DETERMINATION OF WEIGHT FACTORS FOR VVER-440 FUEL ASSEMBLIES WITH BURNABLE POISON

Sándor Tóth, Attila Aszódi
Budapest University of Technology and Economics
Institute of Nuclear Techniques
Műegyetem rkp. 9., 1111 Budapest, Hungary
toth@reak.bme.hu, aszodi@reak.bme.hu

ABSTRACT

Detailed CFD model for the head parts of the VVER-440 fuel assemblies with burnable poison has been developed. The coolant mixing was analyzed in some typical assemblies with this model and the signals of the in-core thermocouples above the selected assemblies were calculated. The investigations pointed out that the mixing is intensive in these assembly heads but the coolant is not perfectly mixed before reaching the thermocouples. Significant differences between the outlet average coolant temperatures and the thermocouple signals were revealed in the case of the fresh fuels. These deviations can cause about 6% underestimations in the online monitored assembly powers unless a proper correction is introduced. The coolant mixing was also studied by means of numerical tracers and weight factors of selected rod bundle regions for the in-core thermocouple were determined. Using these weight factors and the outlet enthalpies of the assemblies’ subchannels, the thermocouple signals can be corrected.

1. INTRODUCTION

In the VVER-440/213 type pressurized water reactors, the core outlet temperature field is monitored with in-core thermocouples, which are installed above 210 fuel assemblies in the holes of the protective tube unit’s lower plate. These thermocouples provide essential information on the reactor core state. The measured temperatures are used to determine the fuel assembly powers and they play an important role in the reactor power limitation as well. For these reasons, interpretation of the thermocouple signals is an important issue. Originally, perfect coolant mixing was assumed in the head parts of the fuel assemblies so the measured values were interpreted as the outlet average coolant temperatures of the assemblies. However, the research activities around the technical conditions of the power upgrade placed this question into the fore.

In the last few years, some members of the VVER community investigated this issue with computational fluid dynamics (CFD) codes [1], [2], [3].
These calculations revealed differences between the thermocouple signals and the outlet average temperatures of the assemblies so they suggested that the coolant mixing is imperfect in the assembly heads.

Lately, this problem was investigated experimentally as well. A full-scale test facility of the VVER-440 fuel assemblies was installed in the Kurchatov Institute (Russia) and extensive measurement series was carried out under close to operational conditions [4]. The coolant mixing in the assembly heads was also studied with particle image velocimetry and laser induced fluorescence on a test facility at the KFKI Atomic Energy Research Institute (Hungary) [5].

Recently, an assembly head model was developed with the code ANSYS CFX [6] at our institute and it was validated with the measured data of the Kurchatov Institute [7]. Extensive sensitivity studies were performed for the mesh, inlet boundary conditions, difference scheme and turbulence model. During these processes, the best practice guidelines [8] were extensively applied. Based on the results of these studies, a CFD model of the head parts of the VVER-440 fuel assemblies with Gadolinium (Gd) burnable poison were developed. The coolant mixing was analyzed with this model in the head parts of some Gd fuel assemblies which will be introduced in the near future in the Hungarian nuclear power plant (Paks NPP). Besides, weight factors of selected rod bundle regions for the in-core thermocouple were determined as well. This paper presents the CFD model and the main results of our investigations.

2. DESCRIPTION OF CFD MODEL

Purposes of our work are study of the coolant mixing in the head parts of the VVER-440 fuel assemblies with burnable poison and investigation of the relations between their outlet average coolant temperatures and the signals of the thermocouples above them. For these tasks, a detailed CFD model has been developed with the code ANSYS CFX. The model geometry (Fig. 1) contains the upper part of the fuel assembly from the end of the rod bundle's active part and a channel of the protective tube unit's lower plate, which includes an in-core thermocouple. All parts in the investigated domain were modeled which can effect the coolant mixing considerably. The dimensions of the model are based on the technical documentation of the Gd fuel assembly. The rod pitch in the triangular lattice is 12.3 mm and the outer diameter of the fuel rods is 9.1 mm.

This complex flow domain was resolved with a hybrid mesh of about 8.5 million cells (Fig. 2). The main features of this mesh agree with the features of the mesh that was accepted in a former validation [7]. The bottom region (Fig. 2/B) was meshed with tetrahedral elements near the spacer grid and with prism elements below and above the tetrahedral zone. Owing to its irregular shape, the top region (Fig. 2/A) was resolved with tetrahedral cells. In the near-wall regions, flat prism layers were generated. Local grid refinements were applied at the ending of the pins, around the mixing grid, the catcher and the thermocouple housing in order to decrease the spatial discretization errors. Non-matching grids of the bottom and top regions were connected with an interface.

Five different fuel assemblies were investigated in this study (Table 1). The selected configurations cover the typical fuel assemblies of a VVER-440 type reactor: internal assemblies of average, high powers and peripheral assemblies with four or five neighbouring assemblies and inclined power distribution profile.
Table 1. Investigated fuel assemblies

<table>
<thead>
<tr>
<th>Notation</th>
<th>Characteristics</th>
<th>Burnup-cycle</th>
<th>Operational time in cycle [EFPD]</th>
<th>Power [MW]</th>
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<tbody>
<tr>
<td>I1</td>
<td>Internal</td>
<td>1</td>
<td>5</td>
<td>5.402</td>
</tr>
<tr>
<td>I2</td>
<td>Internal</td>
<td>2</td>
<td>100</td>
<td>5.322</td>
</tr>
<tr>
<td>I4</td>
<td>Internal</td>
<td>4</td>
<td>100</td>
<td>4.28</td>
</tr>
<tr>
<td>P1</td>
<td>Peripheral, 5 neighbouring assemblies</td>
<td>1</td>
<td>5</td>
<td>3.615</td>
</tr>
<tr>
<td>P4</td>
<td>Peripheral, 4 neighbouring assemblies</td>
<td>4</td>
<td>100</td>
<td>1.861</td>
</tr>
</tbody>
</table>

Fig. 1. Model of the Gd fuel assembly heads (cutaway view)
First, load-follow calculations were carried out with the C-PORCA core design code in order to determine the nodal power distributions of the assemblies' fuel pins [9]. Based on our former results, the COBRA subchannel code [10] can provide acceptable predictions for the main inlet parameters of the assembly head model [7]. Therefore, the rod bundle calculations were accomplished with the COBRA subchannel code using the nodal pin powers, the mass flow rate (23.79 kg/s) and the inlet temperature of the coolant (267 °C) [9]. The boundary conditions of the CFD calculations can be seen in Fig. 1. At the inlet of the model, velocity ($W_{IN}$) and temperature ($T_{IN}$) distributions determined with the COBRA code were applied. Fig. 3 shows these inlet temperature fields (shape of the I4 field is very similar to the I2 field). The turbulence intensity ($Tu$) and the viscosity ratio ($\mu_t/\mu$) of the entering coolant were given based on the results of previous CFD rod bundle calculations [11]. Inlet boundary condition was applied at the end of the central tube in order to take into account the flow from it. The magnitudes of the velocity and temperature were set on the basis of experimental correlations [4]. Due to the lack of any related data, medium turbulence level was assumed.

In order to study the contributions of the assembly regions to the in-core thermocouple signal, numerical tracers were added to the coolant flowing from the central tube (R1 region) and to the coolant of five rod bundle regions at the model inlet (Fig. 4, R2-R6 regions). Concentration of a tracer ($C_A, A=1..6$) is 1 kg/kg in its own entrance region and 0 kg/kg in every other zone.

At the top surface of the model, outlet boundary condition was applied of 0 bar relative average pressure. The reference pressure was set to 123 bar, which is the average coolant pressure in the reactor core. All physical walls were treated as no-slip smooth adiabatic walls. The BSL Reynolds stress model with automatic near-wall treatment [6] was applied to model the turbulence. In this second-order closure model, transport equations for Reynolds stresses and for specific dissipation are solved. Due to a blending, the model possesses the advantages of the $\omega$ based Reynolds stress models without free stream sensitivity. In the validation, the BSL Reynolds stress model was proven appropriate to calculate the coolant mixing in the VVER-440 fuel assembly heads [7]. High resolution scheme [6] was applied in the discretization of the advection terms. Temperature dependencies of the coolant properties were taken into account using IAPWS-IF97 water data.
Fig. 3. Temperature distributions at the inlet of the assembly head CFD model calculated with the COBRA subchannel code (burnable poison containing rods are marked by thick circles)

Fig. 4. Entrance regions of the numerical tracers
A passive scalar transport equation was solved for each tracer:

\[
\frac{\partial (\rho C_A)}{\partial t} + \frac{\partial (\rho U_j C_A)}{\partial x_j} = \frac{\partial}{\partial x_j} \left( \frac{\mu_t}{Sc_t} + \rho D_A \right) \frac{\partial C_A}{\partial x_j}
\]  

(1)

where \(C_A\) is the concentration of tracer 'A', \(\rho\) is the density, \(U_j\) are the velocity components, \(\mu_t\) is the turbulent viscosity, \(Sc_t\) is the turbulent Schmidt number and \(D_A\) is the diffusion coefficient of tracer 'A'. This equation is analogous with the heat transfer equation. It describes the convective, the turbulent transports and the molecular diffusion of a tracer. The volume-averaged value of the thermal diffusivity was given for the values of the tracers' diffusion coefficients.

Thus, six partial differential equations were solved in addition to four equations of the Navier-Stokes equation system, seven equations of the turbulence model and an equation of the heat transfer. Convergence criteria were \(10^{-4}\) for the RMS of the equation residuals.

Since completely steady-state solutions were not found, transient simulations were performed also and time averages of the temperature and concentration fields were evaluated. In the transient runs, a part of the entire domain that contains the unstable upper region was investigated only in order to reduce the computational time and effort. This part begins 2.5 mm behind the spacer grid. The initial and inlet boundary conditions of the transients were determined with steady-state simulations. The velocity components, temperature fields, tracer concentrations and turbulence quantities (Reynolds stresses and turbulent dissipation) on a plane 2.5 mm behind the spacer grid were used as inlet conditions for the transients. Second order backward Euler scheme was applied in the temporal discretization. 0.00125 s time step was chosen, which has resulted 3-4 iterations within a time step. 1-1.5 s long transients were investigated. This time lengths were long enough to evaluate the time averages of the thermocouple signals within about ±0.1 °C uncertainty.

3. RESULTS AND DISCUSSION

Steady and transient calculations were carried out for each investigated fuel assembly using eight INTEL XEON 3000 MHz processors. A steady calculation needed about 20 hours and a transient simulation needed about 210 hours wall clock time. The results of the transient calculations are discussed in the following.

Since there are no significant differences between the calculated velocity fields of the fuel assembly heads the result of the simulation I2 (Fig. 5) is presented only. It can be seen that the velocity field is relatively uniform in the rod bundle region. Because of the flow separation, large eddies develop behind the rods and in the corners bordered by the mixing grid and the shroud. Downstream, in the cuts of the mixing grid, the velocity increases and further swirls form behind the grid. Approaching the catcher, the velocity field becomes evener. Behind the catcher, the stream accelerates in the middle region and vertical eddies develop in the peripheral zone. The trace of the catcher can be clearly observed at the level of the in-core thermocouple.
Fig. 6 shows the instantaneous temperature distribution in one of the center planes, the fluctuation of the area-averaged temperature on the thermocouple housing ($T_{TC'}$) and variation of its time average ($T_{TC}$) in the case I2. It can be seen that an unsteady thermal plume evolves in the middle region of the head and it causes large temperature fluctuations ($\pm 0.5$-1.5 °C) on the thermocouple housing. This fluctuation is smoothed by the thermal lag of the steel housing so the in-core thermocouple detects nearly time average value.

Fig. 5. Instantaneous velocity field in the assembly head (calculation I2)

Fig. 6. Unsteady temperature distribution in the assembly head (calculation I2)
**Fig. 7.** Time-averaged temperature distribution in the assembly head (calculation I1)

**Fig. 8.** Time-averaged temperature distribution in the assembly head (calculation I2)
Fig. 9. Time-averaged temperature distribution in the assembly head (calculation P1)

Fig. 10. Time-averaged temperature distribution in the assembly head (calculation P4)
In order to determine the mean thermocouple signals, the time averages of temperature fields were evaluated. In the cases of the inner fuel assemblies, some typical results are presented in Figs 7-8. These time-averaged results are similar. The entrance temperature fields are inhomogeneous but they are nearly symmetrical. The cross sectional temperature distributions practically do not change through the unheated rod bundle region. Because of the intensive coolant mixing caused by the mixing grid and the catcher, the temperature fields become more uniform downstream. However, the 4-5 length/diameter long section between the end of the rod bundle and the in-core thermocouple is too short to mix the coolant perfectly. 4-5 °C differences remain between the extremes of the temperature fields at the thermocouple level. In the case of the fresh inner fuel (calculation I1), the coolant temperature at the thermocouple housing is significantly lower than the cross sectional average value. The reason for this is that the powers of the rods which are containing burnable poison (Fig. 3, marked by cycles) are relatively lower at the beginning of the assembly lifetime therefore the coolant heats up less in the middle region. By the internal assembly in the second burnup cycle (calculation I2), this effect does not occur because of the depletion of the Gd burnable poison, therefore average temperature coolant surrounds the thermocouple housing. The situation is very similar by the inner fuel with high burnup (calculation I4).

Figs. 9-10 show the time-averaged temperature distributions of the peripheral assemblies. The inlet temperature fields are rather asymmetrical because of the strong anisotropy of the neutron flux at the edge of the core. Despite the intensive coolant mixing, the main characteristics of the temperature distributions at the end of the active rod bundle can be clearly observed at the level of the thermocouple. There are about 11-14 °C differences between the extremes of the temperature fields in this section. In the case of the fresh peripheral fuel (calculation P1), the temperature of the coolant around the thermocouple housing is slightly lower than the outlet average temperature owing to the impact of the Gd. Whereas, the coolant temperature at the thermocouple housing agrees well with the cross sectional average value by the peripheral assembly with high burnup (calculation P4).

Table 2. Main results of the calculations

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<tbody>
<tr>
<td>I1</td>
<td>1</td>
<td>5</td>
<td>306.9</td>
<td>309.5</td>
<td>-2.6</td>
<td>42.5</td>
<td>-6.1%</td>
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<tr>
<td>I2</td>
<td>2</td>
<td>100</td>
<td>309.1</td>
<td>308.9</td>
<td>0.1</td>
<td>41.9</td>
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</tr>
<tr>
<td>I4</td>
<td>4</td>
<td>100</td>
<td>301.6</td>
<td>301.3</td>
<td>0.3</td>
<td>34.3</td>
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</tr>
<tr>
<td>P1</td>
<td>1</td>
<td>5</td>
<td>294.5</td>
<td>296.3</td>
<td>-1.8</td>
<td>29.3</td>
<td>-6.0%</td>
</tr>
<tr>
<td>P4</td>
<td>4</td>
<td>100</td>
<td>282.6</td>
<td>282.4</td>
<td>0.2</td>
<td>15.4</td>
<td>1.1%</td>
</tr>
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</table>

The main time-averaged results of the calculations are summarized in Table 2 where T_TC is the thermocouple signal, T_OUT is the outlet average coolant temperature, ΔT is the average heat up of the coolant. The thermocouple signal is determined as time average of the area-averaged temperature on the thermocouple housing.

In the cases of the fresh Gd fuel assemblies (simulations I1 and P1), the calculated thermocouple signals are significantly lower than the outlet average coolant temperatures. These results call the attention that the outlet average temperatures and consequently the assembly powers predicted directly based on the in-core temperature measurements could be considerably underestimated for fresh Gd fuel assemblies. Whereas, the signals agree well with the outlet average temperatures by Gd assemblies with higher burnup so this problem
does not seem to occur. These results point out that the in-core thermocouples not always measure the outlet average coolant temperatures of the assemblies therefore determination of correction for the thermocouple reading is needed.

In order to determine the contributions of the assembly regions to the thermocouple signal, the coolant mixing was analyzed with numerical tracers. Similarly to the temperature field, the concentration fields of the tracers were fluctuating in time. Since our aim is the determination of the assembly regions’ weight factors the time averages of the tracer concentrations were evaluated (Fig. 11). These results show well that the mixing grid and the catcher have considerable impact on the coolant mixing. The grid influences the mixing in the whole cross section while the catcher affects it in the peripheral region. The tracer distributions indicate intensive but imperfect mixing. The thermocouple reading is influenced considerably by the central tube flow and the flow from the R2, R3 and R4 regions namely the flow from the central four subchannel rings. While the stream from the R5 and R6 regions do not have significant effect on the thermocouple reading. The weight factors of the assembly regions ($\delta_A$) were obtained with normalizing and time averaging the area-averaged tracer concentrations on the thermocouple housing:

$$\delta_A = \frac{1}{\sum_{A=1}^{6}} \frac{1}{\Delta t} \frac{1}{A_{TC}} \frac{1}{\Delta t A_{TC}} \int \int C_A \, dA \, dt$$

(2)

where $\Delta t$ is the averaging time, $A_{TC}$ is the outer surface of the thermocouple housing at the thermocouple (Fig. 1, marked by black). The normalization was necessary because of the smaller overshoot in the sum of the concentrations, which was caused by the used advection scheme. The enthalpy at the thermocouple housing ($h_{TC, \delta}$) can be calculated based on the weight factors ($\delta_A$) and the average enthalpies of the regions at the end of the rod bundle’s active part ($h_A$):

$$h_{TC, \delta} = \sum_{A=1}^{6} \delta_A h_A$$

(3)

(The average enthalpies of the regions can be calculated with subchannel codes.) The thermocouple signals (Table 3, $T_{TC, \delta}$) were determined based on Eq. 3 and static pressures using IAPWS-IF97 database. The signals calculated in this manner are in good agreement with the signals determined with the three dimensional CFD heat transfer calculations. The velocity field at the end of the active rod bundle (at the inlet of the assembly head model) is slightly influenced by the pin power distribution because of the temperature dependency of the coolant density. For this reason the thermocouple signals were also calculated using the weight factors of the calculation P1 in order to verify the generality of the factors (Table 3, $T_{TC, \delta P1}$). Similar predictions were obtained compared to the previous results in spite of the fact that the weight factors were applied to assemblies with rather different pin power profile. Consequently, the weight factors are general in the investigated range.

Using these weight factors and the outlet temperatures of the fuel assemblies' subchannels calculated with the on-line core analysis system, the in-core thermocouple signals and the outlet average coolant temperatures can be estimated. The mixing corrections for the measured values can be determined as the differences of these estimated temperatures.
Fig. 11. Time-averaged concentration fields of the numerical tracers (calculation I2)
Table 3. Thermocouple signals calculated with weight factors

<table>
<thead>
<tr>
<th></th>
<th>I1</th>
<th>I2</th>
<th>I4</th>
<th>P1</th>
<th>P4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T_{TC,δ}$ [$^\circ$C]</td>
<td>307.4</td>
<td>309.3</td>
<td>301.8</td>
<td>294.5</td>
<td>282.7</td>
</tr>
<tr>
<td>$T_{TC}$ [$^\circ$C]</td>
<td>306.9</td>
<td>309.1</td>
<td>301.6</td>
<td>294.5</td>
<td>282.6</td>
</tr>
<tr>
<td>$T_{TC,δ},δ,δ,δ,T_{TC}$ [$^\circ$C]</td>
<td>0.5</td>
<td>0.2</td>
<td>0.2</td>
<td>0</td>
<td>0.1</td>
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<tr>
<td>$T_{TC,P1}$ [$^\circ$C]</td>
<td>307.3</td>
<td>309.2</td>
<td>301.8</td>
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<td>282.7</td>
</tr>
<tr>
<td>$T_{TC,P1},T_{TC}$ [$^\circ$C]</td>
<td>0.4</td>
<td>0.1</td>
<td>0.2</td>
<td>-</td>
<td>0.1</td>
</tr>
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</table>

4. CONCLUSIONS

A CFD model for the head parts of the VVER-440 fuel assemblies with burnable poison was developed based on experiences of former model validation and sensitivity studies. Investigations were performed for typical Gd fuel assemblies which will be introduced in the Hungarian NPP in the near future.

The examinations pointed out that the coolant mixing is intensive in the head parts of these assemblies but 4-5 length/diameter long section between the end of the active rod bundle and thermocouple housing is too short to achieve a complete mixing. In the cases of the fresh Gd fuel assemblies, significant differences were found between the outlet average coolant temperatures of the assemblies and the thermocouple signals. These deviations can cause about 6% underestimations in the assembly powers when a proper correction is not introduced for the thermocouple readings. For the Gd fuel assemblies in the second or subsequent burnup cycles, the calculated signals agree well with the outlet average temperatures despite of the imperfect mixing so the problem does not seem to occur. Because of the possible deviations between mentioned temperatures, determination of correction for the thermocouple reading was needed.

In order to determine the contributions of the rod bundle regions to the thermocouple signal, the coolant mixing was analyzed with numerical tracers. These investigations showed that the thermocouple reading is influenced significantly by the central tube flow and the flow from the central four subchannel rings only. Weight factors of the rod bundle regions were determined based on the calculated tracer concentrations. The thermocouple signals determined with these factors are in good agreement with the signals obtained from the CFD heat transport calculations. The weight factors are not sensitive to the pin power distributions in the investigated range.

Using the weight factors and the outlet temperatures of the assemblies’ subchannels calculated with the on-line core analysis system, mixing corrections of the in-core thermocouple readings become possible. Test and implementation of these weight factors into the core monitoring and analysis system are in progress in the Hungarian NPP. The new system will allow a more precise comparison between the fuel assembly outlet temperatures determined with the coupled neutronics and thermohydraulics calculations in core analysis system and the temperatures measured with the in-core thermocouples.
REFERENCES