1. Introduction

At present a WWER-1000 reactor core may contain more than one fuel assembly types having different hydraulic resistances. The substantiation of a thermal-hydraulic (T/H) compatibility and a thermal-mechanical reliability of a new fuel assembly type in a mixed core is part of the safety evaluation part for fuel reloads.

T/H compatibility of the new fuel assembly type with the existing fuel in a mixed core becomes more important when the loaded fuel assemblies are being designed and supplied by different vendors.

Westinghouse Electric Company (Westinghouse) has developed a comprehensive thermal-hydraulic analysis methodology approved by the United States Nuclear Regulatory Commission (USNRC) and other international licensing authorities for PWR mixed cores containing square-lattice fuel assemblies with different hydraulic resistances [1, 2]. It should be stressed that Westinghouse has been successfully using this methodology for PWR mixed cores containing fuels with the different skeleton design, as well as different number of grids and grid designs that had relatively-large differences in hydraulic resistances. Also, the mentioned T/H methodology is used by Westinghouse for mixed cores with fuels from the different vendors.

Within the framework of the Ukrainian Nuclear Fuel Qualification Program, the Westinghouse T/H analysis methodology for PWR mixed cores has been extended to mixed cores of WWER-1000 reactor to account for loading pattern specifics and core parameter variations at different operating and accident conditions and non-LOCA transients. The proposed procedure of thermal-hydraulic compatibility of new fuel assembly type in a WWER-1000 mixed cores has been successfully applied in the safety substantiation for the use of six Westinghouse Lead Test Assemblies (WLTAs) in the South Ukraine Nuclear Power Plant Unit 3 (SU NPP-3) having a WWER-1000 (V-320) reactor, without significant restrictions on core reload designs or operation.

The objective of this paper is to present the T/H design methodology for evaluating fuel power margin in a WWER-1000 mixed core containing two fuel assembly types. The major T/H analysis results for the six WLTAs loaded in the SU NPP-3 core containing the existing Russian TVS-M fuels are also shown and discussed.

2. Thermal-Hydraulic Analysis Methodology for Mixed Core

2.1. Determination of Transition Core DNB Effects

It is well known that the redistribution of coolant flow in pressurized light water-cooled nuclear reactor cores (PWR and WWER) occurs due to variations of thermal-hydraulic fluid conditions and power distributions within the core. In a mixed core, fuel assemblies with different local and possibly total hydraulic resistances can result in additional crossflow due to the open lattice nature of the core. The local crossflows will in turn cause local differences in axial coolant flow. Changes in local axial flow rate will cause changes in local enthalpy and in local Departure from Nucleate Boiling Ratios (DNBR, critical to actual heat flux),
since the DNBR correlations are functions of local mass velocity and local quality (enthalpy).

According to the safety analysis requirements for WWER and PWR reactors [3-6], the plant design DNBR limit must not be violated by the DNBRs evaluated on the most limiting fuel rod located in the most hot channel of fuel assembly for normal operation, anticipated operational transients and any transient conditions arising from faults of moderate frequency (Condition I and II events).

Based on this requirement, the T/H safety analysis for the mixed core consists of an evaluation of potential fuel rod power reduction for each fuel type, in order to offset the reduction of DNBR due to coolant flow redistribution caused by differences in the fuel hydraulic resistances. To calculate the power reduction for limiting fuel rod, the range of operating parameters such as thermal power, system pressure, inlet coolant flow rate and temperature, and axial power distributions should bound those used in the determining of core DNBR limits. As a rule, the limiting core parameters are related to the following effects, which should be investigated for mixed cores:

- Configuration at nominal, overpower, and loss of coolant flow conditions.
- Thermal conditions over the range of representative core limit conditions.
- Axial power distributions for normal operation, operational transients and design basis accidents.

For the core conditions mentioned above and new fuel assembly type fraction in a mixed core, DNBR calculations have to be also performed for various fuel loading patterns to determine the DNBR limiting fuel assembly configuration in a reactor core. This fuel configuration is used in the final evaluation of rod power reduction for investigated fuel type and assures that the DNBR evaluated on the most limiting fuel rod will bound all of the effects given above and, as a result, will not violate the plant DNBR design criteria.

To perform the T/H analysis procedure described above, the DNBR calculations have to be fulfilled with an appropriate empirical DNB correlation and a T/H subchannel code such as the VIPRE-W code which are briefly described in the subsequent sections.

2.2. VIPRE-W Subchannel Code

The VIPRE-W code is the Westinghouse modified version of thermal-hydraulic subchannel code VIPRE-01/MOD02 [7] widely used in the nuclear industry. VIPRE-01 was developed based on the COBRA code series. Similar to other subchannel codes currently used for PWR and WWER analyses, VIPRE-01 predicts the three-dimensional velocity, pressure, and thermal energy fields and fuel rod temperatures for single- and two-phase flow in PWR, WWER and Boiling Water Reactor cores. VIPRE-01 solves the finite difference equations for mass, energy, axial and lateral momentum conservation for an interconnected array of channels, assuming incompressible and thermally expandable homogeneous coolant flow. Although the formulation is homogeneous, empirical models are incorporated into the code to account for subcooled boiling and vapor/liquid slip in two-phase flow. VIPRE-01 contains two profile fit subcooled boiling models, one developed by Levi [8] and the other one by Lellouche and Zolotar [9]. It should be noted, the only major limitation of the code is its inability to consider severe accidents involving extreme two-phase flow conditions such as blowdown and reflood typically encountered in LOCA transients.

Westinghouse has incorporated many additional features into the VIPRE-01 code for PWR and WWER design applications [10-14]. It should be noted that these features enhance VIPRE-W capability for PWR and WWER core design and licensing applications, but they do not alter the fundamental solution scheme of the VIPRE-01 code. Some of these features are listed below:
- The Bishop-Sandberg-Tong film boiling heat transfer correlation [15] which allows using the VIPRE-W code for the hot spot analysis of several ANSI Condition IV non-LOCA transients such as control rod ejection and locked rotor events.

- The Baker-Just equation [16] is used in order to account for the heat addition as a result of the zirconium-steam reaction at high temperature.

- Westinghouse mass evaporation rate model for subcooled nucleate boiling conditions. It is known that subcooled boiling in PWR and WWER can result in crud deposition and accumulation of coolant additives (for instance, boric acid) in porous crud. As shown in Ref. [13], under certain coolant chemistry environment with relatively high corrosion product concentration, a large amount of subcooled boiling could lead the axial offset anomaly, which is characterized by a neutron flux depression in the upper half of the core. New mass evaporation rate model is based on a model proposed by Bergles and Rohsenow [17] and assumes heat transfer from fuel clad surface to coolant by the following mechanism in the partial boiling regime:

\[ q_{total} = \left[ q_{fc}^2 + (q_{nb} - q_{bo})^2 \right]^{1/2}, \]  

where the forced convection heat flux \( q_{fc} \) is based on the Dittus-Boelter heat correlation [18]; the fully developed nucleate boiling heat flux \( q_{nb} \) is calculated using the Thom heat correlation [19]; the heat flux of boiling onset \( q_{bo} \) is obtained from the Rohsenow-Hartnett correlation [20]. The mass evaporated rate is determined at each node in a VIPRE-W model as

\[ m_e = \frac{(q_{nb} - q_{bo})}{h_{fg}}. \]

- DNB correlations for fuel assemblies containing Mixing Vane (MV) and Intermediate Flow Mixer (IFM) grids. The OKB (Bezrukov) DNB correlation [21] for the Russian hexagonal fuels with non-MV grids has been also installed into the VIPRE-W code.

- In order to account for the effect of grid spacer, a heat transfer correlation with the empirical coefficients proposed by Yao et al. [23] has been incorporated into the VIPRE-W code:

\[ \frac{Nu}{Nu_0} = \left[ 1 + C_1 \varepsilon^2 e^{C_2 (\varepsilon/De)} \right] \left[ 1 + A^2 \tan^2(\phi) e^{C_3 (\varepsilon/De)} \right] C_4 \]

- For setpoint analysis, the options for capability to iterate operating conditions to a given minimum DNBR have been improved.

### 2.3. VIPRE-W Modeling of WWER-1000 Core

VIPRE-W modeling of PWR and WWER-1000 core is based on the one-pass modeling technique which was first introduced by Moreno et al. [24]. In the one-pass modeling, hot channels (subchannels with the highest enthalpy rise, \( P_{\Delta \theta}^{Y} \)) and their adjacent regions are modeled in detail, while the rest of the core is lumped into several increasingly larger channels. As long as at least one full row of subchannels is placed to completely surround the hot channel to adequately resolve the details of the flow field in the vicinity of the hot channel, the hot channel flow conditions are very insensitive to the core radial layout or how the rest of the hot fuel assembly or core is modeled. In order to obtain correct crossflow for the lumped channels, parameters such as crossflow area and crossflow resistance are redefined for the VIPRE-W lateral momentum equation using the techniques described by Stewart et al. [7].

VIPRE-W one-pass modeling has been applied to pressurized light water reactor core DNB analyses and approved by the USNRC [10].
For WWER-1000 DNB analyses, a 1/6th of the core, as shown in Fig. 1, is modeled in light of the hexagonal symmetry. Figure 1 shows the VIPRE-W 30- and 57-channel models. For a WWER-1000 mixed core, the VIPRE-W 30-channel model allows comparative DNB analysis for the three cases: 1, 7 and 37 fuel assemblies of a new type in the mixed core. For the other fuel type fraction, the DNB analysis has to be performed by the VIPRE-W 57-channel model. Also, the 57-channel model is used if the DNB analysis versus the fuel assembly configuration in the mixed core is performed. As Fig. 1 shows, a hexagonal fuel is in rotational symmetry, not in reflective symmetry, due to the locations of guide thimble tubes. In order to simulate the rotational symmetry, the subchannels at the symmetrical lines of the 1/6th hot assembly are connected to each other. For instance, Channel 2 is connected to Channel 4, and Channel 5 is connected to Channel 9, and so on. In addition to a more realistic presentation of the hexagonal fuel, the subchannel connection results in more stable solutions in DNB calculations [14, 25].

The adequacy of the VIPRE-W 30-channel one-pass model described above has been confirmed through previous model sensitivity studies [26]. It was shown that the maximum DNBR change was less than 1% among the 450 state points calculated for nine core conditions with fifty axial power distributions with the use of the VIPRE-W 170-channel (whole core), 104-channel (1/6th core) and 21-channel (1/6th core) models.

Empirical correlations are used in the VIPRE-W code to model two-phase flow effects on friction pressure losses, subcooled boiling, and the relationship between the flowing quality and the void friction. The Levy’s subcooled model [8], homogenous bulk boiling model and homogeneous two-phase friction multiplier were selected for the WWER-1000 core model. The selected two-phase flow correlations give good comparisons of local fluid conditions with experimental data for PWR and WWER-1000 DNB analyses. The hot wall correction is not used for modification of the friction factor. This is usually a minor effect, since fluid viscosity is not an exceedingly strong function of temperature for water [7].

To model the heat transfer from the fuel rod wall to a liquid, VIPRE-W code contains a lot of sets of heat transfer correlations which are used in the four major segments of the boiling curve - single-phase forced convection (FCON), subcooled (SUBC) and saturated nucleate boiling (SATB), transition boiling (TRNB), and film boiling (FLMB). Based on the WWER-1000 plant operating parameters ranges, the following heat transfer correlations can be used in the VIPRE-W model [10, 13]:

- FCON segment – EPRI correlation is used.
- SUBC segments - Thom with EPRI correlation are used.
- SATB segments - Thom with EPRI correlation are used.
- The peak of boiling curve is defined by W-3 or OKB correlation.
- TRNB segment - Condie-Bengston or Bishop-Sandberg-Tong correlation is used.
- FLMB segment - Groenveld 5.7 correlation can be applied.

The VIPRE-W empirical models describing the exchange of energy and momentum between adjacent channels due to a turbulent mixing are based on the model proposed by Cadek [27]. The turbulent mixing model for WWER-1000 DNB analysis was the same as the model in the USNRC-approved THINC code for PWR DNB analysis [10]. According to Ref. [27], the turbulent crossflow per unit length, $w'$, is evaluated as follows:

$$w' = A_{mix} * S_{gap} * G_{ave},$$  

where $S_{gap}$ is the gap width between the adjacent channels, m; $G_{ave}$ is the average axial mass flux in the adjacent channels, kg/(m²*s); $A_{mix}$ is the coefficients which corresponds to the thermal diffusion coefficient. For the non-MV grid with triangular channels, the mixing coefficients for each gap between the adjacent channels are evaluated using the procedure described by Tong [28] and Bell et al. [29]. For lumped channels, which are used in the VIPRE-W 57-channel model, the $A_{mix}$ coefficient has been recalculated as follows:
Figure 1. VIPRE-W subchannel models for DNB analysis for a WWER-1000 core. The 30- and 57-channel models are shown on the left and right side of the figure, respectively. Both models represent the 1/6th WWER-1000 core and include the 1/6th Hot Assembly shown at the top of the figure. The 1/6th Hot Assembly is modeled by the 27th channels and 19th fuel rods. For the 1/6th Hot Assembly, Channel 1 is an instrumental cell, Channel 3 is a typical cell, Channel 7 is a thimble cell, and Channels 26 & 27 represent the lumped channels. For the VIPRE-W 30-channel model (see left side of the figure), Channel 28 represents a hexagonal fuel assembly and Channels 29 & 30 are the lumped ones. For the VIPRE-W 57-channel model (see right side of the figure), channels from 28th through 57th represent the hexagonal fuel assemblies with the fuel Rods from 20th through to 49th, respectively.
\[ A_{\text{mix}} \text{(lump)} = A_{\text{mix}} \ast \frac{d_s}{d_l}, \quad (6) \]

where \( d_s \) is the subchannel centroid distance, m; \( d_l \) is the centroid distance for a lumped channel, m. The centroid length is the distance between the centroids of the adjacent channels.

Pressure losses due to friction drag were calculated in the VIPRE-W code for both axial and transverse flow. The axial friction factor used in the WWER-1000 model is determined from the Blasius relation

\[ f_{\text{axial}} = a \ast \text{Re}^b + c, \quad (7) \]

where Re is the Reynolds number based on axial velocity. The code evaluates both a turbulent and laminar set of coefficients and selects the maximum, defining the friction factor as \( f_{\text{axial}} = \max\{f_{\text{axial(turb)}}, f_{\text{axial(lam)}}\} \).

Pressure drop through the gap in the transverse direction is defined as follows

\[ \frac{dP}{dy} = -0.5 \ast K_G \ast \nu' \ast |w| \ast w / S_{\text{gap}}, \quad (8) \]

where \( K_G \) is the coefficient of form drag in the gap between adjacent channels; \( \nu' \) is the specific volume for momentum, \( \text{m}^3/\text{kg} \); \( w \) is the lateral mass flow rate, \( \text{kg}/(\text{m} \ast \text{s}) \). In the WWER-1000 model, the subchannel crossflow resistance is based on a correlation for crossflow in triangular lattice [30]:

\[ K_G = C_G \ast \text{Re}^{-0.27}_L, \quad (9) \]

where \( C_G \) is the coefficient based on the pitch and rod diameter for typical cell; \( \text{Re}_L \) is the Reynolds number based on lateral velocity.

It should be noted that all empirical models selected for the safety analysis are previously approved by the USNRC.

### 2.4. Fuel Rod Power Reduction Procedure

As discussed above in Chapter 2.1, the fuel rod power reduction procedure is applied to the hottest fuel rod located in the hot channel of hot fuel assembly having the highest hydraulic resistance (limiting fuel type) among all fuel types which are loaded in the mixed reactor core. For the given fraction (\( m \)) of the limiting fuels in the mixed core, a power search for the hottest fuel rod is performed by the VIPRE-W 30- or 57-channel model for the various core operating conditions as mentioned above in Chapter 2.1.

The following plant operating parameters are used in the fuel rod power reduction procedure:

- set of the core operating conditions \( (N^0_r, P^0, T^0_{in}, V^0_{in})_I, I = 1, \ldots, M_{oc} \)
- set of the axial power distributions in each fuel assembly \( K_{k,j}(z), J = 1, \ldots, M_{ap} \)
- effective core flow fraction
- radial power distribution in each fuel assembly \( F^{N_k}_{\Delta H,k} \)
- fuel fabrication parameters \( F^{E}_{\Delta H,k} \)

The limiting power per rod \( (N^1_{r(m)}) \) is obtained by variation of an average fuel rod power \( (N^0_r) \) until the local DNBR value calculated in the hottest \((i, j, k)\) cell is adjusted to the reference DNBR value:

\[ N^1_{r(m)} = \min \left| \text{DNBR}_{\text{ref}} - \frac{q_{\text{pred}}(i, j, k)}{q_{\text{actual}}(i, j, k)} \right|_{L,J}, \quad (10) \]

where \( q_{\text{pred}} \) is the heat flux predicted by the corresponding DNB correlation function \( (f_{\text{DNB}}) \) using the local thermal-hydraulic parameters and \( q_{\text{actual}} \) is the actual local heat flux calculated by the VIPRE-W code.
For the homogeneous core containing only fuels with highest hydraulic resistance, the limiting power $N_{r(h)}^{I,J}$ (here $(h)$ index denotes the homogeneous core) for the hottest fuel rod locating in the most hot channel is obtained by the same VIPRE-W iteration procedure using the same operating conditions as for the mixed core. As the result, the power reduction for the hottest fuel rod in the limiting fuel assembly is evaluated as

$$\Delta N_{r(m)}^{I,J} = 1 - \frac{N_{r(m)}^{I,J}}{N_{r(h)}^{I,J}} \quad (11)$$

This procedure is repeated for the next axial power distribution with the unchangeable core operating parameters ($I = \text{constant}$). When all axial power shapes analyzed ($J = M_{ap}$), the next set of the core operating conditions is chosen for the new power reduction calculations as described above. It should be noted that the DNBR$_{ref}$ value in Eq. (10) is chosen, as a rule, to be equal to the correlation limit. However, the $\Delta N_{r(m)}^{I,J}$ value is not sensitive to the choice of the exact DNBR$_{ref}$ value because it is a relative value just reflecting a ratio of the $N_{r(m)}^{I,J}$ and $N_{r(h)}^{I,J}$ values, which use the same DNBR$_{ref}$ value.

In the result of these calculations, the power reduction array $\Delta N_{r(m)}^{max}(M_{ap} \ast M_{oc})$ will be obtained. The maximum value of $\Delta N_{r(m)}^{max}(0)$ selected from the $\Delta N_{r(m)}$ array corresponds to the design fuel rod power reduction for the limiting fuel type. The same procedure is performed for other fraction of the limiting fuel type in the mixed core.

The design fuel rod power reduction, as previously mentioned, depends on the plant operating parameters, nuclear and thermal parameters and fuel fabrication parameters. All of these parameters include the measured or manufactured uncertainties which potentially have an impact on the $\Delta N_{r(m)}^{max}(0)$ calculated value. Therefore, the design fuel rod power reduction should be corrected by a power reduction uncertainty factor in which all design parameters uncertainties are statistically combined with the calculation uncertainty. To evaluate this factor, the statistical variation analysis derived in Ref. [31, 32] can be applied.

Let $Y$ represents continuous population having a normal distribution with mean $\mu_Y$ and standard deviation $\sigma_Y$. It is well known [31] that a random value from the $Y$ population is bounded by the Upper 95% Tolerance Limit (UTL) expressed as follows:

$$\text{UTL} = \mu_Y \ast [1 + 1.645 \left( \frac{\sigma_Y}{\mu_Y} \right)], \quad (12)$$

where the ratio $\sigma_Y/\mu_Y$ is called the coefficient of $Y$-variation.

Let us assume that $Y$ depends on the ($X_1, X_2, \ldots, X_n$) independent design parameters, each having a normal distribution with mean $\mu_{X_i}$ and standard deviation $\sigma_{X_i}$. In order to relate the $Y$-variation coefficient with the coefficients of $\sigma_{X_i}/\mu_{X_i}$ variation for all independent parameters, it is necessary to obtain a relationship between $Y$ and the uncertainties $\delta_{X_i}$ in the design parameters.

Consider the $\bar{Y}$ value defined by the $\bar{X}_i$ parameters, each having a small deviation around the mean $\mu_{X_i}$ value, i.e. $\bar{X}_i = \mu_{X_i} \pm \delta_{X_i}$. If $\bar{Y}$ is expanded in a Taylor’s series about the $\mu_{X_i}$ the following expression is obtained

$$Y - \mu_Y = \sum_{i=1}^{n} \frac{\partial Y}{\partial X_i} (X_i - \mu_{X_i}) + \sum_{i,j=1}^{n} \frac{\partial^2 Y}{\partial X_i \partial X_j} (X_i - \mu_{X_i}) (X_j - \mu_{X_j}) + \text{higher order terms} \quad (13)$$
As mentioned above, the perturbations from the mean values are small, therefore the second and higher order terms in Eq. (13) will be considerably smaller in magnitude that the first order term and as a result can be ignored, i.e.

\[ Y - \mu_Y = \sum_{i=1}^{n} (X_i - \mu_{X_i}) \left( \frac{\partial Y}{\partial X_i} \right)_{X_i=\mu_{X_i}} \]  

(14)

The partial derivatives in Eq. (14) are evaluated at the point where all the \( X_i \) are at their mean values \( \mu_{X_i} \). The value of \( Y \) at this point is represented by \( \mu_Y \). Under these conditions, the variance of \( Y \), as shown in Ref. [31], is determined by the following expression:

\[ \sigma_Y^2 = \sum_{i=1}^{n} \left( \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^2 \]  

(15)

If Eq. (15) is divided by \( \mu_Y^2 \) and then rearranged as follows

\[ \left( \frac{\sigma_Y}{\mu_Y} \right)^2 = \sum_{i=1}^{n} \left( \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^2 = \sum_{i=1}^{n} \left( \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^2 \left( \frac{\partial Y}{\partial X_i} \right)_{X_i=\mu_{X_i}}^2 = \sum_{i=1}^{n} \left( \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^2 S_{X_i}^2, \]  

(16)

then the 95% UTL for \( Y \) can be rewritten as

\[ \text{UTL} = \mu_Y * \left[ 1 + 1.645 \sum_{i=1}^{n} S_{X_i}^2 \left( \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^2 \right]. \]  

(17)

The factor \( S_{X_i} \), which has been introduced in Eq. (16), represents the sensitivity factor associated with the \( X_i \) design parameter. If all the parameters in Eq. (16) are held constant except for one, then it is obviously that if the \( X_i \) are independent

\[ S_{X_i} = \frac{\left( \frac{\partial Y}{\partial X_i} \right)_{X_i=\mu_{X_i}}}{\frac{\partial \ln(Y)}{\partial \ln(X_i)}} \equiv \frac{\partial \ln(Y)}{\partial \ln(X_i)} \]  

(18)

The value of \( S_{X_i} \) can be interpreted as representing the percentage change in \( Y \) factor from a one percent change in \( X_i \), all other parameters being held constant. For practical purposes, the sensitivity factors are calculated using the modified form of Eq. (18):

\[ S_{X_i} = \frac{\partial \ln(Y)}{\partial \ln(X_i)} = \frac{\ln(Y_1) - \ln(Y_2)}{\ln(X_{i,1}) - \ln(X_{i,2})} = \frac{\ln \left( \frac{Y_1}{Y_2} \right)}{\ln \left( \frac{X_{i,1}}{X_{i,2}} \right)} = \frac{\ln \left( \frac{Y_2}{Y_1} \right)}{\ln \left( \frac{X_{i,1} \pm \delta_{X_i}}{X_{i,1}} \right)}, \]  

(19)

where the index 1 denotes the \( Y_1 \) value calculated at the \( X_{i,1} \) parameter value, while the index 2 denotes the \( Y_2 \) value calculated at the \( X_{i,2} \) parameter value which has a small deviation \( \delta_{X_i} \) around the \( X_{i,1} \) value.
Thus if the sensitivity factors defined by equation (18), as well as the mean and standard
deviation of the probability distribution are known for each of the design parameters $X_i$, the
95% UTL for the $Y$ factor can be determined.

The central limit theorem of statistics indicates that the probability distribution function
for $Y$ will approach a normal distribution with mean $\mu_Y$ and standard deviation $\sigma_Y$ even if the
individual distributions of the $X_i$ are not normal. It should be noted that Eq. (17) is subject to
the restrictions that the $X_i$ are independently distributed and that the variations in the $X_i$ can
be considered small. In addition the sensitivity factors $S_{X_i}$ are considered to be constant,
thus independent of the $X_i$.

Based on the statistical analysis described above, the corrected design fuel rod power
reduction for the limiting fuels having the $m$-fraction in the mixed core can be expressed as
follows

$$
\Delta N_{r(m)} = \Delta N_{r(m)}^{\text{max}}(0) * \left(1 + 1,645 \sum_{i=1}^{n} \frac{\sigma_{X_i}}{\mu_{X_i}} \right)^{2} \left( \frac{\ln \left( \frac{\Delta N_{r(m)}^{\text{max}}(\delta)}{\Delta N_{r(m)}^{\text{max}}(0)} \right) \right)^{2},
$$

(20)

where $\Delta N_{r(m)}^{\text{max}}(0)$ is the maximum value of design fuel rod power reduction calculated at the
design plant operating parameters $(X_1^0, X_2^0, ..., X_n^0)$, i.e. $\Delta N_{r(m)}^{\text{max}}(0) = \Delta N_{r(m)}^{\text{max}}(X_1^0, X_2^0, ..., X_n^0)$;
$\Delta N_{r(m)}^{\text{max}}(\delta)$ is the fuel rod power reduction calculated at the $(X_i^0 \pm \delta_{X_i})$ parameter when
other design plant operating parameters being held constant.

The maximum acceptable fuel rod power in the fuel assembly with the highest hydraulic
resistance in the mixed core will
ensure that there is at least a 95% probability that DNB will not occur on the limiting fuel
rod during normal operation, operational transients, and any transient conditions arising from
faults of moderate frequency at a 95% confidence level.

3. Power Reduction Procedure for 6 WLTAs in WWER-1000 Mixed Core with
Russian TVS-M Fuel Assemblies

The described T/H power reduction procedure was applied to the six WLTAs loaded in
the SUNPP-3 core with the existing Russian TVS-M fuels.

VIPRE-W 57-channel model discussed above has been selected for the fuel rod power
reduction calculations. For the WLTAs and TVS-M fuel assembly, the channel geometric
characteristics, such as a total flow area, heated and wetted perimeters, gap width and
centroid distance between adjacent channels, have been calculated in compliance with the
VIPRE-W code requirements [7]. Table 1 shows the comparative analysis of design
characteristics of fuel assemblies.
### Comparative characteristics of WLTA and TVS-M

<table>
<thead>
<tr>
<th>Component</th>
<th>Russian TVS-M Design</th>
<th>Westinghouse LTA Design</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel Rod Outer Diameter OD&lt;sub&gt;FR&lt;/sub&gt;</td>
<td>1,005</td>
<td>1,005</td>
</tr>
<tr>
<td>Guide Thimble Tube Outer Diameter OD&lt;sub&gt;Th&lt;/sub&gt;</td>
<td>1,000</td>
<td>1,125</td>
</tr>
<tr>
<td>Instrumentation Tube Outer Diameter OD&lt;sub&gt;Ins&lt;/sub&gt;</td>
<td>1,125</td>
<td>1,125</td>
</tr>
<tr>
<td>Number of Mid-Grids</td>
<td>15</td>
<td>15&lt;sup&gt;(1)&lt;/sup&gt;</td>
</tr>
<tr>
<td>Number of Bottom Grid</td>
<td>-</td>
<td>1&lt;sup&gt;(2)&lt;/sup&gt;</td>
</tr>
<tr>
<td>Total Number of Grids</td>
<td>15</td>
<td>16</td>
</tr>
<tr>
<td>Bottom Nozzle Loss Coefficient K&lt;sub&gt;BN&lt;/sub&gt;</td>
<td>0,68</td>
<td>K&lt;sub&gt;BN&lt;/sub&gt;</td>
</tr>
<tr>
<td>Top Nozzle Loss Coefficient K&lt;sub&gt;TN&lt;/sub&gt;</td>
<td>0,37</td>
<td>K&lt;sub&gt;TN&lt;/sub&gt;</td>
</tr>
<tr>
<td>Mid-Grid Loss Coefficient K&lt;sub&gt;M-GR&lt;/sub&gt;</td>
<td>1,29</td>
<td>K&lt;sub&gt;M-GR&lt;/sub&gt;</td>
</tr>
<tr>
<td>Bottom Grid Loss Coefficient K&lt;sub&gt;B-GR&lt;/sub&gt;</td>
<td>1,45</td>
<td>K&lt;sub&gt;M-GR&lt;/sub&gt;</td>
</tr>
</tbody>
</table>

<sup>(1)</sup> The axial locations of WLTA grids coincide with the axial locations of TVS-M grids
<sup>(2)</sup> WLTA bottom grid is in line with the TVS-M bottom nozzle plate

In order to provide a detailed resolution of flow redistribution due to geometric and hydraulic changes in the mixed core, an axial nodal length is about of 30 mm was used in VIPRE-W model. Also, the chosen axial nodal length provides an accurate calculation of the DNB axial location. The heated length of fuel rod has been reduced to account for effect of fuel densification, which is in compliance with the USNRC-approved T/H methodology for pressurized light-water reactor safety analyses.

With respect to the Westinghouse Revised Thermal Design Procedure DNB methodology, which is approved by the USNRC [33], the WLTA hot assembly including the most hot channels is located in the center of core (see Fig. 1 and 2). The average power of hot assembly is assumed to be equal to \( F_{\Delta h}^N (\text{Hot Assembly}) = F_{\Delta h}^N (\text{limit})/1,08 \), where the numerical factor of 1,08 represents a sum of the measurement uncertainty of 4% and the calculated uncertainty of 4% [33]; \( F_{\Delta h}^N (\text{limit}) \) power factor is the technical specification limit for a fuel rod. In present calculations, the technical specification limit of 1,70 has been taken from Ref. [34]. It should be noted that the \( F_{\Delta h}^N (\text{limit}) \) value is not a critical parameter in the fuel rod power reduction calculations because the \( \Delta N_{r(lim)} \) value, as discussed in the previous chapter, is not sensitive to the \( F_{\Delta h, k}^N \) values.

Radial rod power distribution inside the WLTA hot assembly is approximated by the flat power distribution with the peak-to-average factor is about 1,05. The power factor for the first five fuel rods (see Fig. 1) was taken as \( F_{\Delta h}^N (\text{design}) = F_{\Delta h}^N (\text{limit})/1,04 \), where the numerical factor of 1,04 reflects the “measurement” uncertainty of 4%. It should be pointed that the flat power distribution provides a gradual power gradient with peaks around the hot channels to reduce the benefit of crossflow into the hot channels.

For the remained fuel assemblies, see Rods 21-49 in Fig. 1, lower power factors are assigned and the power factors are normalized to unity on a core-wide basis.

A five percent coolant flow reduction to WLTA hot assembly and the adjacent fuel assemblies has been taken in order to provide more conservative DNB analyses. This assumption is consistent with the USNRC-approved T/H methodology for safety analysis of a pressurized light-water reactor. The assumption of 5% flow reduction is also consistent with the original WWER-1000 design basis [35].

The \( \lambda_{\text{mix}} \) coefficient in Eq. (5), which is used for turbulent crossflow, was evaluated as 0,009 based on the relations from Ref. [28, 29].
Figure 2. VIPRE-W 57-model for DNB analysis for the following WWER-1000 mixed cores: left picture represents the core with 1 WLTA and 162 TVS-M and right picture represents the core with 7 WLTA and 156 TVS-M. The WLTA is marked by blue color and the Russian TVS-M fuel assemblies are marked by yellow color. The WLTA hot fuel assembly with the most hot fuel rods is located in the center of the core and marked by red band.

Figure 3. The SU NPP-3 mixed core with the 6 WLTA and the Russian 157 TVS-M fuel assemblies. The WLTA are marked by blue color.
The \{a; b; c\} coefficients in Eq. (7) for the turbulent and laminar coolant flow have been taken from Ref. [20]: \(a_{turb}; b_{turb}; c_{turb}\) =\{0,184; -0,20; 0,0\} and \(a_{lam}; b_{lam}; c_{lam}\) =\{64,0; -1,0; 0,0\}. The crossflow resistance coefficient \(C_G\) in Eq. (9) has been evaluated as 3.97 based on the pitch and rod diameter for typical cell.

Because the non-mixing vane grids are used in WLTAs, both W-3 and OKB DNB correlations with the reference DNB values of 1.3 [22] and 1.15 [21] have been used. The W-3 and OKB DNB heat flux predictions are based on local fluid conditions from VIPRE-W. The DNB heat flux is then used with the local heat flux to calculate DNB. The DNB margin of the WLTAs is conservatively based on the lower margin predicted by either W-3 or OKB DNB correlation.

In order to evaluate the maximum acceptable fuel rod power for WLTAs in the mixed core, the following cases of operating conditions were investigated:

Case 1 - Nominal Operation Conditions at 4- Loop Operation: \(N_r^0 = 59\) kW/rod, \(P^0 = 15,7\) MPa, \(T_{in}^0 = 286\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 2 - Low Pressure & Low Temperature Core Limit (I) represents a DNB limiting condition during increase in heat removal by the secondary system or reactor cooldown events:
\(N_r^0 = 59\) kW/rod, \(P^0 = 11,0\) MPa, \(T_{in}^0 = 271,1\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 3 - Low Pressure & Low Temperature Core Limit (II) represents a DNB limiting condition during increase in heat removal by the secondary system or reactor cooldown events:
\(N_r^0 = 59\) kW/rod, \(P^0 = 12,6\) MPa, \(T_{in}^0 = 282,2\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 4 - Low Pressure & High Temperature Core Limit (I) represents a DNB limiting condition during decrease in heat removal by the secondary system events:
\(N_r^0 = 59\) kW/rod, \(P^0 = 14,6\) MPa, \(T_{in}^0 = 298,9\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 5 - Low Pressure & High Temperature Core Limit (II) represents a DNB limiting condition during decrease in heat removal by the secondary system events:
\(N_r^0 = 59\) kW/rod, \(P^0 = 15,7\) MPa, \(T_{in}^0 = 304,4\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 6 - High Pressure & High Temperature Core Limit represents a DNB limiting condition during RCCA malfunctions events: \(N_r^0 = 59\) kW/rod, \(P^0 = 16,6\) MPa, \(T_{in}^0 = 310\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 7 - High Pressure & Overpower & Nominal Temperature Core Limit represents a DNB limiting condition during RCCA malfunctions events: \(N_r^0 = 70,8\) kW/rod, \(P^0 = 16,6\) MPa, \(T_{in}^0 = 286\) °C, \(V_{in}^0 = 5,17\) m/s.

Case 8 - Low Flow Condition (I) represents a DNB limiting condition during flow reduction events: \(N_r^0 = 59\) kW/rod, \(P^0 = 15,7\) MPa, \(T_{in}^0 = 286\) °C, \(V_{in}^0 = 3,87\) m/s.

Case 9 - Low Flow Condition (II) represents a DNB limiting condition of a Loss of Flow Accident with all Pumps Coastdown: \(N_r^0 = 57,4\) kW/rod, \(P^0 = 15,6\) MPa, \(T_{in}^0 = 286\) °C, \(V_{in}^0 = 4,16\) m/s.

The ten reference axial power distributions with the core axial offsets (AO) in the range (−45%, +45%) were used in the power reduction calculations.

To evaluate the 95% UTL for the design fuel rod power reduction coefficient in Eq. (20), the following uncertainties for the plan operating parameters have been taken from Ref. [26, 35, 36]: \(\delta_N = 2%\) nominal value, \(\delta_p = 0,2\) MPa, \(\delta_{r_{in}} = 2^\circ\)C, \(\delta_{\text{Flow}_{in}} = 1%\) nominal value, \(\delta_{\text{bypass}} = 1%\) is conservatively assumed, \(\delta_{\text{r}_{\Delta H}} = 4%\) nominal value, \(\delta_{\text{r}_{\Delta H,1}} = 3%\), \(\delta_{\text{VIPRE-}}\text{W (Power Convergence)} = 0.5%\). The \(\delta_{\text{VIPRE-}}\text{W}\) uncertainty value has been evaluated for the VIPRE-W iteration to the reference DNB values with the DNB convergence factor of 0,0001.
In order to evaluate the maximum fuel rod power for the six WLTAs (even quantity) in the WWER-1000 mixed core containing the 163 fuel assemblies (an odd quantity), the power reduction calculations were performed for the one and the seven Westinghouse fuels in the mixed core. The corresponding VIPRE-W models are shown in Fig. 2. For the one WLTA in the mixed core, the WLTA fraction is equal to \( m(1) = 0.0061 \), while for the seven WLTAs – \( m(7) = 0.0429 \). It should be noted that the VIPRE-W model for the fuel fraction \( m(7) \) reflects a conservative WLTAs configuration in the mixed core which was realized in the SU NPP-3 mixed core and shown in Fig. 3.

For the six WLTAs in the mixed core, the fuel rod power reduction is evaluated by a linear interpolation as follows

\[
\Delta N_{r(6)} = 0.1666(7) \Delta N_{r(1)} + 0.8333(3) \Delta N_{r(7)}
\]

For the nine cases of DNBR limiting operating conditions, the maximum fuel rod power reduction results for WLTA in the WWER-1000 mixed core with the Russian TVS-M fuel assemblies are shown in Table 2.

<table>
<thead>
<tr>
<th>Case</th>
<th>( \Delta N_{r(1)}^{\text{max}}(0), % )</th>
<th>( m(1) = 0.00613 )</th>
<th>( \Delta N_{r(7)}^{\text{max}}(0), % )</th>
<th>( m(7) = 0.04294 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>W-3 DNBR OKB DNBR</td>
<td>6.67 6.87</td>
<td>6.08 6.18</td>
<td>6.10 6.45</td>
<td>5.73 5.90</td>
</tr>
<tr>
<td>1 2 3</td>
<td>5.10 6.45</td>
<td>4.65 5.37</td>
<td>6.13 6.73</td>
<td>5.60 6.11</td>
</tr>
<tr>
<td>4 5 6</td>
<td>6.33 6.68</td>
<td>5.80 6.04</td>
<td>6.48 6.84</td>
<td>5.99 6.16</td>
</tr>
<tr>
<td>7 8 9</td>
<td>6.92 7.02</td>
<td>6.27 6.38</td>
<td>6.53 7.22</td>
<td>6.04 6.43</td>
</tr>
<tr>
<td>1 2 3</td>
<td>6.42 6.76</td>
<td>5.90 6.18</td>
<td>6.42 6.76</td>
<td>5.90 6.18</td>
</tr>
</tbody>
</table>

For both DNB correlations, Table 2 shows that of all the investigated cases described above, the case 7 and 8 have the highest maximum rod power reduction values. The OKB DNBR correlation shows the slightly higher power reduction values than the W-3 DNBR correlation, but the difference between the corresponding values is not significant. For instance, the maximum \( \Delta N_{r(1)}^{\text{max}}(0) \) value of 7.22% corresponds to the case 8 with OKB DNBR correlation and the maximum \( \Delta N_{r(7)}^{\text{max}}(0) \) value of 6.92% corresponds to the case 7 with the W-3 DNBR correlation. Also, it was found that the reference axial power shapes WCAP-1959 (core AO = -2.4%, peak power = 1.1073) and CTEM (AO = -7.94%, peak power = 1,209) are the most limiting axial power distributions of all the investigated axial power shapes.

The calculation results of the 1.645* \( \left( \frac{\sigma_{\Delta N_{r(1)^{\text{max}}}(0)}}{\Delta N_{r(1)^{\text{max}}}(0)} \right) \) correction factor in Eq. (20) for the cases with the highest \( \Delta N_{r(1)^{\text{max}}}(0) \), which are shown in Table 2, are placed in Table 3.
Using Eq. (22) together with the correction factors from Table 3, the corrected maximum fuel rod power reduction for the six WLTAs in the mixed core is evaluated as follows: $\Delta N_{r(6)} = 6,9\%$ for the OKB DNBR correlation and $\Delta N_{r(6)} = 6,7\%$ for the W-3 DNBR correlation.

Thus the maximum fuel rod power in the WLTAs is reduced by 7% from the current design limit for the existing fuel will ensure sufficient DNBR margin to account for potential transition core effects as a result of larger hydraulic loss coefficients of the WLTAs.

4. Conclusions

The Westinghouse T/H analysis methodology for a PWR mixed core was extended to a WWER-1000 mixed core. The fuel rod power reduction analysis for the six Westinghouse LTAs loaded in the WWER-1000 core with the Russian TVS-M fuels was performed with the VIPRE-W subchannel code. The calculated fuel rod power reduction has been corrected by a factor in which all core operation parameters uncertainties are statistically combined with the calculation uncertainty. It was shown that the maximum fuel rod power in the WLTAs should be reduced by 7% to assure that there is at least a 95% probability that DNB will not occur on the limiting WLT fuel rod during normal operation, operational transients, and any transient conditions arising from faults of moderate frequency at a 95% confidence level.

Nomenclature

$A$  fraction of areas of the vanes to the flow cross section  
$C$  empirical constant  
$N_{tu}$  Nusselt’s number, $hD/k$  
$N_r$  average thermal power per fuel rod (W per rod)  
$K(z)$  axial power distribution in the pin  
$P$  operating system pressure (Pa)  
$V$  coolant average inlet velocity (m/s)  
$T$  absolute temperature or core inlet temperature, (K)  
$De$  hydraulic diameter  
$F_{\Delta H}^{N}$  nuclear enthalpy rise hot channel factor. It is the ratio of integral of linear power along the fuel rod with the highest integrated power to the average rod power. $K_r$ is the analog parameter in the Russian design.  
$F_{\Delta H,1}^{E}$  engineering enthalpy rise hot channel factor. This factor accounts for the effects of flow conditions and rod fabrication tolerances.  
$h$  heat transfer coefficient  
$h_{fg}$  latent heat of evaporation  
$m_e$  mass evaporated rate
Subscripts

- $bo$: boiling onset
- $nb$: nucleate boiling
- $fc$: forced convection
- $o$: original $Nu$ for single tube or rod bundle
- $i$: channel index in the VIPRE-W subchannel model
- $j$: axial node index in the VIPRE-W subchannel model
- $k$: fuel rod index in the VIPRE-W subchannel model
- $m$: fraction of the limiting fuel assemblies in a mixed core
- $0$: initial state of the core operating parameter
- $ref$: reference value
- $oc$: operating condition
- $ap$: axial power distribution
- $in$: inlet
- $I$: current set index of core operating conditions
- $J$: current set index of axial power distributions

References


