



Analysis of the IAEA SPE-4 small-break LOCA experiment with RELAP5, TRACE and APROS system codes

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ABSTRACT

The Small Break Loss of Coolant Accident (SBLOCA) of a nuclear power plant is a transient that is subject to numerous studies. With the support of both experiments and simulation codes, professionals aimed to gain a deeper knowledge of the process, which would prove invaluable in the design and licensing of nuclear power plants. The comparison of integral test results with simulations serve as basis for licensing code validation. A notable example in its history of research is the series of IAEA Standard Problem Exercises (SPE), which were carried out in the 80s and 90s on the Hungarian PMK-2 facility, a scaled down model of the VVER-440/213 type pressurized water reactors of Paks NPP. This paper presents in detail our RELAP5, TRACE and APROS models that were recently developed for the simulation of the fourth Standard Problem Exercise (SPE-4). In order to assess the reliability and adequacy of these models, both qualitative and quantitative analyses were performed, for which the available measurement results served as reference. Quantitative analysis was performed using the original and the improved Fast Fourier Transform Based Method (FFTBM) and the Stochastic Approximation Ratio Based Method (SARBM). The application of these methods allowed for a thorough evaluation of our models and provided a basis of comparison with previously published results.

1. Introduction

The pressurized water reactors (PWR) constitute the large majority of the world's operating nuclear power plants. In order to meet the design and licensing safety criteria and to prove if the safety requirements of the nuclear safety codes are fulfilled, adequate deterministic analysis of physical processes in the reactor is required. Thanks to the advanced computational technology, it is now possible to simulate system behaviour by using system codes which have the capabilities to calculate thermal-hydraulics and reactor-physics processes together with the interactions of control and protection systems. Although these codes are built upon the same fundamental principles, the methods of modelling particular phenomena may differ between them which could lead to calculating different results for a given problem. This entails the need of comparing different codes and investigate the root of possible disparities.

The main field of application of system codes is the analysis of various transients occurring in nuclear power plants (NPP). One of the most important phenomena to investigate is the loss of coolant accident (LOCA), which depending on its severity may compromise the cooling of

the reactor core and result even in the loss of integrity of fuel rods. Apart from LOCA, other accidents such as primary to secondary leakage also require a better understanding (Groudev et al., 2004).

RELAP5 is a widely used code of which the application has decades of history (Groudev et al., 2015), unlike the novel system code TRACE, which can be considered the successor of RELAP5 and the new flagship of the system codes owned by the US Nuclear Regulatory Commission (NRC). TRACE has so far been used primarily to study western PWRs (and not VVERs), for which the modelling of the Maanshan PWR serves as an example (Wang et al., 2009). Recent applications of TRACE also include the simulation of a small-break LOCA for the AP1000-type power plant as presented in (Montero-Mayorga et al., 2015), where the results from a best-estimate TRACE model were compared to those calculated by the conservative NOTRUMP code. The NRC actively encourages the use of TRACE since numerous improvements were made compared to RELAP5 in terms of correlations, numerical solution methods as well as user-friendly features. On the other hand, due to the long history of application of RELAP5 (Groudev and Stefanova, 2006); (Groudev and Pavlova, 2004), it is reasonable to do a comparison between the aforementioned two and possibly other validated system

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codes.

It has to be mentioned that around the turn of the 80s to 90s one of the major safety and licensing questions of the Russian designed VVER reactors was whether the western deterministic safety analysis codes are able to adequately describe the physics of VVER reactors with their specific features compared to western type PWRs (e.g. horizontal steam generators, triangular fuel pin lattices, different primary pipe arrangements in VVERs, see further details later). In order to provide with a more comprehensive validation of western system codes for VVER-type reactors, numerous test facilities were constructed including the Finnish PACTEL (Tuunanen et al., 2000) and the Hungarian PMK-2 facility (Szabados et al., 2007).

Several experiments have been carried out on the PACTEL test facility for studying the characteristic thermal-hydraulic processes of VVER-type reactors. In Tuunanen et al. (2000) the operation of the passive emergency core cooling system components was examined using APROS, CATHARE and RELAP, while different aspects of numerical modelling of the pressurizer were discussed in (Takasuo, 2006). Furthermore, in the framework of an international cooperation, a small-break LOCA experiment similar to SPE-4 of the PMK-2 facility was conducted on PACTEL (Kouhia et al., 2013). The simulation involved numerous participants using different system codes. It is observed that in VVER type reactors the presence of loop seals influences the course of LOCA significantly, which was also investigated in PACTEL experiments. For these purposes, APROS and TRACE system models were also developed (Kauppinen et al., 2015). In addition, simulations were performed for the depletion of accumulator during LOCA in APROS and TRACE (Kauppinen et al., 2019). The works listed above also show that comparing the results for an experimental facility with multiple system codes (simultaneously) could be relevant and make it easier to identify the causes of discrepancies.

The Standard Problem Exercises (SPE), organized in the framework set by the International Atomic Energy Agency (IAEA), were one of the many series of experiments performed on the Hungarian PMK-2 facility (Szabados et al., 2007). In this paper, we are focusing on the simulation of the fourth exercise, which replicated a small-break LOCA resulting from a rupture at the downcomer head. The exercise involved international cooperation with the participation of numerous institutes (IAEA TECDOC-848, 1995). The aim of the collaboration was to investigate the know-how of modelling, to study the user effect and to provide training for the professionals in this field for which another example is presented in Aksan et al. (1993)). Since then, a handful of reports were published concerning the difficulties of modelling the SPE-4 experiment. In Cebull (1994) the authors studied the conditions of core dry-out occurrence in their computer models while in Szabados et al. (1995) the issue of the numerical simulation of two-phase natural circulation is addressed by comparing the results from RELAP5 and ATHLET models of the experiment. It was also found in Petelin and Parzer (1998) that the limits of one-dimensional flow modelling have significant effect on the results in case of components with multiple inlet-outlet connections such as the downcomer top, where the break was located. The authors have presented multiple different configurations with respect to the nodalization and the positioning of inlet connection points of the break location to assess the optimal modelling layout. Other publications report the occurrence of core dry-out in the simulation of SPE-4 (Bánáti, 1995; Bánáti and Ézsöl, 1997), and good agreement was observed between the computed and measured curves. It was also shown in Parzer and Zagar (2003) that the adequate numerical modelling of critical flow is crucial for LOCA-type transients.

One of the motivations for writing this article is to perform a thorough comparison of three system codes (RELAP5, TRACE and APROS) by simulation of the well-known SPE-4 benchmark exercise. By preparing a model in each code and evaluating the obtained results, the question of necessary modelling considerations and application of the implemented methods is investigated thus revealing the underlying causes of possible discrepancies between the codes. This effort is in line

with the direction set by the US NRC, and follows the way of comparing several system codes paved by the above-mentioned experiments performed on PACTEL (Tuunanen et al., 2000). The comparison of different system codes is relevant in light of the licensing of new PWR and VVER reactors. Furthermore, it was needed to assess the accuracy of these models. For this purpose, quantitative analysis was performed using variants of the FFTBM and SARBM methods, which also provided an opportunity to compare their results and to test the proposed improvements of the FFTBM, since so far, only the original method had published applications in analyses of the SPE-4 experiment.

2. PMK-2 test facility

The PMK-2 test facility (see Fig. 1) is a full-pressure and full-temperature thermal-hydraulic model of the primary and partly the secondary circuit of the VVER-440/213 type pressurized water reactor of Paks nuclear power plant (NPP) (Szabados et al., 2007). It is located in Budapest, Hungary in the former KFKI Atomic Energy Research Institute (AEKI). The facility was primarily designed and constructed for the experimental investigation of small-break loss-of-coolant accidents (SBLOCA) in the primary circuit of a VVER-440 type reactor (Szabados et al., 2007). These have some distinguishing features compared to western-type reactors resulting in different system responses for most of the transients:

- Horizontal steam generators
- Triangular fuel lattice, hexagonal fuel assemblies
- Loop seals in both hot and cold legs
- Relatively high pressure in the safety injection tanks

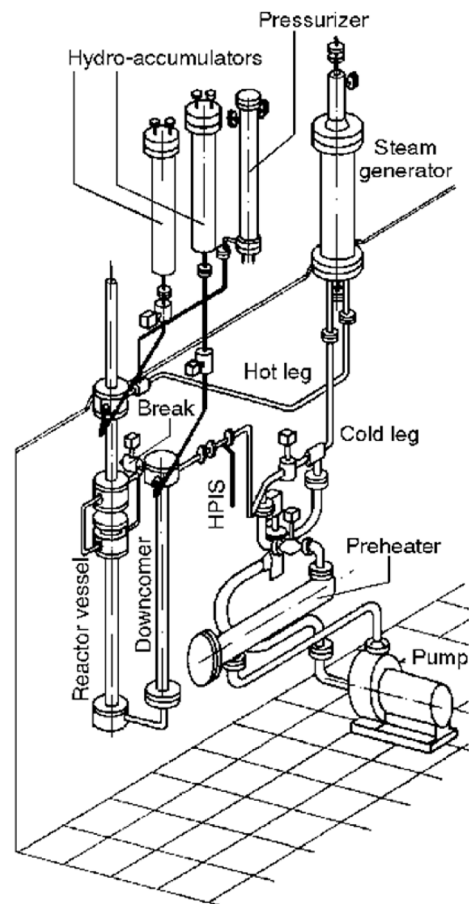


Fig. 1. Axonometric view of the PMK-2 facility (Bánáti, 1995)

The scaling ratio of PMK-2 is 1:2070 for the volumes and the power, while it is 1:1 for the component heights and relative elevations except for the lower plenum and pressurizer. The latter is of great importance for preserving the driving, gravitational forces of both single- and two-phase natural circulation. As a consequence, the pressure and temperature distributions set at the beginning of the experiments are very similar to the nominal values of the operating VVER-440/213 plant.

The reactor vessel model consists of an active core with 19 electrically heated fuel rods (see Fig. 2), an external downcomer and additional pipes modelling the lower plenum, the upper plenum and the upper head. The heated length, the fuel rod pitch and diameter are identical to those of the referenced reactor. The heating power is continuously controlled throughout the experiments and its axial and radial distribution is uniform. A single active loop represents the six loops of the real NPP, including the hot leg, the steam generator and the cold leg.

The steam generator (SG) model shown in Fig. 3 has two collectors and 82 rows of horizontal heat transfer tubes between them with the same vertical surface distribution and overall heat transfer coefficients as in the real plant. The secondary pressure and steam generator water level are controlled by the PV21 (feedwater) and PV22 (steam output) valves shown in Fig. 4.

The design of the cold leg is unique, because the PMK-2 pump cannot be operated with two-phase medium and it does not share the characteristics of the power plant-scale MCP's, which is especially apparent in the coast-down process. In the test facility, the main circulating pump is located in a by-pass line connected to the main cold leg and its only role is to establish nominal conditions. The pump trip is simulated by closing the PV11 control valve of the bypass line in a prescribed manner (see Fig. 4). At the end of this process the change of circulation path is initiated by closing PV11, MV12 and opening MV11 valves. The primary circuit also includes a pressurizer to set and maintain the steady state primary pressure.

The PMK-2 facility is equipped with various emergency core cooling systems shown in Fig. 4. There are two hydro-accumulators (safety injection tanks (SITs), equivalent to the four tanks in the operating reactor) supplying water to the upper plenum and the top of the downcomer of the reactor. The high and low pressure injection systems are connected to the cold leg and the surge line of SIT-1 (top of the downcomer), respectively. HPIS is not shown on Fig. 4, because it was not available during the SPE-4 experiment. Further safety systems include the emergency feedwater injection and the secondary steam

relief valve.

3. Description of the IAEA SPE-4 experiment

The IAEA-SPE-4 experiment (IAEA TECDOC-848, 1995) (Standard Problem Exercise 4) performed in 1993 was the fourth thermal-hydraulic benchmark task carried out on the PMK-2 test facility in the framework of a project organized under the umbrella of the International Atomic Energy Agency. Main features of the experiment are the following:

- A Ø3.2 mm cold leg break modelling a 7.4% break in the plant
- Experiment started from full power
- Pump coast-down simulated in a prescribed manner
- Injection from both hydro-accumulators (SITs) with water amount equivalent to three accumulators of the real plant
- Without injection from the high pressure injection system (HPIS)
- Injection from the low pressure injection system (LPIS)
- Secondary side isolated after transient initiation
- Secondary side bleed
- Emergency feedwater (EFW) injection

The transient was initiated by opening the valve MV31 downstream of the Ø3.2 mm break orifice. The primary pressure dropped rapidly and reached the activation setpoint of the reactor SCRAM (11.15 MPa) within seconds. Thereafter, the core power followed the prescribed decay heat curve. The secondary side was isolated in 6 s by closing the feedwater (PV21) and steam line (PV22) control valves. The 150 s long pump coast-down started when the primary pressure of reached 9.21 MPa. The secondary side bleed through valve PV23 was initiated simultaneously with the termination of the pump coast-down.

Due to the lack of the high pressure injection and the slow depressurization characteristic of the SBLOCA, only the accumulators provided emergency coolant for the core for more than 20 min. The accumulator injection was actuated at 5.9 MPa primary pressure and terminated when the liquid level in the two tanks reached 0.245 m (SIT1) and 1.035 m (SIT2). An early shorter and a later permanent clearing of the hot leg loop seal could be observed. Following the latter, the pressure and liquid levels decreased slowly because of the one-phase steam blowdown, the secondary bleeding and the system heat losses. At this stage of the transient, the heated part of the core got partially uncovered and suffered a significant temperature increase. In the later stage, the decreasing primary and secondary pressures reached the value of 1.04 MPa and 0.93 MPa, respectively, which in turn activated the LPIS and the emergency feedwater system. As a result, the core could be reflooded. At the end of the transient (1800 s), the cooling of the core and the removal of decay heat was provided by the available emergency systems. Based on the fact that the high pressure emergency core cooling system is unavailable during the whole transient it can be classified as beyond design basis accident.

4. Models of the PMK-2 facility

The RELAP5/MOD3.3, TRACE version 5.0 patch 5 (both with version 3.0.2 of the SNAP environment) and APROS code version 6.08.23.01 were used in the simulations of the IAEA SPE-4 experiment. The RELAP5 and TRACE system codes are developed by the U.S. Nuclear Regulatory Commission, while the APROS system code is developed by the Finnish VTT Technical Research Centre and Fortum Oyj company. All of these codes use a two-fluid six-equation model based on the one-dimensional mass, momentum, and energy conservation equations. On the other hand, there are relevant differences as well, for example in the used critical flow models, heat transfer and friction correlations. One of our main goals is to examine the differences between the results, which may be partly originated from these factors.

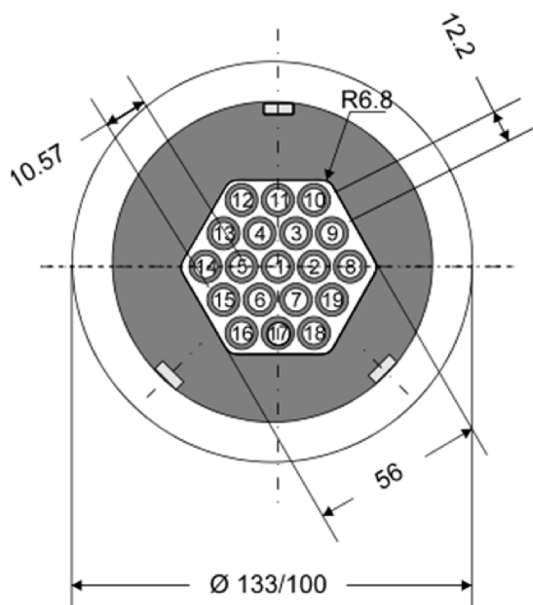


Fig. 2. Cross-sectional view of the core (Szabados et al., 2007)

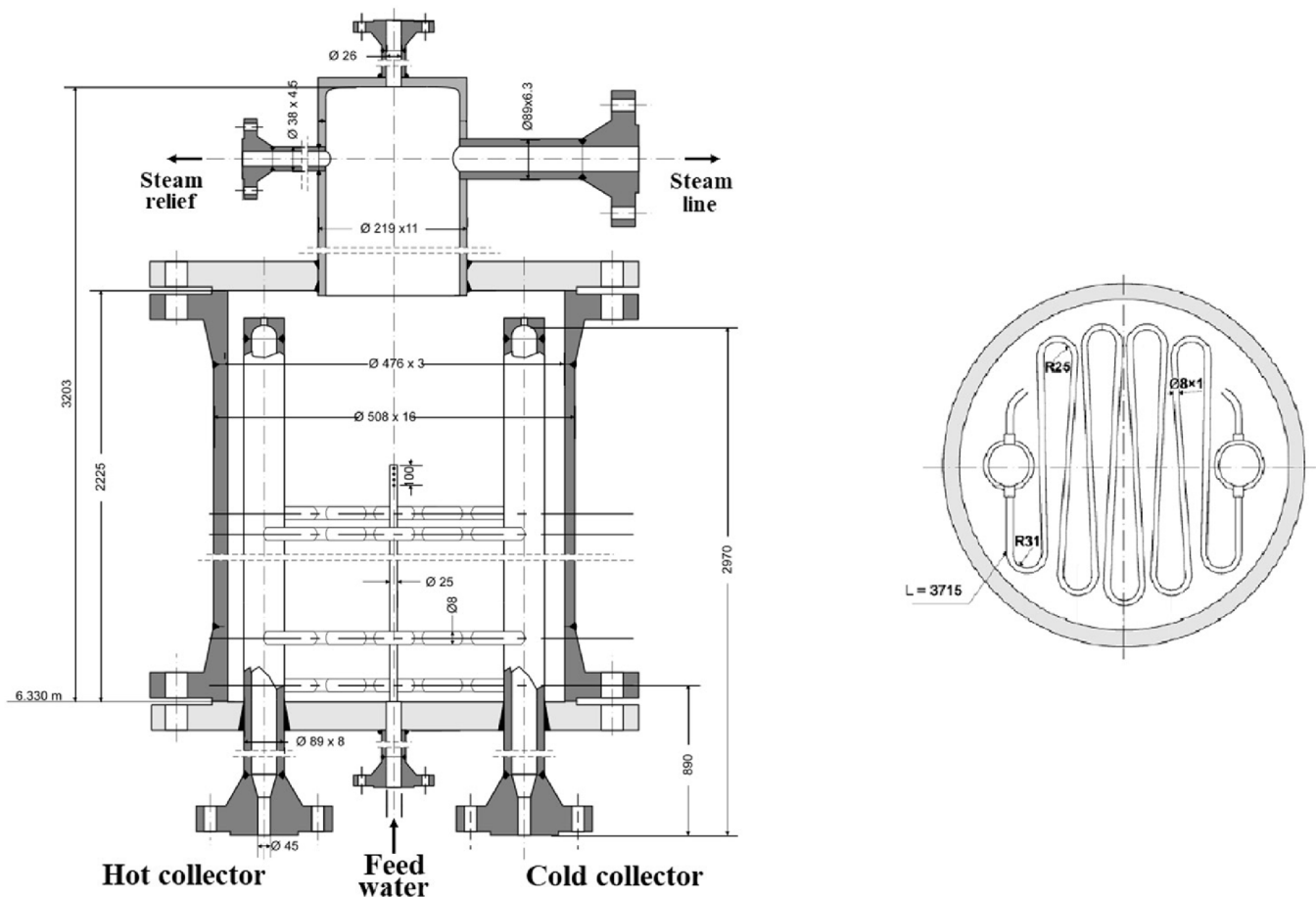


Fig. 3. Steam generator model with cross-sectional view (Szabados et al., 2007)

4.1. RELAP5 model

The geometry, thermal-hydraulic properties of the initial state as well as the implementation of control systems are in accordance with the data published in documents (Szabados et al., 2007); (IAEA TECDOC-848, 1995). The RELAP5 model has undergone many modifications during research and it is the final version that is discussed in detail in this document. The nodalization scheme of the experiment, as shown in Fig. 5 relies mainly on the APROS model described in (Csige et al., 2013).

Due to the volume scaling ratio of the facility, the effect of heat losses is significant. In order to correctly estimate the behaviour of the system, heat structures were assigned to the majority of control volumes which simulate heat transport towards the environment.

The model can be divided into five principal subsystems, namely the emergency core cooling systems, the reactor coolant loop piping, the reactor pressure vessel, the steam generator and the pressurizer.

The emergency core cooling system has three main parts, the two hydro-accumulators, being the passive components and the low-pressure safety injection system. Hydro-accumulators SIT1 and SIT2 were modelled using the ACCUMULATOR component of RELAP5, however, the surge lines were modelled independently from the tank. The non-condensable gas species defined for the component was nitrogen. As in the experiment, the coolant inventory stored in the accumulators was equivalent to three components, with the liquid level in SIT2 being higher.

The reactor coolant loop piping consists of the pipes of the cold and hot leg. In the cold leg, during steady state the coolant flows through the bypass branch where the main circulating pump is located. Therefore, a few components of the main circulation branch (cold leg) are stagnant

which means that no flow occurs through these volumes. This would mean that due to the thermal coupling, the temperature of the fluid in these regions would decrease until it reached equilibrium with the environment. In order to remedy that, the heat transfer coefficient towards the environment was set to zero until the transient initiation.

In the PMK-2 model the pressurizer had an essential role in controlling the pressure in the reactor coolant loop. It is made out of a stainless steel pipe with two inlets and one outlet which is connected to the hot leg of the reactor coolant loop. One of the inlets is for the spray system which supplies liquid from the cold leg, the other inlet connects to a time-dependent junction component (a simplified pump so to speak) which draws the liquid from a TMDPVOL component (see in RELAP5 documentation (Nrc, 2016–2019)). This system provides the necessary coolant inventory in the model.

The break is modelled with a motor valve, which is a connection between the primary circuit and the environment. The pressure of the environment is not constant and the time dependence is defined in the TMDPVOL component modelling the exterior to which the break opens. In the simulation, the Ransom-Trapp critical flow model was applied for which the default subcooled, two-phase and superheated discharge coefficients were appropriate. In the experiment, critical flow was also observed at the PV23 relief valve during the secondary bleeding, for which the discharge coefficients were set to 0.46.

The main components of the reactor pressure vessel are the downcomer top, the downcomer, the lower plenum, the core, the upper plenum and the upper head. There were two heat structures defined for the core. The first described the heating power introduced to the system by the electrical heater bundles. A separate heat structure simulated the heat losses towards the environment. In the upper plenum, the modelling of the two U-shaped tube sections exploited the symmetry of the

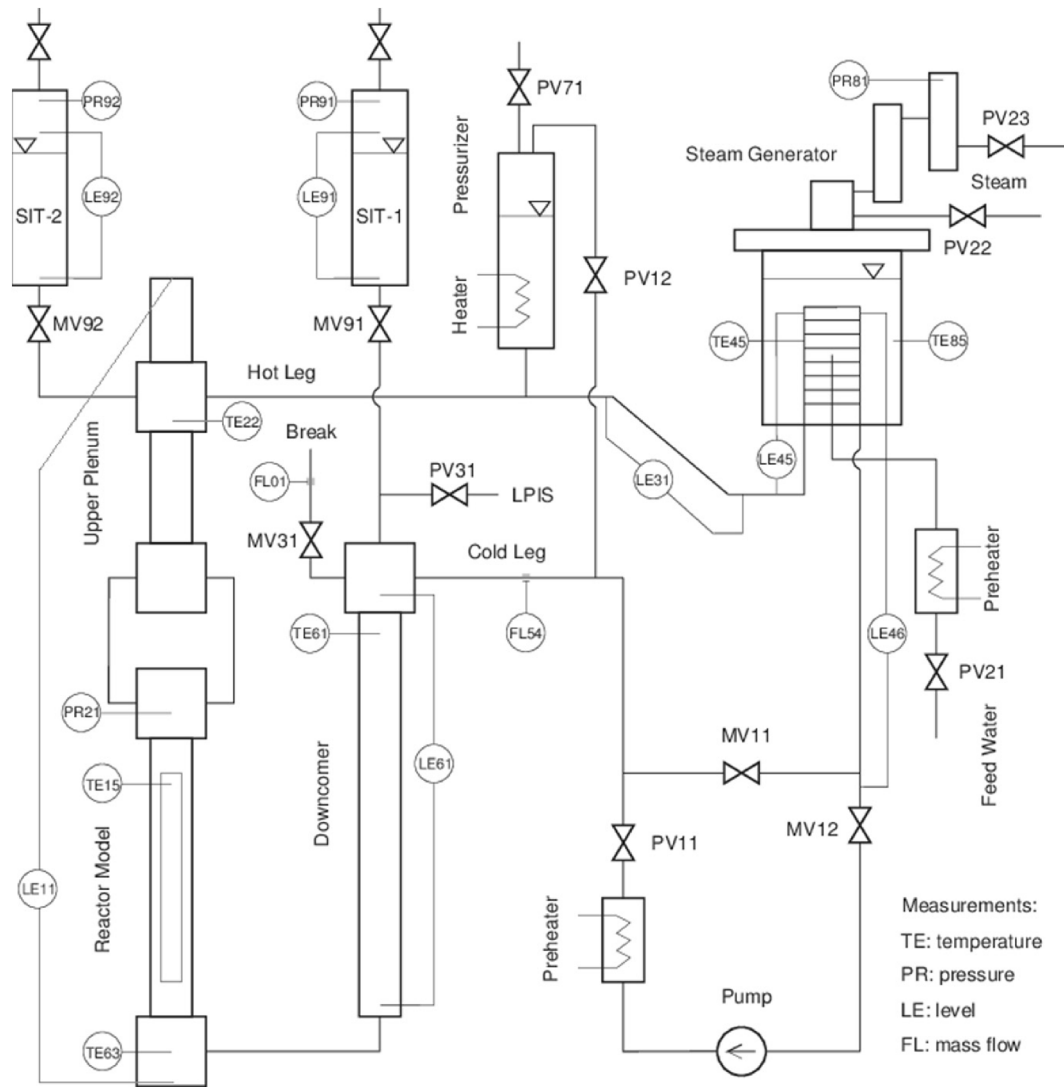


Fig. 4. Flow diagram of the PMK-2 facility with the most important measurements (Bánáti, 1995)

component. In the model, the cold leg nozzle, the surge line of accumulator SIT1 and the valve simulating the break were all connected to the lower node of the downcomer top. Since the one-dimensional flow simulation is not capable of correctly estimating the mixing in this region, the positioning of these connection points has a strong influence over the characteristic of the processes. The best results were achieved by introducing the accumulator surge line below the axial connection point of the break and the cold leg nozzle. The surge line of accumulator SIT2 was connected to the upper head bottom node at the same height as the hot leg nozzle, both in cross-connection (perpendicularly to the orientation of the volume). In steady state, the upper head and the uppermost part of the downcomer are stagnant and thus the same considerations apply as in case of the cold leg circulation branch.

The adequate modelling of the heat transfer between the primary and secondary side of the steam generator is crucial. The primary tubes of the steam generator were divided into three axial layers, represented by an adequately parametrized pipe component. On the secondary side, a separator ensures phase separation. It has to be noted that there are currently no intermediate cross-connections between the secondary side riser and downcomer which is related to numerical stability.

4.2. TRACE model

Throughout the TRACE model development numerous model

versions were set up of which this article discusses the one showing the best agreement with the experiment (Fig. 6). The TRACE model includes all together 53 measurement signals, which are essential in the comparison of the results of the simulation and the measurement performed on the PMK-2 facility.

The thermal-hydraulic model includes both the primary and the secondary loops along with their respective complete control systems. The main objective of the SPE-4 is to investigate the behaviour of the reactor core during the transient, therefore it is crucial to model its geometrical and material properties adequately and to use a sufficiently fine nodalization. In our case the active length has been divided into 10 nodes of equal size within which all of the 19 heater rods were modelled as heat structures. The RPV is completed by connecting the core with the downcomer and upper plenum pipes. In the case of the two hydro-accumulators we have chosen pipe type [1] *Accumulator* (see in TRACE documentation (Nrc, 2017a, 2017b)). In accordance with the experiment, the default non-condensable (NC) gas option (air) was set.

Precise modelling of the steam generator is essential to approximate the thermal coupling between the primary and the secondary side properly. In the TRACE model, the steam generator collectors are connected by three horizontal pipes, each representing a bundle of an equal number of physical tubes. The flow area and the heated surface of these modelling pipes were calculated in a way that the total flow area and total heated surface remains unchanged with respect to the

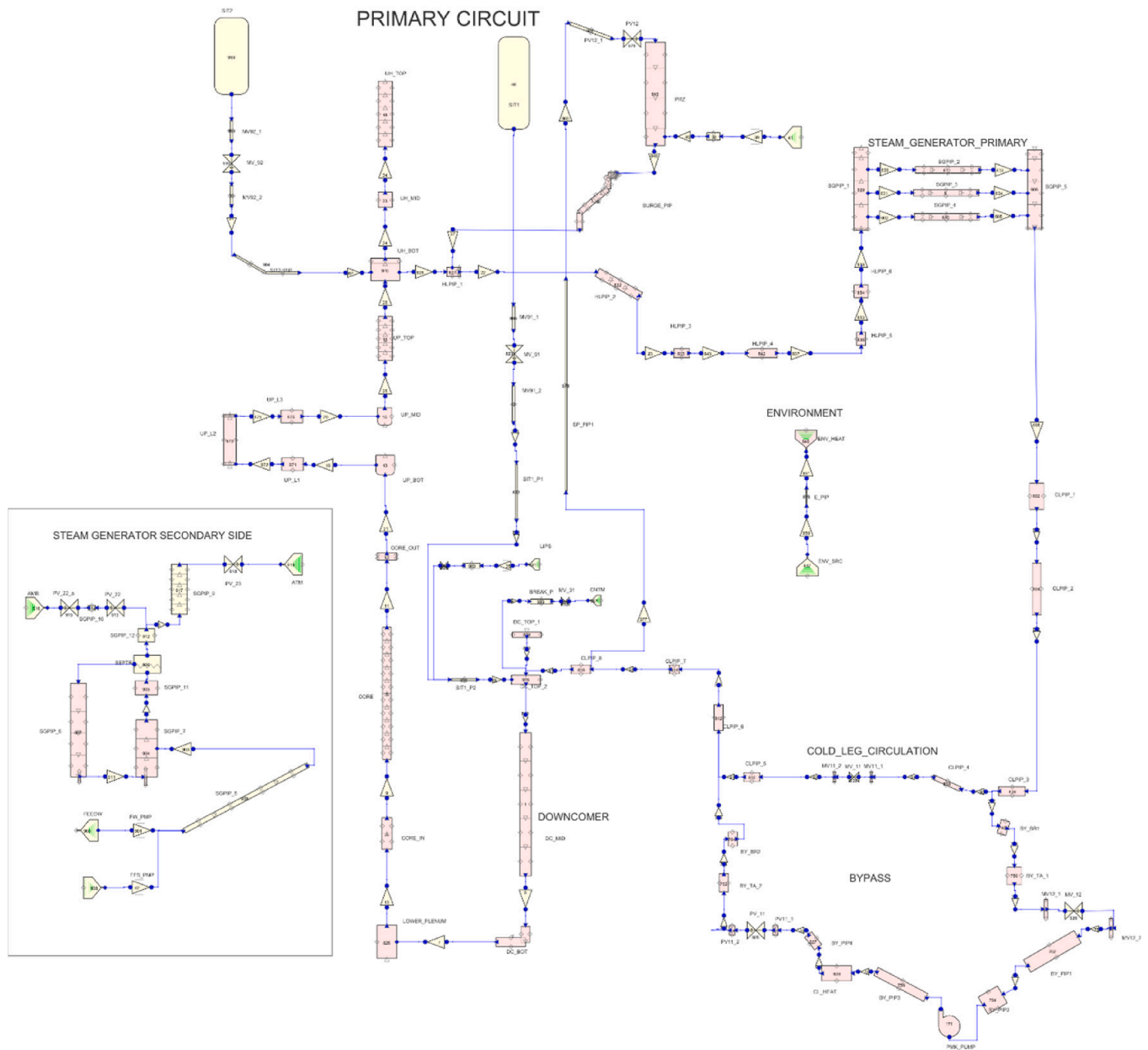


Fig. 5. Nodalization scheme of the RELAP5 model.

corresponding properties of the steam generator in the PMK-2 facility. A detailed model of the secondary side can be observed in Fig. 6. According to Szabados et al. (2007), a special type of manganese steel has been used as the material of the steam generator vessel (components 600, 606 and 607). To ensure the proper mixing, the SG heater and downcomer parts are connected directly at multiple elevations. In the uppermost connection, however, at low collapsed water level in the relevant component (605) extreme gas temperatures were observed, which often resulted in a simulation error. Therefore, in this case the single junction has been replaced with a controlled valve which closes and opens at 0.01 m and 0.02 m water levels respectively. The secondary side model also contains an ideal separator component and the piping to the PV23 relief valve and the PV22 main steam line valve.

Before the transient initiation, the circulation takes place in the bypass branch of the cold leg, which includes 7 pipes, 2 valves and the pump. It is noticeable, that the pump parameters were taken from the RELAP5 model described in Bánáti (1995), however, several changes were made in order to fulfil the model requisites. Moreover, the heat

generated by the pump has been modelled by introducing 18.5 kW thermal power until the end of the pump coast-down.

Considering the current experiment of the PMK-2 facility, the need for the adequate modelling of the downcomer head must be emphasized. Numerous complex processes take place in this small volume as the cold leg, the SIT1 and LPIS supply line and the valve modelling the break are all connected to the above-mentioned component. It is understood based on previous experience as well as practical considerations, that one-dimensional flow modelling tends to perform less efficiently in the simulation of complex, strongly three-dimensional mixing processes. During the model development we followed the exact geometry of the facility by placing all the aforementioned crossflow connections to the same elevation of 4.825 m.

The one remaining subsystem to be discussed is the pressurizer, which consists of a vertical tank, a Prizer component (Nrc, 2017a, 2017b), the connecting spray system and the heaters of the pressure control system.

Boundary conditions, such as the main steam line boundary, steam

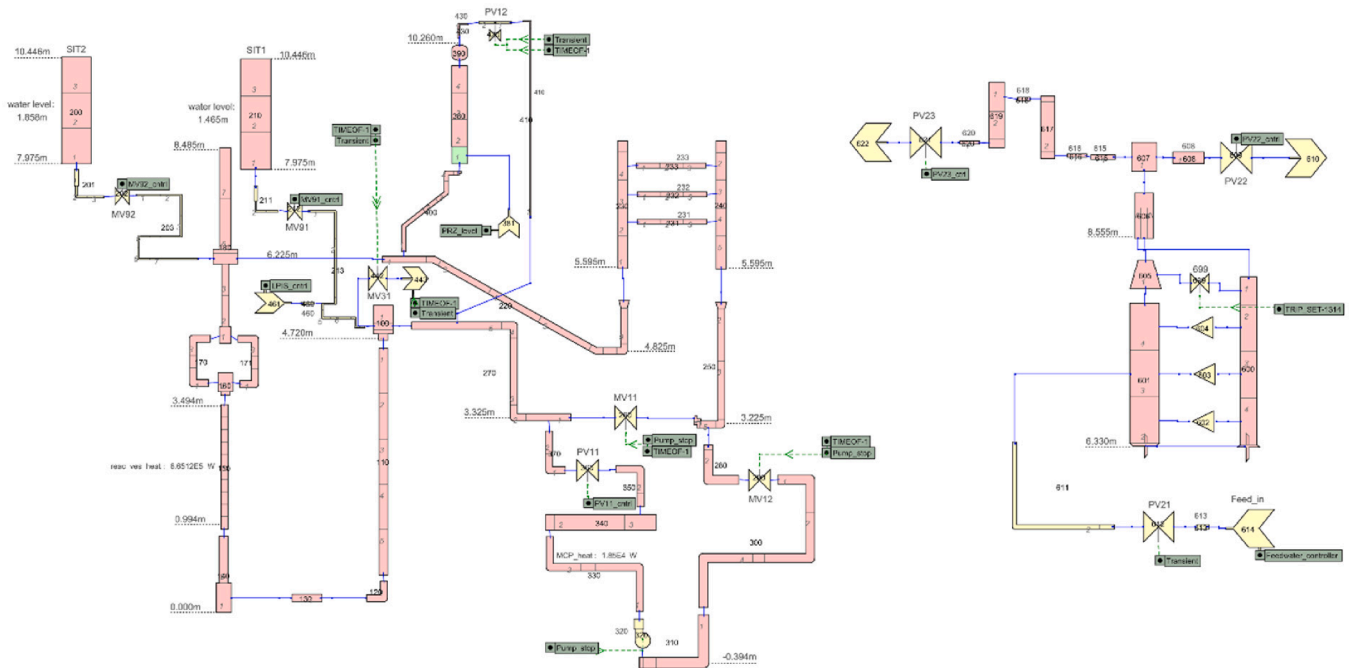


Fig. 6. TRACE model nodalization scheme.

relief boundary and the break blowdown tank are modelled with the built-in Break component of TRACE and their properties are in accordance with those of the measurements and the code guidelines in the manual. Heat structures, which are meant to connect the hydraulic components with the environment have constant heat transfer coefficient (HTC) and sink fluid temperature (SFT) outer surface boundary conditions. The SFT is set to 293 K, while the HTC values were calculated so that the simulated heat losses approximate the measured heat loss distribution (IAEA TECDOC-848, 1995).

In case of the so-called stagnant nodes (where there is no flow through the volume), such as the top of the upper plenum and the uppermost node of the downcomer, the heat-transfer coefficients towards the environment were set to zero during the steady state simulation in order to prevent the liquid from cooling down to ambient temperature. These values were reset to their nominal values at the start of the transient. The uppermost nodes of the steam generator collectors also form stagnant volumes, but from here the heat is transmitted to the secondary side; therefore, omitting the heat transfer would result in an erroneous approximation of heat transfer between the primary and the secondary side.

Choke flow (critical flow) models are used for both the steam relief (PV23) and break (MV31) valves. Since the behaviour of the primary and the secondary loop heavily depends on the total mass flow leaked through the openings, it is crucial to find the optimal settings for the simulation. In the presented, best-fitting TRACE model, the subcooled and two phase discharge coefficients were set to 1.2 and 1.0 for valve MV31. For valve PV23 the discharge coefficient was set to 0.4 in both regimes.

4.3. APROS model

The Finnish APROS thermal-hydraulic system code has been used successfully both for educational purposes and in research projects at the Institute of Nuclear Techniques of BME for decades. In 2013, a PMK-2 APROS model was built for a different benchmark case, namely for the SPE-2 experiment (Csige et al., 2013) as part of a four-year research program. This model served as a good basis for the current study, but it was developed in an older APROS 5.11 version, which is not supported anymore. Consequently, it was decided that the model should be

reconstructed in APROS version 6.08. Although the two models are similar, some modifications were necessary. The most important changes were related to the different properties and available sub-systems (such as the emergency core cooling systems) of the experiments that each model aimed to simulate. Then, some adjustments were also needed due to the features of the new version of the APROS code. Finally, minor modelling errors were also discovered in the old model that needed to be remedied.

The completed model includes all the main components of the PMK-2 facility as it is presented in Fig. 7. The model contains no intentional modification of geometric parameters, the dimensions, position and orientation of the components was defined according to Szabados et al. (2007); IAEA TECDOC-848 (1995).

The model consists mainly of HEAT_PIPE and HEAT_TANK modules, and special components such as the DESIGN_REACTOR, STEAM_GENERATOR_HORI, PRESSURIZER and ACCUMULATOR. The reactor core is modelled with 10 thermal-hydraulic nodes, which is the minimum value recommended by the APROS user manual (APROS, 2018). The HEAT_PIPE and HEAT_TANK modules typically contain 1–1 nodes.

It was found that an accurate estimation of flow characteristics in the bypass loop was less important, so the number of computation-level nodes was set to 0 for these components. In the longer pipeline sections of the primary circuit (*CL_PIP2*, *DC_MIDDLE*, *HL_PIP3*, *SURGE_PIP1*), a finer nodalization was applied. The top of the downcomer is divided into 2 nodes.

The steam generator is modelled with the combination of a built-in horizontal steam generator, a HEAT_PIPE and a HEAT_TANK. The first represents the larger, bottom volume with the primary collectors and heat transfer tubes of the SG (Fig. 3). The pipe and tank components model the upper, slimmer cylinder of the real steam generator. An extra branch ensures the circulation of coolant in the upper part. The minimum recommended number of nodes was used in the built-in SG component, while the pipe is divided into 4 nodes (Fig. 8). The 82 heat transfer tubes of the facility are modelled by 3 tubes, each representing one batch containing one third of the physical heat transfer tubes. Due to the high initial water level, a significant amount of water could be entrained by the steam on the outlet, which is prevented by activating the phase separation in the tank.

The heat capacity and heat losses of structural materials are much

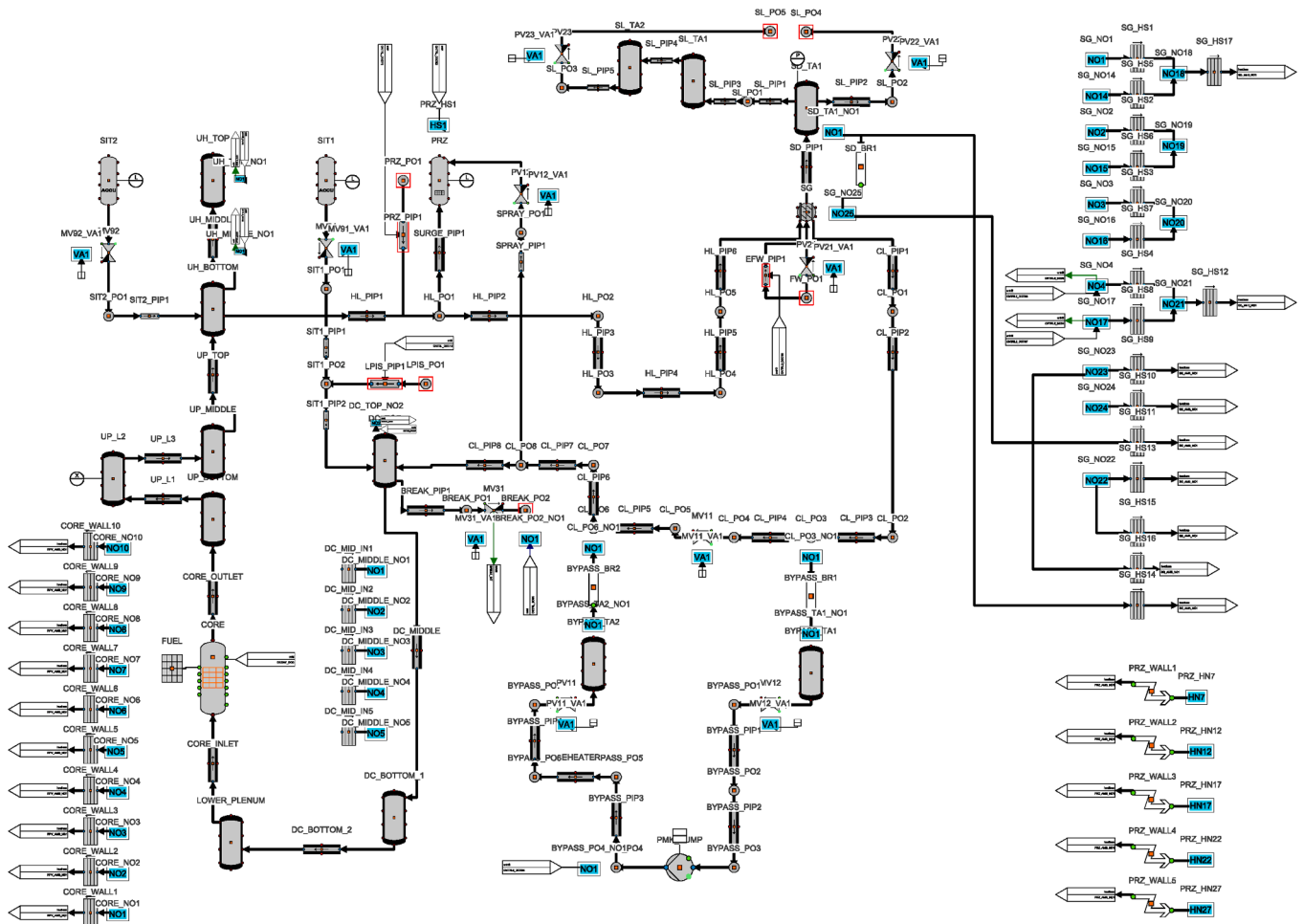


Fig. 7. APROS model of the PMK-2 facility.

more important in a small experimental facility like the PMK-2 than in a power reactor, therefore – with a few exceptions – the thermal hydraulic nodes were connected to the environment by defining heat-structures for them. The HEAT_PIPE and a HEAT_TANK modules include these, while some additional ones were needed in special places, like the core and the steam generator.

Valves are modelled by BASIC_VALVE modules, because the CONTROL_VALVE modules require the nominal pressure drop as an input parameter, which is not available. Their task is performed by separate control circuits as presented later. The break valve MV31 is connected horizontally to the top of the downcomer through a pipe in a similar way that it was constructed in the experiment.

In APROS, boundary conditions can be set using external points and pipes, which are indicated by the red frames in Fig. 7. These are required for the secondary in- and outlet boundary, and to provide the low pressure injection and the emergency feedwater injection. The time variation of the break back-pressure is also defined in an external point. To model the system heat losses, the modules with heat structures were also connected to external points filled with non-condensable gas at 20 °C temperature and 1 bar. The effect of the unknown thermal insulation was taken into account by reducing the heat transfer coefficient calculated by APROS for the outer surface of the uninsulated heat structures of the model by constant multiplication factors to obtain the total heat loss and its distribution (published in Szabados et al. (2007); IAEA TECDOC-848 (1995)).

The Moody critical flow model was used for the MV31 (break) and PV23 (steam relief) valves. A parametric study of the discharge coefficient gave the best behaviour using the value of 0.9 for the break.

However, the case of the steam relief valve proved to be more difficult. The exact conditions of the secondary bleed during the transient are not known, so a constant boundary pressure was assumed in the model. The test calculations have shown that the trend of the secondary pressure change is not adequate with a constant discharge coefficient. The measured curve has a gradient change at around 2.7 MPa, which is not simulated by APROS using a single coefficient. Consequently, the trend is either too slow in the first part, or it is too fast in the second half. Therefore, in order to properly simulate the transient and the effect of the secondary side on the primary side, it is necessary to change the coefficient of PV23 once (from 0.74 to 0.43) when the secondary pressure reaches the value of 2.7 MPa. These coefficients have been tuned to get the best agreement with the measured pressure curve. Such a change of the discharge coefficients has not been applied in case of the RELAP5 and TRACE models as their values are constant throughout the simulation.

The hydro-accumulators are modelled by the specific ACCUMULATOR component of APROS. The interfacial heat transfer efficiency of these modules needs to be increased by a factor of five partly for stability reasons and to observe the expected depletion. It was found that without this modification, the gas temperature in the components would decrease to unrealistically low temperatures (close to 0 °C), causing very low injection rates and occasional runtime error. This is probably due to the fact that the accumulator of the facility is an order of magnitude smaller than that of a real NPP, for which the calculation model has been optimized. Proper modelling of another experimental facility (PWR PACTEL) also required the augmentation of this efficiency according to paper (Kauppinen et al., 2019).

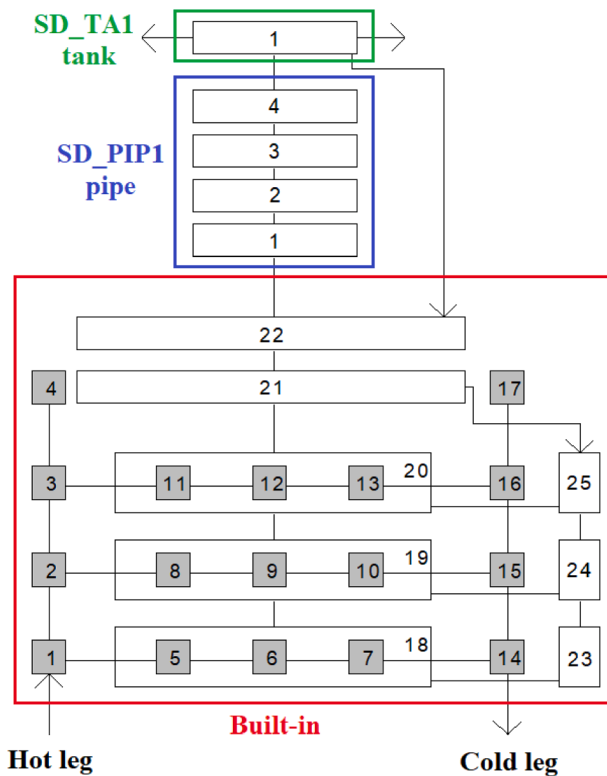


Fig. 8. Steam generator nodalization in APROS.

The reactor vessel nodes above the hot leg and the uppermost nodes of the downcomer and the primary steam generator collectors form dead-ends in 1D simulation and thus the fluid in them stagnates before the start of the transient. Due to the lack of flow, the modelled heat loss would cause the water to cool continuously down to the ambient temperature of 20 °C during the steady state calculation. This problem was solved by introducing an amount of heat equal to the heat loss into each of the nodes in question until the start of the transient.

4.4. Control systems of the models

In order to correctly simulate the transient, numerous control schemes were implemented in the models, which consist of variable and logical trips, proportional-integral (PI) control blocks, additive and multiplicative blocks and the various combinations of these units. One can distinguish two types of controls, the first of which is responsible for setting the steady-state parameters while those of the second type are required to simulate the behavior of the components during the transient.

In steady-state conditions the homogeneous, constant heating power introduced into the reactor core by the heater rods is 665 kW. The desired primary mass flow rate is achieved by controlling the flow area of valve PV11 in the bypass line. The steam generator liquid level is set by the relevant boundary condition modules through the feedwater mass flow rate. It is worth to mention that the measured variable, which serves as reference for the control block, is the collapsed level in the downcomer of the SG, instead of its riser. The reason for this choice is that otherwise a significant quantity of liquid would be pushed into the separator of the model and the required level could not be reached. The secondary pressure is controlled by the PV22 steam line valve using PI controllers like the previous ones. Based on Csige et al. (2013); 18.5 kW of heat (generated by the pump) is inserted to the node downstream of the pump until the end of the coast-down in TRACE and APROS, and until the end of the steady-state in RELAP5.

Both the nominal primary pressure and the pressurizer water level

are - achieved by PI controllers. The pressurizer model includes a spray system connected to the cold leg, which, by letting through a constant, minor mass flow through valve PV12 ensures that in steady-state simulation the pressurizer tank does not behave as a stagnant volume. The pressure is controlled with the electrical heaters. In the experiment and in the RELAP5 and APROS models, the pressurizer operated with a maximum of 3200 W heating power. It is noticeable, however, that in the TRACE simulation this setting caused prolonged steady-state runs. Therefore, this model allows the pressurizer to operate with a heating power of up to 5000 W, but the final steady-state value is below the real maximum of 3200 W. In addition, a connected boundary line to the lowermost node of the pressurizer sets its water level and thus the initial inventory to the nominal value. In case of APROS, this connection caused unstable pressurizer behaviour, so it was moved to the lower end of the surge line.

The transient is initiated once the break valve (MV31) is opened. The signal of this event disables some of the aforementioned controls by closing valves PV12 (pressurizer spray) and PV22 (main steam line), terminating the feedwater flow and the operation of the pressurizer heaters. Shortly after the break the reactor SCRAM starts and the heating power follows the characteristic exponential decay heat curve, in the calculations given as a data table. Having reached the pressure setpoint, the 150 s long pump coast-down is initiated. Throughout this period of time, the stem position of PV11 follows a predefined curve. At the end of this process, the bypass line is isolated by closing valve MV12 and opening valve MV11, and thus natural circulation develops in the primary loop. Moreover, the operation of the pump is terminated simultaneously.

Emergency core cooling systems (ECCS) are present in form of two safety injection tanks (SIT1 & SIT2) and a low pressure injection system (LPIS). In case of the hydro-accumulators, reaching of the predefined pressure setpoint results in opening MV91 and MV92 valves, which are forced to close at water levels of 0.245 and 1.035 m, respectively. The LPIS is intended to maintain the flow rate necessary for the cooling of the core in the later stages of the transient. The operation of this system is initiated when the primary pressure drops below 1.04 MPa. From that point on, the LPIS delivers a constant mass flow of 0.042 kg/s.

The secondary side bleed is started right after the pump coast-down by opening the steam relief valve (PV23). Low secondary side pressure initiates the emergency feedwater injection of the same mass flow rate as the LPIS. Both the secondary side bleed and feed persist throughout the transient once initiated. It should also be mentioned, that the secondary side pressure limitation function of valve PV23 was only implemented in APROS and TRACE. To perform this function, the valve would open at 5.3 MPa and close at 4.9 MPa to limit the pressure, but it did not reach the opening setpoint neither in the simulations nor in the experiment. This behaviour was already discovered during the trial runs and thus the limitation function of PV23 was omitted from the RELAP5 model.

In order to compare the models and the experiment, numerous measurements were simulated in a relatively straightforward way in most cases. When calculating the differential pressures, the hydrostatic pressure difference resulting from the elevation change was also taken into account. Surface temperatures were extracted from the corresponding heat structures of the given nodes, although it has to be considered that there was no information about the radial positioning of the transducers used in the measurement. In the APROS model, the simulation of 52 measurement signals was performed with the built-in SCL scripting language.

4.5. Qualification of the models and evaluation of the steady-state conditions

In the qualification of model nodalization the three most important factors to consider are geometrical fidelity, the accuracy of steady-state parameters and finally the satisfactory transient behaviour (Bonuccelli et al., 1993; D'Auria et al., 2000). The geometry and the steady state

conditions are evaluated below, while the transient behaviour is examined in the following chapters.

The first 11 points of the acceptability table of Bonuccelli et al. (1993); D'Auria et al. (2000) define the criteria concerning geometrical fidelity, which is verified for our three models in Table 1. It is important to note that the explicit reference values for the facility are not available, so these values were calculated on the basis of technical drawings and data tables of the nearly 30-year-old experiment (Szabados et al., 2007; IAEA TECDOC-848, 1995). According to the results, all three models are within the acceptable range and only the non-active structures' heat transfer area of the RELAP5 model is close to the limit. This latter is mainly due to the abandonment of the heat structures of the steam relief line leading to the PV23 valve in RELAP5, which is however described by the APROS and TRACE models. The core heaters and thus the area and volume of active structures have been modelled perfectly within the accuracy of the available data.

The further six points of acceptability given in Bonuccelli et al. (1993); D'Auria et al. (2000) are component-based and therefore could not be described with a single error value, or it was not possible to determine an exact error for them, therefore these points are discussed here:

5. *Non-active structures heat transfer volume*: In the absence of an exact reference value, we could not accurately determine a reference from the available data mainly due to the (30 years old) documentation uncertainty of some wall thicknesses of the experimental facility. However, we performed the modeling with the best known thicknesses, while the heat transfer area meets its criterion of 10%, so it is justly expected that the relevant 14% criterion of the HS volume is also met.
7. *Volume vs. height curve*: Component elevations are perfectly modelled, while the volumes have less than 2% discrepancy. Consequently, the volume vs height curve is appropriate.
8. *Component relative elevation*: Elevation of the components are modelled well within the error criterion.
9. *Axial and radial power distribution*: The core had a uniform power distribution both in the experiment and in the calculations.
10. *Flow area of valves, pumps, orifices, etc.*: The area of special components have been modelled according to available data.
11. *Generic flow areas*: The data available for the core inlet and outlet is ambiguous but it is estimated that the resulting error is within the prescribed limit.

Based on the above evaluation, it can be concluded that all three models have the appropriate geometrical fidelity to simulate the SPE-4 experiment.

Steady-state nominal conditions were achieved by a long transient run with the break valve remaining in closed position. The reason for this is that APROS is not able to perform steady-state calculations directly, while no separate steady-state models have been built in case of RELAP5 and TRACE. The measured and simulated initial conditions are listed in Table 2. A good agreement is observed with all of the codes as almost all parameters are within the measurement uncertainty ranges.

Table 1
Geometrical acceptability evaluation of the models.

Quantity	Relative error [%]			
	Acceptable	RELAP5	TRACE	APROS
1 Primary circuit volume	1.0	0.80	0.68	0.53
2 Secondary circuit volume	2.0	1.54	0.55	0.92
3 Non-active structures' heat transfer area (overall)	10.0	9.73	0.13	1.82
4 Active structures heat transfer area (overall)	0.1	~0.0	~0.0	~0.0
6 Active structures heat transfer volume (overall)	0.2	~0.0	~0.0	~0.0

In case of a few temperature measurement points of APROS and TRACE, the error exceeds the range of measurement uncertainty, but all of them are still within the criteria set by the Table 1 in (D'Auria et al., 2000).

It has to be emphasized that both here and during the following qualitative and quantitative analysis of the results, the used identifications (RELAP5, TRACE, APROS) are related to the specific calculation (composed by the code, the nodalization, the user choices, etc.) and not only to the code.

5. Simulation results of the SPE-4 experiment with RELAP5, TRACE and APROS

The SBLOCA processes and simulation results are illustrated in Fig. 9 - Fig. 19, while a comparison of the occurrence of main events are presented in Table 5. Based on these, the transient can be divided into 4 main stages as listed in Table 3.

Phase 1: The transient begins with the opening of the MV31 break valve. A single-phase outflow is observed first, while the primary pressure (Fig. 9) drops sharply. The break mass flow is limited by the critical flow, for which different models were used and different discharge coefficients were set. For subcooled liquid, both RELAP5 and TRACE use a modified form of the Burnell model, while it is based on the isentropic expansion assumption in APROS. For two-phase calculations, RELAP5 and APROS use the Ransom-Trapp and the Moody model respectively, while in TRACE the critical mass flux is calculated assuming thermal equilibrium and slip between the phases that maximises the mass flux (Table 4).

According to the evolution of the integral break mass (Fig. 11), the subcooled flow and the later one-phase steam flow is well predicted by all of the codes, while the two-phase flow shows a slightly different behaviour starting at around 44 s. However, this is not only the result of the different critical flow models, but is highly dependent on the general system response and the modelling accuracy of the complex, three-dimensional flow and mixing in the downcomer top region. In this phase, TRACE shows the best agreement.

The evaporation that starts at the highest points of the primary circuit breaks the rate of the rapid pressure drop and a temporary stagnation can be observed. This process is well described by all of the codes. However, it occurs more intensively and keeps a slightly higher pressure in RELAP5 and APROS, suggesting higher void fractions, which is in accordance with the smaller break flow rate. During the first phase, the decreasing pressure activates the reactor SCRAM at around 4 s, and the pump coast-down at around 10 s, which the codes estimated to occur a bit earlier.

Concerning secondary side, the steam line and feedwater line of the steam generator are isolated 6 s after the start of the transient which leads to an initial rise of pressure (Fig. 10). However, the increasing pressure does not reach the opening set-point of the relief valve PV23, because the direction of heat transfer in the SG tubes is reversed. Each code overestimates the magnitude of this peak and underestimates the slope of the drop following it. This may indicate that the heat transfer from the steam generator towards the primary side and the environment is not properly modelled. However, the same behaviour is observed in the results of the organizations participating in the original code validation project related to the SPE-4 experiment (IAEA TECDOC-848, 1995). These experiences suggest that some unknown processes may be responsible for this effect, for example a leakage through one of the secondary valves in the original experiment as proposed by József Bánáti (Bánáti and May, 2020).

Phase 2: At the beginning of this period, the pump coast-down is finished and the secondary bleed is initiated by the operator simultaneously. These events and the continuous accumulator injection result in a series of complex thermal-hydraulic processes. The secondary pressure decreases continuously because of the bleeding, which is tuned by the discharge coefficients applied on valve PV23 (see Table 4).

A persistent decrease of the collapsed levels is observed in the

Table 2
Measured and calculated steady-state conditions.

Parameter	ID	Unit	Measurement	RELAP5	TRACE	APROS
Upper plenum pressure	PR21	MPa	12.329 ± 0.05	12.30	12.36	12.33
SG secondary pressure	PR81	MPa	4.559 ± 0.02	4.58	4.55	4.56
Core power	PW01	kW	665.12 ± 3.0	665.00	665.12	665.12
Cold leg mass flow rate	FL53	kg/s	4.912 ± 0.06	4.91	4.91	4.91
Pressurizer water level	LE71	m	9.124 ± 0.02	9.12	9.12	9.12
SG collapsed level	LE81	m	8.954 ± 0.05	8.94	8.95	8.95
Core inlet temperature	TE63	K	540.1 ± 1.0	540.7	539.3	538.8
Upper plenum temperature	TE22	K	566.2 ± 1.0	566.8	565.5	564.5
SG inlet temperature	TE41	K	566.7 ± 1.0	566.6	565.2	564.1
SG outlet temperature	TE42	K	539.4 ± 1.0	539.4	539.0	538.5
SIT-1 pressure	PR91	MPa	6.053 ± 0.05	6.05	6.05	6.04
SIT-2 pressure	PR92	MPa	6.043 ± 0.05	6.04	6.04	6.03
SIT-1 water level	LE91	m	9.440 ± 0.02	9.44	9.44	9.44
SIT-2 water level	LE92	m	9.833 ± 0.02	9.83	9.83	9.83

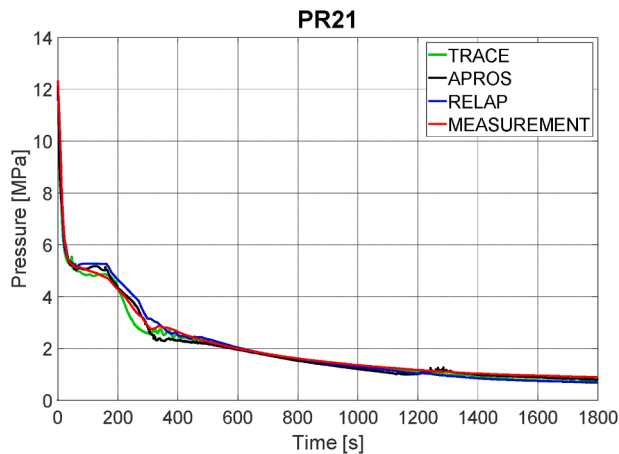


Fig. 9. Upper plenum pressure.

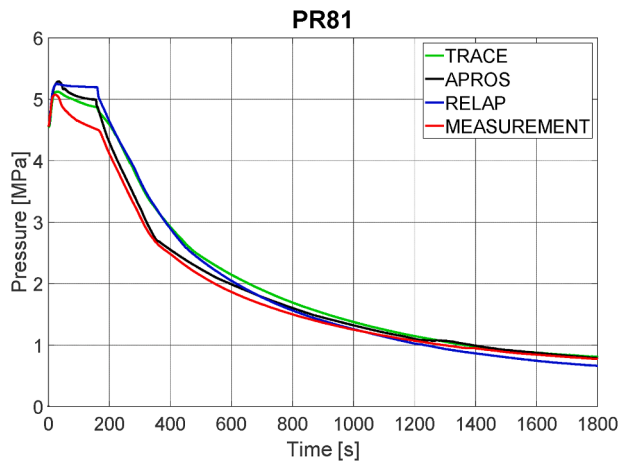


Fig. 10. Secondary side pressure.

Table 3
Main phases of the transient (time intervals are based on the experiment and differ in the simulations).

	Time interval	Characterization
Phase 1	0–158 s	Pump coast-down period
Phase 2	158–310 s	Start of natural circulation, initiated secondary bleed, intense SIT injection
Phase 3	310–345 s	SIT injection terminated, final loop seal clearing
Phase 4	345–1800 s	Loop seals are cleared, steam blow-down, core dry-out and quenching, LPIS and EFW initiation

Table 4
Discharge coefficients of the models.

	MV31 (break)			PV23 (relief valve)		
	Subcooled	Two-phase	Superheated	Subcooled	Two-phase	Superheated
RELAP5	1.0	1.0	1.0	0.46	0.46	0.46
TRACE	1.2	1.0		0.4	0.4	
APROS	0.9			0.74 (0.43)		

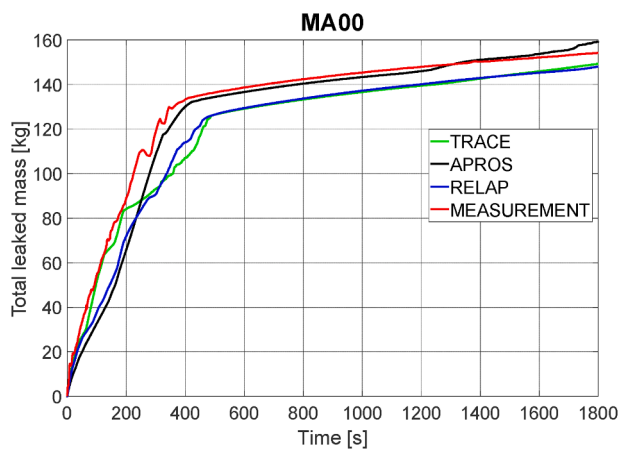
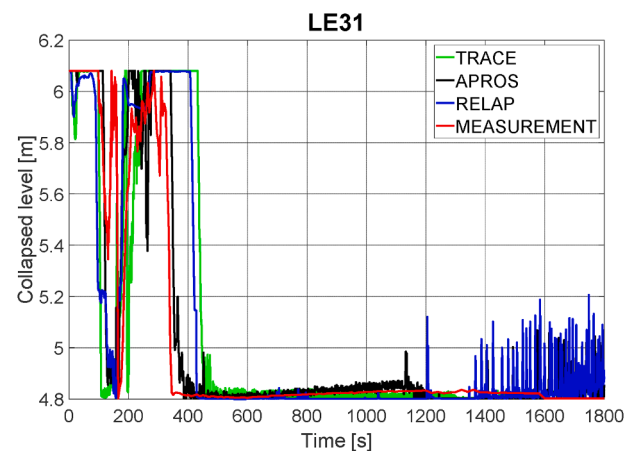
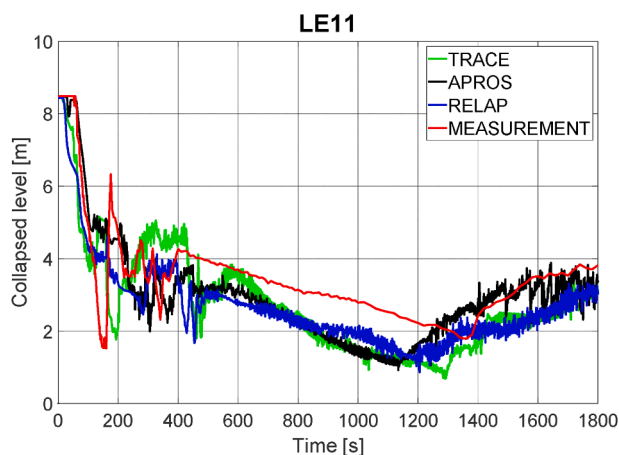
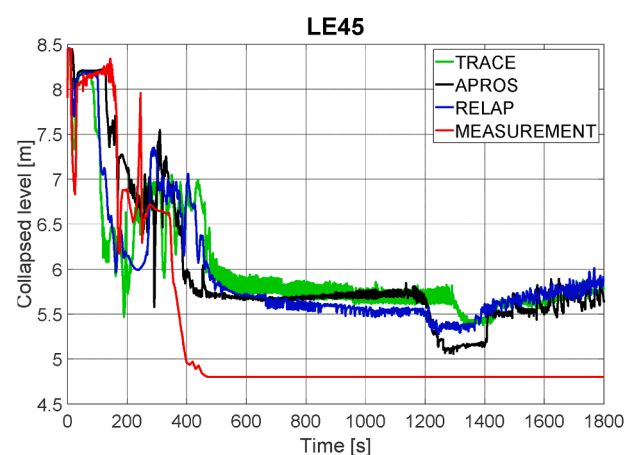
primary system (core - Fig. 12, hot leg - Fig. 13 and Fig. 14, cold leg - Fig. 15 and the downcomer - Fig. 16). The hot leg loop seal is completely cleared soon after the start of this phase at around 165 s, however, the clearing is only temporary. The steam in the hot leg is partially condensed by the subcooled ECC water, partially driven into the steam generator and condensed there. As a result, the primary pressure starts to decrease again (Fig. 9) and the injection rate of the accumulators (Fig. 17) is increased. Thus, the supplied subcooled water refills the hot-leg loop-seal. Meanwhile, the primary pressure becomes lower than the secondary and the water level in the cold leg loop seal begins to decrease until the complete emptying (~226 s). The predicted timings are not perfect, but these processes are well described by all of the codes, APROS being the closest to the measurement. After the clearance of the cold leg, the level of the downcomer also starts to decrease, which is stopped by the high intensity ECC injection. This general trend is perfectly simulated by RELAP5, while TRACE is the only one showing the earlier sudden changes after the pump coast-down. The latter is consistent with that observed at the core level (LE11), as its early local minimum is also only shown in the measurement and the TRACE simulation. This phenomenon is highly sensitive to the proper simulation of the end of pump coast-down suggesting the need for further refinement of the RELAP5 and APROS models. Thereafter, several increases and decreases of the core level is observed in all cases until the termination of accumulator injection.

The coolant and consequently the heater rod surface temperatures mainly follow the trend of the primary pressure as the fluid in the primary system is in saturated state except for the locations where subcooled water is injected. Based on the integral break mass, it can be concluded that RELAP5 and especially TRACE underestimates, while

Table 5

Times of the main events of the SPE-4 experiment and simulations.

Sequence of Events	Experiment (Szabados et al., 2007)	Experiment (IAEA TECDOC-848, 1995)	Calculation (RELAP5)	Calculation (TRACE)	Calculation (APROS)
Break valve MV31 starts opening	0 s	0 s	0 s	0 s	0 s
SCRAM is initiated at	4 s	–	3.3 s	2.7 s	2.0 s
Pump coast-down	10.0–158.0 s	–	9.1–159.1 s	7.7–157.7 s	7.5 – 157.5 s
Injection from SITs begins	26.0 s	26 s	23 s	20 s	27 s
Break flow two-phase	–	44 s	34 s	43 s	44 s
Collapsed level in the reactor vessel reaches the top of the core	–	125 s	185 s	161 s	233 s
Core heat-up begins	(138), 1127 s	138 s	1055 s	(483), (934), 1019 s	(240), (336), 692 s
Hot leg loop seal clearing	165, 345 s	165 s	157, 430 s	108, 196, 460 s	130, 356 s
Cold leg loop seal clearing	226 s	250 s	191 s	189 s	256 s
Primary pressure equals secondary pressure	248 s	–	(54), 214 s	(61), 184 s	(38), 260 s
SIT-1 injection terminates	308 s	310 s	370 s	429 s	323 s
SIT-2 injection terminates	310 s	310 s	380 s	412 s	324 s
LPIS flow is initiated at	1380 s	1380 s	1192 s	1274 s	1120 s
Maximum cladding temperature	1385 s	–	1202 s	1297 s	1235 s
Heater rod surf. temp. >873 K	731 K	–	724 K	801 K	871 K
SG secondary pressure <0.8 MPa	–	No	No	No	No
		1720 s	1505 s	No	1770 s

**Fig. 11.** Integral break mass.**Fig. 13.** Collapsed liquid level in the hot leg loop seal (RPV side).**Fig. 12.** Collapsed liquid level in the core.**Fig. 14.** Collapsed liquid level in the hot leg loop seal (SG side).

APROS follows the measured break flow rate. It is strongly influenced by the one-dimensional modelling of the complex, three-dimensional flow and mixing of the hot steam and subcooled ECCS water in the top of the downcomer, as mentioned before. In case of RELAP5 and TRACE, certainly less ECCS water escapes through the break, however, it is

caused partly by the injection characteristic itself. It is mainly accurately modelled by all of the codes, but at the end it slows down slightly in case of RELAP5 and TRACE (see Fig. 17). This later phenomenon originates primarily from the earlier refill of the downcomer.

Phase 3: The next short, but typical period of an SBLOCA in a VVER

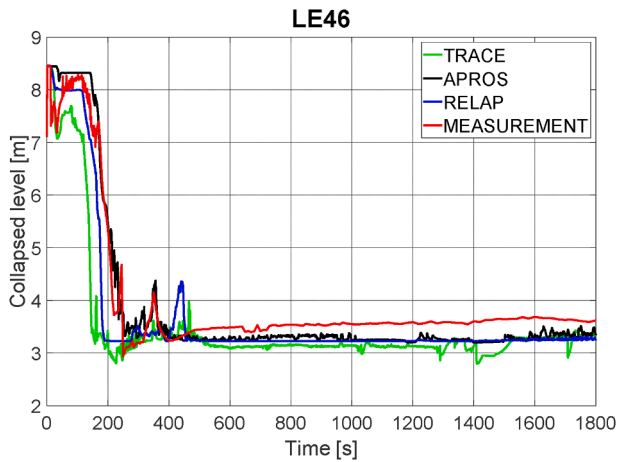


Fig. 15. Collapsed liquid level in the cold leg loop seal.

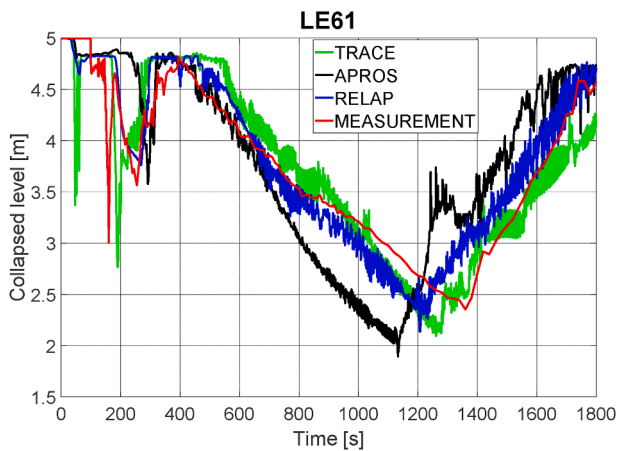


Fig. 16. Collapsed liquid level in the downcomer.

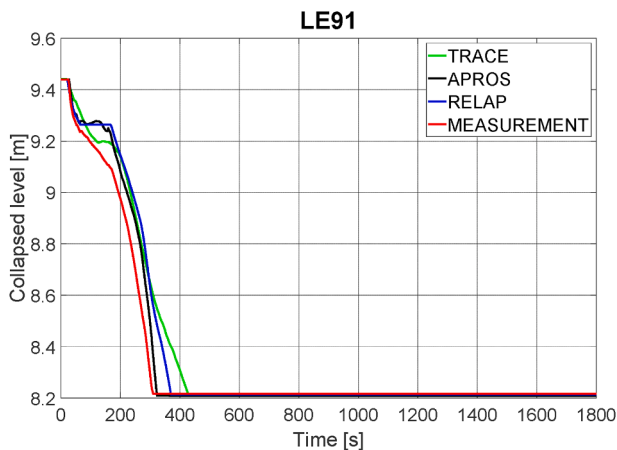


Fig. 17. Liquid level of the SIT1 accumulator.

type reactor (or in a facility similar to the PMK-2) starts when the accumulator injection is terminated. The filled hot leg loop seal prevents the steam generated in the core from passing through the steam generator and reaching the break. As a result, the upper plenum pressure (Fig. 9) increases and pushes the coolant out of the loop seal and out of the core. Certain amount of the hot leg water is transferred to the cold leg causing a temporary level rise (Fig. 15). The water squeezed out of

the core completely refills the downcomer. This process continues until the hot leg loop seal is emptied and the steam reaches the break. Then, the earlier core level is regained.

The behaviour presented above can be observed in the results of each code, but there are some discrepancies. The timing is quite well described by APROS and slightly delayed (~ 80 s) in RELAP5 and TRACE caused mainly by the also delayed termination of the accumulator injection. The rise of the core level is underestimated by all of the codes by 0.5–1.0 m, which has an important impact on subsequent processes. This missing amount is most likely trapped in the steam generator side of the hot leg loop seal as seen in Fig. 14. This assumption is also confirmed by the perfect modelling of the integral break mass (Fig. 11) in APROS, since the total inventory must be the same. However, there is a 9 kg shortage of integral break mass in RELAP5 and TRACE, while the core, and loop seal levels are quite close to that simulated by APROS. This discrepancy can only be explained by the following uncertainty: no measurement data is available concerning the ratio of steam and water excluded from the system in the bypass branch, which might differ significantly between the experiment and the individual simulations.

Phase 4: After the second, permanent drainage of the loop seals, a single-phase steam outflow is observed at the break, while the primary and secondary pressures decrease slowly. As a result of the lack of ECC injection, the collapsed water level of the core and downcomer decreases continuously as well.

The cooling of the heater rods is provided by the two-phase medium for a long time, but eventually the dry-out occurs in the upper section (Fig. 18). This process can be observed in each code, but it occurs at different water levels and at different times as presented in Table 6.

APROS is close to the measured water level, but the dry-out is too early because of the lower liquid level at the end of Phase 3. The results of RELAP5 and TRACE suggest a different flow regime as a much lower collapsed water level is seen, and thus their timing is much closer. In all cases, the rod surface temperatures start to rise due to the poor heat transfer towards the steam. The nature of the temperature change is well simulated by APROS and TRACE, while it is slightly faster in RELAP5.

Meanwhile, the primary and secondary pressures reach the set pressure of the low pressure injection (1.04 MPa) and the emergency feedwater injection (0.93 MPa), respectively. It is important to note, that the slope of these pressure curves is lower in the final stage and thus the timing of LPIS and EFW injection are highly sensitive to simulation discrepancies. Thanks to the operation of these systems, the collapsed levels of both the primary and secondary circuits start to recover leading to the rewetting of the fuel rods. The temperature maximum is best simulated by RELAP5, while it is overestimated by TRACE and especially by APROS, however it remains below 873 K in all cases. The rewetting at high temperatures results in an intense process in APROS, the effect of which appears in most figures, but it is most spectacular in the upper

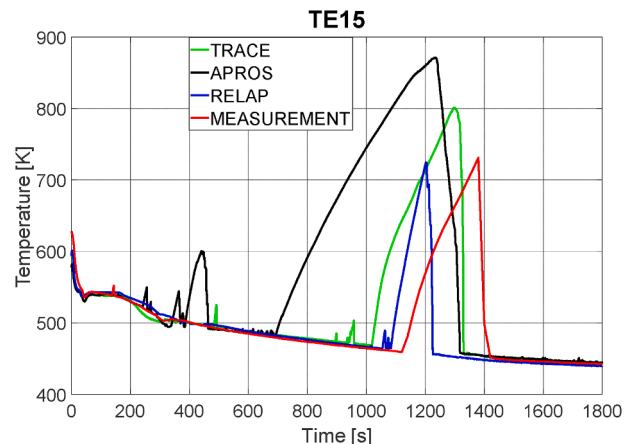


Fig. 18. Fuel rod surface temperature.

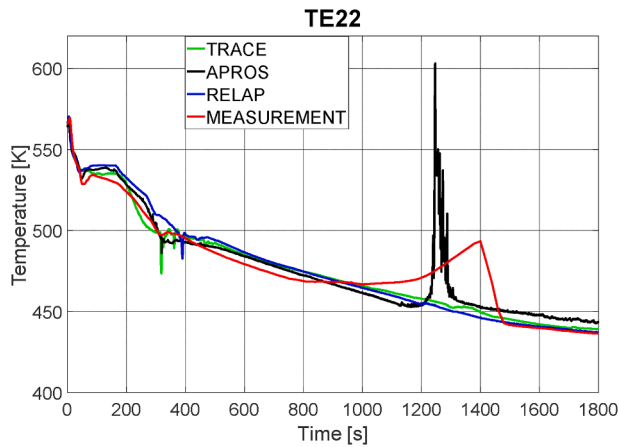


Fig. 19. Upper plenum coolant temperature.

Table 6

The collapsed water levels of the core and the simulation time, when the dry-out occurs.

	Water level [m]	Timing [s]
Experiment	2.5	1120
RELAP5	1.6	1080
TRACE	1.6	1020
APROS	2.7	700

plenum temperature (Fig. 19). Due to the release of energy previously accumulated in the fuel rods, a temporary, but significant temperature increase is observed in APROS calculation and in the experiment. Such a phenomenon is not seen in the upper plenum of RELAP5 and TRACE.

On the long term, cooling of the core is ensured, the decay heat is dissipated by the coolant leaving through the break and the secondary relief valve, while the low pressure and emergency feedwater injection provide the required supply of coolant.

Based on the qualitative analysis performed above, all three models are generally suitable for the modelling of the SPE-4 experiment. However, a quantitative analysis is also required to support this statement.

6. Quantitative assessment of the simulation results

Objective judgement on the acceptability of thermal-hydraulic models has always been important and thus many methods have been developed over the years. One of the oldest and most widely used one is the Fast Fourier Transform Based Method (FFTBM) developed originally at the University of Pisa (Ambrosini et al., 1990; Leonardi et al., 1994; Bovalini et al., 1992). Another well-known method is the Stochastic Approximation Ratio Based Method (SARBM) (Islamov and Ustinov, 2000). For the evaluation of our three code calculations, a combination of these two is applied. A great advantage of these methods is that they had already been used for SPE-4 calculations by several professionals, as presented in the following subsections, providing an opportunity for the comparison.

6.1. The original quantitative methods

The FFTBM and SARBM shows the calculation discrepancies in the frequency and time domain, respectively. Detailed descriptions are given in the referred and many other papers of the authors, so just an overview is presented here, while the main focus is on the application.

Originally, two main accuracy measures have been defined in the FFT based method, the dimensionless average amplitude AA and the weighted frequency WF. The AA is more representative in most cases as

it gives the relative magnitude of the discrepancy, while WF emphasizes the relevant frequency range. Better accuracy is generally represented by low AA values at high WF.

The SARBM has one main accuracy measure called SAR, which is close to 1, if the calculation shows a good agreement, and it is close to 0, if not. However, this is the opposite of the average amplitude measure of FFTBM in nature, and thus the accuracy factor (AF = 1 – SAR) is used more often (Prošek, 2002).

The above described measures are useful for single parameter evaluations, but global measures are also required for the acceptability judgement. The weighted sum of the AAs and AFs of the selected parameters give the total accuracy (AA_{tot}, AF_{tot}) for the examined code calculations. The weighting factors (w_p) of each parameter depend on the measurement accuracy, safety relevance and relevance with respect to the primary pressure. Based on the AA_{tot} and AF_{tot} values, acceptability limits (K) can be defined as presented in Table 7 (Prošek, 2002). The same quality ranges can be applied in a single parameter evaluation except for the primary pressure. Because of its importance, a much stricter K = 0.1 limited has been set.

6.2. Applications of the original quantitative methods

The first application of the FFT based method for the SPE-4 experiment was presented in year 1995 in article (Frogheri et al., 1995). The total average amplitudes and weighted frequencies have been calculated for both the pre- and post-test calculations performed by the 'Jozef Stefan' Institute (JSI) with three versions of RELAP. In case of the pre-test calculations, two types of break configuration have been examined. Five time intervals and 22 variables were considered. The quantitative results are outstandingly good as the evaluated AA factors range from 0.1 to 0.22. However, only one of the total nine calculations was able to model the core dry-out (pre-test MOD3.1), while giving the worst AA_{tot} value. This experience indicates that if the nature and the timing of the dry-out is not properly modelled, as in the case of the pre-test MOD3.1 simulation, then the discrepancy can be more significant compared to a case without dry-out occurrence. Consequently, the qualitative and quantitative analysis need to be performed together to get a full picture of the calculation accuracy. This behaviour also shows the limitations of the FFTBM.

In article D'Auria et al. (1996) all four Standard Problem Exercises have been evaluated with the FFT based method in terms of the primary pressure. The pre- and post-test calculations were performed by the University of Pisa and the ANDA/Disp with various RELAP and CATHARE versions. The results are quite good in most cases, as the AAs of the primary pressure are below 0.1. The second, great dry-out was only predicted by the post-test calculation, which also gave better AA values. It is important to highlight, that the primary pressure based single parameter evaluations are quite useful to get a quick picture of the general accuracy of the calculations, but care should be taken, because, despite the importance of the primary pressure, it cannot give a complete picture of the global accuracy on its own.

The original IAEA collaboration had participants from several countries with various thermal-hydraulic codes (IAEA TECDOC-848, 1995). A quite detailed FFTBM analysis of these calculations is presented in the (Mavko et al., 1997) article (1997), which covers both the pre- and post-test calculations by evaluating the primary pressure,

Table 7

Acceptability limits for calculation accuracy (Prošek, 2002)

Quality	FFTBM	SARBM
acceptable	AA _{tot} ≤ K = 0.4	AF _{tot} ≤ K = 0.2
very good	AA _{tot} ≤ 0.3	AF _{tot} ≤ 0.1
good	0.3 < AA _{tot} ≤ 0.5	0.1 < AF _{tot} ≤ 0.25
poor	0.5 < AA _{tot} ≤ 0.7	0.25 < AF _{tot} ≤ 0.45
very poor	0.7 < AA _{tot}	0.45 < AF _{tot}

cladding temperature and overall accuracy. Because of the limited number of time series data, a set of 12 parameters were used, which suggests a less complete analysis compared to other applications. However, the strict primary pressure limit (0.1), which only less than half of the calculations could fulfil, allowed this comparison. The bifurcation phenomenon related to the dry-out is also discussed here as several models could not predict it including the three best pre- and post-test calculations presented in (Mavko et al., 1997).

To overcome some limitations of the quantification method used previously and to extend its abilities, two new accuracy measures were proposed and used for SPE-4 in year 2002 by Prošek (2002): the number of discrepancies (ND) and the minimal variable accuracy ($VA_{\min}$). Simulation accuracy of a parameter is minimal when its accuracy measure is maximal, so $VA_{\min}$ gives the maximum of the variable accuracies (VA_i). Additionally, ND shows the number of variables with VA_i greater than the limit K. The quantitative analysis performed in Mavko et al. (1997) has been repeated in Prošek (2002) supplemented by the new measures and the SARBM. The expanded FFT based method proved to be effective, as it was able to indicate the discrepancies of the cladding temperature TE15 through the $VA_{\min}$ measure and improve the overall picture with the ND value, which is higher in the less acceptable calculations. The SARBM results are generally in good accordance with the FFTBM's, but the variable with minimal accuracy is the core differential pressure DP11 in most cases, instead of the cladding temperature. This is mainly due to the intrinsic characteristics of the methods.

Unfortunately, the articles summarized above do not present all of the required information to perform the comparative FFTBM and SARBM analysis with our own RELAP5, TRACE and APROS calculations. However, a Microsoft Excel Add-in was developed and validated with SPE-4 calculations for accuracy evaluation, the results of which were presented in 2003 in Prošek and Mavko (2003). The paper includes all of the information required for repetition, which gives a good opportunity for the comparison here. This software incorporated both of the discussed methods and provides a much easier input preparation and use compared to the older Fortran code. For the validation process of Prošek and Mavko (2003), three post-test calculations (UKRP, SIEP, PZ1P see in Table 8) were selected from the list of Prošek (2002). These models were able to predict the core dry-out and their results were in the first half according to the total average amplitude.

Our Institute was given access to the new Excel Add-in and permission to use it for the evaluation from Andrej Prošek (JSI). So, the quantitative analysis could be performed for our three calculations with the same settings (time windows, weighting factors, etc.) and compared to the results of (Prošek and Mavko, 2003). The values of the original accuracy measures for the first 1500 s of the transient is shown in Table 9, while the new accuracy measures ($VA_{\min}$, ND) are summarized in Table 10 for all three time windows.

The total average amplitudes suggest 'very good' quality in all six cases, but the UKRP and APROS calculations has slightly failed on the primary pressure criterion (see in Table 9). During the qualitative analysis, APROS was found to be accurate in the pressure prediction as shown on Fig. 9, so this finding will be investigated further in this article. Concerning the single average amplitudes, more than half of the parameter calculations are excellent, while the cladding temperature (TE15), the break mass flow rate (FL01), the core level (LE11) and

pressure drop (DP11) has a higher discrepancy in accordance with the qualitative analysis. The DP11 and FL01 prediction of TRACE is outstandingly poor, so it will also be investigated further in this paper. The average amplitudes of TE15 give a perfect illustration of how sensitive the FFTBM method is to proper modelling of the timing and the peak temperature. Another interesting observation is that the upper plenum temperature prediction of APROS seems to be the weakest among the new three, while it was the only one able to model the rise (Fig. 19). This behaviour is analogue to the dry-out issue discussed in (Frogheri et al., 1995; Mavko et al., 1997).

The new measures confirm the previous findings and show that typically 1–3 parameters fail the criterion, the most significant of which is the cladding temperature after the onset of the core dry-out, and the core level or pressure drop beforehand. It also highlights that APROS gives the best global prediction until the dry-out, but it starts too early resulting in a jump of the related measures.

Generally, it can be stated that based on the quantitative comparison the new RELAP5 model gives the most accurate results, while APROS and TRACE are between the referred, older ones. Most of the parameters are better predicted by the new models, the main exception of which is the cladding temperature. It is important to note however, that there are some known errors in the time record data of SIEP and PZ1P according to the final report (IAEA TECDOC-848, 1995) and the article (Prošek and Mavko, 2003). The accumulator water level (LE91) data of SIEP is incorrectly zero throughout the simulation, while the hot leg water level (LE31) signal of PZ1P is shifted presumably due to the incorrectly set reference level. These are also indicated by the related AA values. Unfortunately, the effect of these errors on the total average amplitudes cannot be determined on the basis of the available information.

6.3. Improvement of the quantitative methods and their application

As presented in the previous section, the original FFTBM and SARBM methods were used several times to evaluate the SPE-4, and many other experiment calculations. However, the FFTBM was often criticized for its consistency problems and its strange behaviour for example in the case of signals with the shape similar to triangles (e.g. core dry-out and quenching).

To overcome these weaknesses of the method, new improvements were proposed later (Prošek and Mavko, 2009; Prošek and Leskovar, 2011), the first of which was the use of the time dependent accuracy. The FFTBM methodology requires the subdivision of the transient into phenomenological windows. Previously, just a few (3–5) windows were selected as seen also in Table 10, but the use of a series of narrow windows (~40) instead can provide much more additional information. This gives a possibility to visualize the time dependency of the accuracy measures and identify those time intervals and events when the largest discrepancies occur and how they influence the global picture.

Thanks to the application of the new time dependent accuracy, it was found that the original FFT based method gives incorrect judgement for integral and monotonically increasing or decreasing signals. This problem is caused by the so called edge effect, which was hidden previously (Prošek and Leskovar, 2011). The Fast Fourier Transformation by definition processes an infinite signal, which is achieved by the periodical extension of the real, finite data series. The significantly different initial and final values thus cause a discontinuity (edge) in the infinite signal and introduces several artificial harmonic components in the frequency domain. It influences the sum of the amplitudes and consequently the calculated accuracy measures, but its contribution is unpredictable and it may even overshadow real effects. A detailed discussion of this issue is presented in (Prošek and Mavko, 2009; Prošek and Leskovar, 2011).

The authors have also proposed a solution for this inconvenient drawback and named the new method 'improved FFTBM by signal mirroring' and its result AA_m instead of AA. As its name suggests, the problem can be resolved by eliminating the artificial edges by mirroring the signal before the application of FFT. The LOFT L2-5 test (OECD/

Table 8

Information summary of the calculations used for comparison (based on Mavko et al., 1997).

Label	Country	Organization	Code
UKRP	Ukraine	Ukrainian Scientific and Technical Centre	RELAP5/MOD3.1
SIEP	Germany	Siemens AG	RELAP5/MOD2.5
PZ1P	Croatia	University of Zagreb	RELAP5/MOD2

Table 9

Summary of the original FFTBM results for the selected variables (UKRP, SIEP and PZ1P were taken from (Prošek and Mavko, 2003), results marked with APROS, RELAP5 and TRACE are related to our actual simulations).

Parameter	Var.	Window 0 – 1500 s					
		AA (UKRP)	AA (SIEP)	AA (PZ1P)	AA (APROS)	AA (RELAP5)	AA (TRACE)
Core pressure drop	DP11	0.777	0.803	0.704	0.569	0.471	1.380
Break flow	FL01	0.815	0.763	1.024	0.674	0.817	1.222
Reactor liq. level	LE11	0.786	0.473	0.483	0.460	0.446	0.584
HL loop seal level	LE31	0.337	0.328	0.827	0.276	0.366	0.404
CL loop seal level	LE46	0.296	0.286	0.714	0.274	0.341	0.447
SIT-1 level	LE91	0.027	1.000	0.724	0.030	0.039	0.039
Integral break mass	MA01	0.108	0.080	0.067	0.098	0.103	0.107
Primary pressure	PR21	0.112	0.092	0.094	0.126	0.054	0.078
Secondary pressure	PR81	0.093	0.002	0.075	0.064	0.097	0.077
Rod surface temp.	TE15	0.356	0.410	0.211	0.721	0.448	0.504
Upper plenum temp.	TE22	0.154	0.175	0.104	0.117	0.098	0.095
Core inlet temp.	TE63	0.062	0.106	0.070	0.048	0.056	0.063
	<i>AA_{tot}</i>	0.224	0.263	0.260	0.230	0.197	0.255

Table 10

Summary of the original FFTBM results for the IAEA-SPE-4 experiment calculations (UKRP, SIEP and PZ1P were taken from (Prošek and Mavko, 2003), results marked with APROS, RELAP5 and TRACE are related to our actual simulations).

Calculation	<i>AA_{tot}</i>	<i>VA_{min}</i>	<i>VA_{min}</i> variable	ND
Window 0–350 s				
UKRP	0.189	0.459	LE11	1
SIEP	0.229	0.600	LE91	1
PZ1P	0.247	0.505	LE31	3
APROS	0.145	0.241	LE11	0
RELAP5	0.156	0.231	LE11	0
TRACE	0.212	0.502	DP11	1
Window 0–1120 s				
UKRP	0.179	0.446	LE11	1
SIEP	0.220	0.600	LE91	1
PZ1P	0.235	0.498	LE31	3
APROS	0.277	1.788	TE15	1
RELAP5	0.164	0.371	TE15	0
TRACE	0.251	0.779	TE15	2
Window 0–1500 s				
UKRP	0.224	0.534	TE15	2
SIEP	0.263	0.615	TE15	2
PZ1P	0.260	0.497	LE31	3
APROS	0.230	1.082	TE15	1
RELAP5	0.197	0.673	TE15	1
TRACE	0.255	0.756	TE15	2

NEA, 2006) was selected for the detailed demonstration of the improved method. The most important findings of this investigation can be summarized as follows based on (Prošek and Mavko, 2009; Prošek and Leskovar, 2011).

- In case of integral and monotonically decreasing signals, the edge effect can significantly decrease the average amplitude, misleadingly suggesting a too accurate prediction. This explains the observed high accuracies of such signals in the previously referred applications.
- The very low limit value (0.1) of the primary pressure accuracy is based on SBLOCA analysis, characterized by a great pressure drop and consequently a significant edge effect. In case of other types of transients with smaller pressure change (e.g. SPE-3), the calculations were unable to fulfil the pressure requirement. This is a good indication how inconsistent the original method was.
- This original, strict pressure criterion must be used carefully with the improved FFTBM as it was confirmed by a comparison between the improved FFTBM and the ACAP software (Kunz et al., 2002) measures. The ACAP results indicate very good pressure accuracy in all calculations of the LOFT L2-5 test, while only a few of them were acceptable according to the strict pressure criterion of FFTBM.

- The influence of the edge elimination is much less significant in those time intervals, when the great changes have already taken place (e.g. in case of the primary pressure after the first, great blowdown).
- The initial and final cladding temperatures are usually close and thus the accuracy calculated with the original method could give reasonable results for the whole transient. However, the intermediate trend of the time dependent accuracy was under- or over-estimated, if the timing of the dry-out or quench was incorrect. It is also caused by the artificial edges developed during the temperature rise and explains the strange behaviour in the case of signals with the shape similar to triangles.

The demonstrations have proved that only the improved FFTBM by signal mirroring gives consistent results and the proposed solution is able to eliminate the raised weaknesses. In some cases, the original method can also be reliable, but the user has to be aware of its limitations.

The Excel Add-in used in the actual evaluation provides a choice between the original and the improved method, so the quantification analysis has been repeated for our new three calculations of SPE-4. Unfortunately, no previous application of the improved method for SPE-4 was found in the literature, so a wider comparison is not possible here. The single variable accuracy measures for the whole transient is shown in Table 11, while general accuracy measures are summarized in Table 12 for four selected time windows (0–1120 s window is not available in this case, so the results of the closest 0–1100 s window is presented).

As expected, all of the average amplitudes increased compared to the original method, but quite differently depending on the nature of the

Table 11

Summary of the improved FFTBM and SARBM results for the selected variables.

Parameter	Var.	Window 0–1800 s					
		APROS		RELAP5		TRACE	
		<i>AF</i>	<i>AA_m</i>	<i>AF</i>	<i>AA_m</i>	<i>AF</i>	<i>AA_m</i>
Core pressure drop	DP11	0.401	0.867	0.335	0.676	0.481	2.082
Break flow	FL01	0.372	0.846	0.396	1.026	0.517	1.439
Reactor liq. level	LE11	0.239	0.655	0.266	0.623	0.304	0.840
HL loop seal level	LE31	0.035	0.359	0.061	0.484	0.068	0.521
CL loop seal level	LE46	0.077	0.358	0.119	0.474	0.191	0.630
SIT-1 level	LE91	0.006	0.044	0.010	0.057	0.010	0.058
Integral break mass	MA01	0.063	0.200	0.090	0.197	0.086	0.209
Primary pressure	PR21	0.077	0.279	0.074	0.121	0.069	0.168
Secondary pressure	PR81	0.059	0.152	0.112	0.224	0.105	0.183
Rod surface temp.	TE15	0.255	0.941	0.140	0.589	0.136	0.659
Upper plenum temp.	TE22	0.024	0.170	0.028	0.152	0.025	0.141
Core inlet temp.	TE63	0.009	0.076	0.013	0.097	0.014	0.099
	total	0.094	0.335	0.090	0.289	0.101	0.369

Table 12

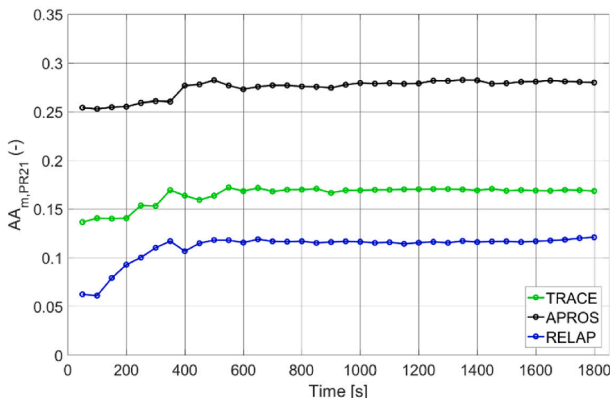
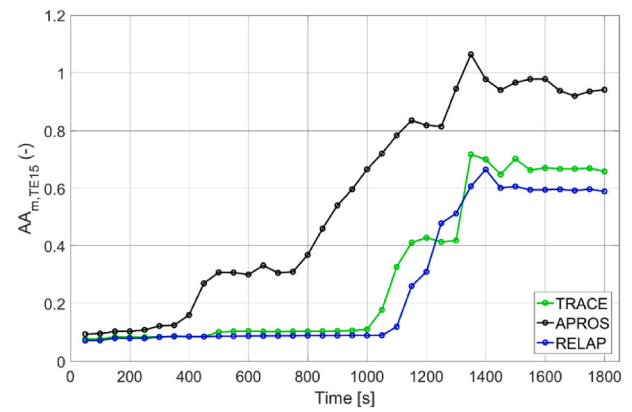
Summary of the improved FFTBM results for the IAEA-SPE-4 experiment calculations.

Calculation	$AA_{m,tot}$	VA_{min}	VA_{min} variable	ND
Window 0–350 s				
APROS	0.202	0.362	PR21	0
RELAP5	0.195	0.331	LE11	0
TRACE	0.270	0.747	DP11	2
Window 0–1100 s				
APROS	0.294	1.176	TE15	1
RELAP5	0.213	0.364	LE11	0
TRACE	0.320	0.736	DP11	3
Window 0–1500 s				
APROS	0.337	1.450	TE15	1
RELAP5	0.288	0.910	TE15	1
TRACE	0.376	1.055	TE15	3
Window 0–1800 s				
APROS	0.335	1.412	TE15	1
RELAP5	0.289	0.884	TE15	1
TRACE	0.369	0.989	TE15	3

parameter. The minimum of the ratios is 1.18 in case of FL01 (TRACE), while the maximum is 2.378 in case of PR81 (TRACE). The highest discrepancies are still obtained in case of DP11, FL01, LE11 and TE15, which fall in the ‘poor’ or ‘very poor’ category. However, cladding temperature is the only one failing in the RELAP5 and APROS calculations according to the ND values. In case of TRACE, the prediction of DP11 and LE11 cannot be accepted either. However, it is important to be aware that the failure of these parameters (especially LE11, see in Fig. 12) is mainly due to the great drops after 100 s, which was modelled only by TRACE, but with a slight delay that results in a worse accuracy measure. Furthermore, great fluctuations of the DP11 and FL01 parameters of TRACE have been observed in the period of 43–63 s. It is caused by the appearance of steam behind the control valve of the bypass line, which phenomenon could not be eliminated during the model development. Its effect on the accuracy measures will be examined later in this paper.

According to the total average amplitudes, all three calculations are still acceptable and RELAP5 remained the best, while all of them failed on the pressure criterion. The application of this strict, unchanged limit value is not considered justified on the basis of these results, since the pressures are still in the ‘very good’ category, and the general accuracy of the models was also found to be good. We would suggest the revision of this criterion and use a higher value of around 0.2 for the pressure, based on the 2.1–2.3 ratio of the improved and original AAs of PR21 in this specific case. The improved FFTBM and the SARBM results are consistent, but the second suggests better general accuracy, if the categorisation of Table 7 is used.

In the improved analysis, the total 1800 s long transient was divided

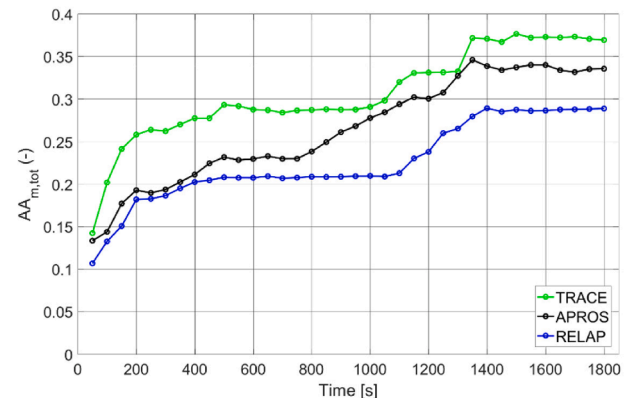
**Fig. 20.** AA_m accuracy measure of PR21.**Fig. 21.** AA_m accuracy measure of TE15.

into 50 s long time intervals providing opportunity for the evaluation of the time dependence. It is presented for the average amplitude of PR21 and TE15, the total average amplitude and the total accuracy factor in Fig. 20–Fig. 23, respectively.

The time dependence of PR21 (Fig. 20) shows that the main discrepancy is accumulated during the very first time interval (0–50 s), which is the period of the great, initial blowdown. APROS encountered difficulties in this period, while it had the smallest further increment during the rest of the transient. This example illustrates quite well, why the quantitative analysis is so important in addition to the qualitative one. Because of the very short relative time interval of the initial blowdown, the qualitative analysis did not highlight the incorrect characteristics of APROS simulation, but the quantitative did as it failed the pressure criterion. Nevertheless, the APROS calculation is still considered acceptable, because the pressure reaches an appropriate level at the end of the first time interval, and its further trend is convincing.

The cladding temperature accuracy (Fig. 21) is quite good in all calculations until the onset of the dry-out, the timing of which is however not precisely modelled. The accuracy measure increases first because of the higher calculated temperature, which is followed by a short stagnation and then by a rise again as a result of the higher measured value. In line with the previous findings, based on the FFTBM and SARBM quantitative comparison, APROS provides the weakest, while the other two provide similar accuracy, but with different characteristics.

The total accuracy measures ($AA_{m,tot}$, AF_{tot}) illustrate well that the complex events have the main contribution to the general discrepancy (Fig. 22, Fig. 23). These processes are primarily observable in the first 400 s until the end of the loop seal clearings, and then during the core dry-out and rewetting period. In the remaining phases of the transient,

**Fig. 22.** Time dependent total accuracy from improved FFTBM.

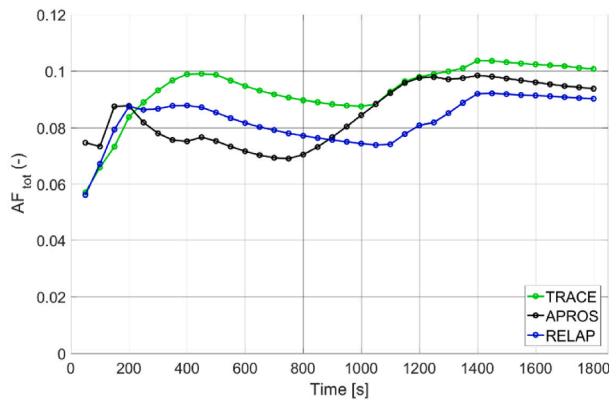


Fig. 23. Time dependent total accuracy from SARBM.

the total average amplitude stagnates, while the total accuracy factor decreases. These two measures seem to be quite consistent for RELAP5 and TRACE, while APROS is slightly different. Its accuracy is much worse according to AF_{tot} in the very first time interval, presumably due to the stronger influence of PR21 on this measure. However, APROS became the most precise after 200 s, while it falls to the second place after the simulated early, great dry-out. Meanwhile, the APROS model proves to be the second best throughout the transient according to $AA_{m,tot}$. This finding suggests that various inaccuracies have slightly different influence on these global measures. This may indicate that both methods should be applied in the evaluation of modelling accuracy.

It was previously observed that the DP11 and FL01 inaccuracy of TRACE is remarkably high, caused partly by the strong fluctuations of these parameters in the period of 43 – 63 s. It was found worthy to identify the contribution of these fluctuations to the total accuracy of the parameters involved. For this purpose, the actual calculation values of DP11 and FL01 were replaced by interpolation for the relevant 20 s long period, and the quantitative analysis was repeated. The results are compared in Tables 13 and 14 (the other 10 parameters remained

Table 13

Effect of the DP11 and FL01 fluctuations of TRACE on the improved FFTBM results (* fluctuations eliminated by interpolation).

ID	Variables	Window 0–1800 s					
		TRACE		TRACE*		Relative differences	
		AF	AA_m	AF	AA_m	ΔAF [%]	ΔAA_m [%]
1	DP11	0.481	2.082	0.451	0.918	6.24	55.91
2	FL01	0.517	1.439	0.497	1.257	3.87	12.65
	total	0.101	0.369	0.099	0.332	1.98	10.03

Table 14

Effect of the DP11 and FL01 fluctuations of TRACE on the improved FFTBM results 2 (* fluctuations eliminated by interpolation).

Calculation	$AA_{m,tot}$	VA_{min}	VA_{min} variable	ND
Window 0–350 s				
TRACE	0.270	0.747	DP11	2
TRACE*	0.227	0.437	LE11	1
Window 0–1100 s				
TRACE	0.320	0.736	DP11	3
TRACE*	0.280	0.504	LE11	2
Window 0–1500 s				
TRACE	0.376	1.055	TE15	3
TRACE*	0.338	1.055	TE15	2
Window 0–1800 s				
TRACE	0.369	0.989	TE15	3
TRACE*	0.332	0.989	TE15	2

unchanged).

According to this comparison, the fluctuations (especially in case of DP11) have significant contribution to the average amplitudes of FFTBM, while the SAR based method is much less sensitive to these. It is important for the users to be aware of this characteristic of the methods. Both of the parameters are still in the ‘very poor’ category, but none of them fail on the ND test anymore. The total average amplitude has also a great reduction in the absence of the fluctuations, resulting in a global accuracy close to that of APROS.

7. Conclusions

The SPE-4 experiment carried out on the PMK-2 facility is a cold leg, small break LOCA transient in which no high pressure injection is available. To mitigate the consequences of this beyond design basis accident, a secondary side feed and bleed operation was applied as an accident management measure. This paper has given a description of three system code models of the facility, built in RELAP5, TRACE and APROS. Finally, the simulations of SPE-4 were performed and compared to the measured data.

The SPE-4 experiment is especially suitable for code assessment because of its various and complex thermal–hydraulic processes, such as the mixing of subcooled water with high void fraction two-phase medium, the behaviour of cold and hot leg loop seals, core dry-out followed by the reflooding and the effect of the secondary side interventions on the primary side. The most challenging region of the system in 1D simulations is the top of the downcomer, as the cold leg nozzle, one of the accumulator surge lines together with the LPIS line and the break are all connected to this relatively small volume.

Based on the qualitative analysis, it can be concluded that each model reproduced well the main processes observed during the real experiment and there is a good general agreement in the results. However, there are also some important differences, one of which is that a significant amount of coolant stuck in the hot collector of the steam generator resulting in a lower core level in the simulations. The consequences of this are different in the simulations. A much earlier dry-out and higher fuel temperature peak is observed in APROS, while the dry-out occurs at a much lower core level in case of RELAP5 and TRACE. Another difference is the slightly elongated accumulator injection and the resulting delay of further processes in RELAP5 and TRACE.

A detailed quantitative analysis was also performed using the original and improved FFTBM and SARBM methods. A thorough comparison was made between the accuracy measures computed with these methods, in which previously published results were also included. The results have shown, that all three here presented calculations are generally acceptable, but a few parameters could not be modelled accurately. In accordance with the qualitative assessment, estimation of the cladding temperature, the break mass flow rate, the core level and pressure drop caused difficulties for the codes. The RELAP5 model proved to be the most accurate in all aspects.

Based on the performance of the models, further refinement, especially in their core and downcomer top region, may be suggested. For instance, the use of the 3D vessel model of TRACE might be able to improve the accuracy. A repeated experiment with more, and more accurate measurements would also considerably support the modelling of SPE-4. The reliability and accuracy of some of the measurements in the nearly 30-years-old experiment is somewhat questionable, and certain details are already obscured like the issue of the secondary pressure. Finally, we would suggest the revision of the pressure criterion of the improved FFTBM. The current limit value (0.1) is based on the original method, and it does not take into account the impact of the elimination of the edge effect. According to our results, a higher value of around 0.2 would be much more appropriate.

CRedit authorship contribution statement

Tamás Varju: Methodology, Validation, Formal analysis, Writing - original draft, Writing - review & editing, Project administration. **Ádám Aranyosy:** Methodology, Validation, Visualization, Writing - original draft, Writing - review & editing. **Róbert Orosz:** Methodology, Validation, Writing - original draft. **Viktor Holl:** Methodology, Validation, Writing - original draft. **Tamás Hajas:** Methodology, Visualization, Writing - original draft. **Attila Aszódi:** Conceptualization, Supervision, Project administration, Funding acquisition, Writing - review & editing.

Declaration of Competing Interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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