to be negligible. It should also be noted that a part of the Bettis CHF data base were used to optimize the empirical parameters in the KfK correlation and the EPRI-B&W correlation together with other CHF data sources.

### Table 10 Predicted-to-measured CHF ratios for 20 rods experiments at Bettis Atomic Power Laboratory

<table>
<thead>
<tr>
<th>Test section</th>
<th>CHF correlations</th>
<th>Predicted CHF with correlation</th>
<th>Measured CHF</th>
<th>Number of data</th>
</tr>
</thead>
<tbody>
<tr>
<td>A - 5</td>
<td>WSC-2</td>
<td>0.863±0.124</td>
<td>0.902±0.041</td>
<td>1.046±0.043</td>
</tr>
<tr>
<td>A - 6</td>
<td>WSC-2</td>
<td>1.034±0.177</td>
<td>0.972±0.061</td>
<td>1.027±0.096</td>
</tr>
<tr>
<td>A - 7</td>
<td>WSC-2</td>
<td>1.163±0.198</td>
<td>1.088±0.114</td>
<td>1.169±0.121</td>
</tr>
<tr>
<td>A - 10</td>
<td>WSC-2</td>
<td>1.020±0.175</td>
<td>1.008±0.070</td>
<td>1.124±0.112</td>
</tr>
<tr>
<td>A - 11</td>
<td>WSC-2</td>
<td>1.212±0.254</td>
<td>1.143±0.126</td>
<td>1.258±0.142</td>
</tr>
<tr>
<td>A - 13</td>
<td>WSC-2</td>
<td>1.023±0.166</td>
<td>1.028±0.084</td>
<td>1.151±0.110</td>
</tr>
<tr>
<td>A - 14</td>
<td>WSC-2</td>
<td>0.906±0.135</td>
<td>0.907±0.056</td>
<td>1.046±0.102</td>
</tr>
<tr>
<td>Average</td>
<td>WSC-2</td>
<td>1.053</td>
<td>1.022</td>
<td>1.130</td>
</tr>
</tbody>
</table>

Since the predictive capability of CHF correlations depends on the test section geometry and test conditions, the trends of predicted-to-measured CHF ratios are different for each correlation and data base. It is suggested from Tables 7 and 10 that among these four correlations the KfK correlation has the most promising features for the application to the triangular tight lattice rod array.

### III. MDNBR CRITERION FOR HCLWR

The DNBR is defined as the ratio between the CHF predicted by an applicable correlation and the local heat flux of a fuel rod. In order to maintain an adequate safety margin regarding DNB, the DNBR in every part of the core should be larger than a minimum allowable DNBR criterion which gives a 95% probability
at the 95% confidence level that no fuel rod in the core experiences DNB.

The DNBR criterion has been one of the major limitations on the thermal output of an LWR. The minimum DNBR (MDNBR) criterion in steady-state and operational transient conditions for a current PWR design is 1.30 with the W-3 correlation\(^{(16)}\). Since the MDNBR depends on the correlation and referred CHF data base, the MDNBR should be newly determined for the HCLWR core configuration.

The MDNBR is given by

\[
\text{MDNBR} = \frac{1}{(x - ks)},
\]

where

- \(x\) : Average of measured-to-predicted DNB ratios (M/P's)
- \(s\) : Standard deviation of M/P's
- \(k\) : Factor for one-sided tolerance limit\(^{(17)}\).

Based on the Bettis CHF data and the KfK correlation, the MDNBR for the HCLWR was determined to be 1.28. Note that this MDNBR criterion should be regarded as tentative because the KfK correlation has not been verified at full pressure with the similar geometry as the present HCLWR design. It is also noted that the MDNBR of 1.28 is approximately equal to the MDNBR criterion of 1.30 of a current PWR.

IV. DNBR ANALYSES FOR DOUBLE-FLAT-CORE TYPE HCLWR

1. DNBR Analysis under Steady-state Operational Condition

The DNBR for the present HCLWR design was obtained by combining the KfK correlation with the subchannel code COBRA-IV-I. The effect of non-uniform axial heat flux profile including the internal blanket and lower core was taken into account in the KfK correlation by using the \(Y\)-factor defined in Table 2.

The whole core was represented by the 1/12 sector due to the symmetrical configuration of the core. Figure 5 shows the radial noding schematics and radial peaking factors. The radial peaking factors were based on neutronic calculations\(^{(18)}\). The engineering hot channel factor of 1.03 and the nuclear uncertainty factor of 1.05 were then taken into accounts. As a result, the radial peaking factor of the hot rod is determined to be 1.607.
heater rods, one of which is the hot rod. Subchannel 2 is named a typical cell that is surrounded by only heater rods including the hot rod. The enthalpy rise is larger in the typical cell due to the larger heat input and the mass velocity is lower in the thimble cell due to the smaller equivalent diameter. Therefore, the DNBR becomes minimum in either of these two cells depending on subchannel calculation results.

Figure 6 shows axial noding, grid spacer locations and axial power distribution. The axial power distribution was assumed to be chopped cosine in the core parts and constant in the blanket parts. The maximum axial peaking factor was also determined by the neutronic calculations\(^\text{18}\).

As conservative assumptions with respect to DNBR, following boundary conditions were used in the COBRA-IV-I calculation:

- Initial core power: \(102\%\) of nominal power
- Initial core inlet temperature: Nominal temperature +2.2 K
- Initial system pressure: Nominal pressure -0.2 MPa

Flow rate in the core:

95\% of the total flow rate.

The mixing coefficient was assumed to be 0.0 in the present calculation, because the smaller mixing coefficient results in the higher enthalpy rise in the hot subchannel resulting in the lower DNBR. The conservativeness of this assumption was confirmed by a sensitivity study, which indicated that the MDNBR was increased by 0.24\% as increasing the mixing coefficient from 0.0 to 0.001, which is an experimentally obtained value using a 36-rod bundle (Table 3).

Figure 7 shows the DNBR vs. elevation for the typical and thimble cells. Here only the upper core is shown because the DNBR is lower in the upper core than in the lower core. The DNBR becomes minimum at 1.7 m from the bottom of the lower blanket in the typical cell. The value of MDNBR is 1.66 which is much higher than the MDNBR criterion of 1.28, indicating that an enough safety margin is assured under the steady-state operational condition.

2. DNBR Analysis under Accident Conditions

In order to evaluate the safety margin of the present design under accident conditions, transient DNBR analyses were performed. Among various accident conditions, the primary coolant pump trip accident and the blocked rotor accident were selected because these two accidents were considered to be the most severe cases from the DNBR point of view.

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Fig. 6 Axial noding model, grid spacer location and axial peaking factors

Fig. 7 Axial distribution of DNBR under steady-state operational condition
The primary system transient calculations were performed with a best-estimate code J-TRAC\textsuperscript{(20)}, which is based on TRAC-PF1 code\textsuperscript{(21)} and incorporates some thermal-hydraulic models developed at JAERI.

Figures 8 and 9 show the noding schematics of the primary loops and the pressure vessel, respectively. The pressure vessel is three-dimensionally divided into 6 azimuthal, 4 radial and 25 axial nodes. The same conservative assumptions were made for the initial conditions as in the steady-state COBRA-IV-I calculation. The reactivity coefficients with respect to fuel temperature, coolant temperature and coolant void were also determined by the neutronic calculations\textsuperscript{(18)}.

Figure 10(a) and (b) show the J-TRAC calculation results of the core inlet flow rate, reactor power, core inlet temperature and system pressure for the pump trip accident and for the locked rotor accident, respectively. In the pump trip accident, the core inlet flow rate is slowly reduced along the free rotation with the flywheel followed by the rapid reduction of reactor power at 1.6 s due to the reactor trip signal of lower pump rotation speed (92.6\% of nominal speed). In the locked rotor accident, on the other hand, the core inlet flow rate is rapidly reduced to 74\% of the nominal flow rate within 1 s and gradually reduced to 65\% in 10 s. In the present analy-
sis the increase of flow rate at the other two intact pumps due to the reduction of core inlet flow velocity was not considered. The reactor power was reduced to 15\% of the nominal value at 1.1 s due to the reactor trip signal activated by low primary coolant flow rate (87\% of nominal flow rate). The core inlet water temperatures are approximately constant during the transients for these two accidents. The maximum pressures are 15.9 and 16.4 MPa for the pump trip accident and the locked rotor accident, respectively, causing no problems for the intactness of the pressure boundary.

In order to obtain the local flow parameters, the time variations in flow rate, core inlet temperature, system pressure and reactor power were inputted to the COBRA-IV-I code and then the KfK correlation was used to determine the DNBR. The parameters in the COBRA-IV-I calculations were the same as those used in the steady-state calculation except that the number of axial nodes was reduced to 21 in the transient calculations. It should be noted that the change of fuel rod surface heat flux is delayed behind the change of reactor power under rapid transients due to the fuel rod thermal resistance. Therefore,
the fuel rod model contained in the COBRA-IV-I was used to determine the surface heat flux in the transient subchannel calculations. The variations of DNBRs at elevations of 1.6 and 1.7 m from the bottom of the lower blanket in the typical and thimble cells are shown in Fig. 11(a) and (b) for these two accidents, respectively. The DNBR becomes minimum at 1.7 m in the typical cell for both cases. The values of MDNBR are 1.56 at 2.2 s for the pump trip accident and 1.34 at 1.6 s for the locked rotor accident. These MDNBRs meet the MDNBR criterion of 1.28. Therefore, it is concluded that the present design is acceptable from a viewpoint of the DNBR concern.

![Fig. 11(a), (b) DNBR analysis results for pump trip and locked rotor accidents](image-url)
V. CONCLUSION

The DNBR analyses under operational and accident conditions for the double-flat-core type HCLWR have been performed to evaluate the thermal-hydraulic feasibility of the present design. Major conclusions are as follows:

(1) The WSC-2, KfK, EPRI-B&W and EPRI-Columbia correlations have been evaluated against the CHF data obtained at JAERI and Bettis Atomic Power Laboratory. It was found that the KfK correlation has the most promising features for the application to the triangular tight lattice rod array. In the JAERI’s experiments, the local flow conditions for the CHF correlations were obtained by the subchannel code COBRA-IV-I using the experimentally determined mixing coefficients.

(2) Based on the traditional criterion that no fuel rod in the core experiences DNB with 95% probability at 95% confidence level, the minimum allowable DNBR for the HCLWR was determined to be 1.28 by comparing the Bettis CHF data with the KfK correlation.

(3) The best-estimate code J-TRAC was used for the system transient calculations under the primary coolant pump trip and locked rotor accident conditions. The COBRA-IV-I code was then used to obtain local flow conditions and fuel rod surface heat flux. The analytical results indicated that an enough safety margin was assured under the steady-state operational condition. Under the accident conditions, the evaluated minimum DNBRs were also above the MDNBR criterion. Therefore, it was clarified that the present HCLWR design is feasible from a view point of the MDNBR criterion.

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