International Journal of Mathematical Archive-2(10), 2011, Page: 1809-1818
Available online through www.ijma.info ISSN 2229 – 5046

SAFETY ANALYSIS OF POWER EXCURSION ACCIDENTS FOR TRAINING AND RESEARCH REACTORS USING DIFFERENT THERMAL-HYDRAULIC MODELS

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(Received on: 15-09-11; Accepted on: 05-10-11)

ABSTRACT

It is true that most power reactors have a code system to analyze the consequences of reactivity transients but is not true for a number of training and research reactors as to the training and research reactor of Budapest University of Technology (BME). The authority's inspection is looming and new programs had to be elaborated to meet the authority requirements. In this sense two different 3D space-time dependent models with thermal-hydraulics feedback has been introduced and compared together and with point-kinetic model for this purpose. The two integrated 3D neutronic/thermal-hydraulic codes POWEX-K/MI and POWEX-K/SV together with point kinetic REMEG code has been tested and compared to analyze power excursion accidents initiated by ramp reactivity 1.2$. A comparison between two models has been given and the physical and mathematical features of both models have been investigated. The results show that the fuel parameters are maintained below specified limits during the accident so that fuel failure and release of radioactivity will not occur.

Keywords: *Momentum integral model, single mass velocity model, power excursion, reactivity accident, thermal-hydraulic, finite difference, diffusion equation.*

1. INTRODUCTION

The Training Reactor of the Technical University Budapest (BME) is the first complete nuclear reactor unit designed and constructed in Hungary after much research and development work with zero-power critical systems and on the basis of experience gained in the course of operation and utilization of a 2 MW research reactor. The BME-Reactor became critical in 1971. It is a tank-type reactor, which is located on the site of the University. It is designed to be compact and safe and it is used mainly for neutron activation analysis, production of short-lived radioisotopes and for education and training. Its maximal thermal power is 100 kW. The cylinder-shaped tank is 1.4 m in diameter and filled with desalted water. The coolant level is 5750 mm. The reactor core is made up of 24 EK–10-type fuel assemblies, which contain 369 fuel rods altogether. Their active length is 500 mm. The fuel is 10% enriched uranium dioxide in magnesium matrix. Each regular fuel assembly contains 16 fuel rods arranged in a four by four square lattice with a pitch of 17.5 mm. The reactor core is moderated and cooled by light water. The reactor core is cooled by the natural convection of the coolant below 10 kW power. Over 10 kW, the coolant is forced at a volumetric flow of 5.8 m^3/h. The reflector is graphite surrounded by water.

The coolant enters into the tank at the bottom and is led off through a pipe at about 1200 mm above the top of the core. This means that a slightly forced buoyant flow can be observed while the cooling loop works; other details can be found in references [1-3]. A summary of the design and thermal-hydraulic parameters of the BME-Reactor core is given in Table 1.

Table 1: Design parameters of the BME-Reactor core

Item	Value
Reactor type	Pool type
Reactor power level (kW, thermal)	100
Fuel type	EK-10
Enrichment (% in U-235)	10
Cladding	Aluminum
Moderator and coolant	H_2O

Reflector	Graphite+H_2O
Fuel radius (mm)	3.8
Thickness of the clad (mm)	1.2
Coolant flow rate (m^3/h)	5.8
Coolant inlet temperature ($^\circ C$)	20
Coolant inlet pressure (bar)	1.57
Total number of pins	369

The training reactor, being located in downtown Budapest, has to satisfy special safety requirements, namely, it has to be inherently safe. It is required by the safety authority that the safety report of the reactor prove that such an accident could not lead to core damage and release of radioactive material. The previous safety report written in 1996 used a combination of point reactor kinetic code REMEG and module HEATING of the SCALE 4.1 programme system [1, 2]. This study was accepted at that time but the excess reactivity may not be increased until the study is repeated based on a more detailed and mathematically better founded computer model.

The actual excess reactivity value of the reactor is 0.82$, which means that no power excursion is possible in the strict sense of the word. In 1996, this excess reactivity value was estimated to be between 1.04$ and 1.10$. Such values would not allow serious reactivity accidents either. At the same time, certain irradiation experiments regularly performed at a power of 100 kW for eight hours use up practically all this excess reactivity: 0.6$ to 0.7$ are lost because of the xenon and the temperature effects. Therefore, the control of the reactor becomes rather difficult during the last hours of the irradiations. Consequently, the excess reactivity needs to be increased to a value that the reactor had in 1980, i.e., nearly 1.2$, in order to restore the original flexibility of the reactor operation.

The reactor can get permission for achieving this only if we are able to prove that even a prompt supercritical state of 1.2$ cannot result in a reactivity accident leading to core damage. It is so trivial that it is not possible to check this statement experimentally at this reactor. Thus, we need a computer models simulating this accident. For this purpose two 3D nutronic/thermal-hydraulic space time models has been built [4-6] to investigate safety analysis of the BME-Reactor.

A power excursion accident is stopped only by the feedback through the temperature coefficients (both Doppler and moderator). It follows from this that only a time-dependent neutronic code with thermal-hydraulic feedback would be acceptable. The root cause of such accidents is not the subject of this article. We simply assume that following steady state operation, some reactivity is inserted into the reactor and the safety and control rods fail to operate for mechanical or any other reasons.

In this article, the physical and mathematical models, features and status of both integrated codes POWEX-K/MI and POWEX-K/SV are described and investigated [5, 6]. Both codes has been tested and compared together and with point kinetic code REMEG [1] developed for the safety report of BME-Reactor in 1996. We don't have to say that both POWEX-K/MI and POWEX-K/SV are more sophisticated and mathematically better founded computer models than REMEG as will be seen.

Similar codes designed for the purpose of simulating power excursion accidents are based on diffusion equation in treating the neutronic part of the problem, further methods can be found in [7]. This approximation should be sufficient to our case too. The two-phase flow in both thermal-hydraulic part of the previous integrated codes namely, Momentum Integral model (MI) and Single Velocity model (SV) is treated as homogeneous flow with no slip, one-dimensional vertical for both thermal- hydraulic core analysis codes. In the first one the homogenous flow is incompressible, the sonic effects are neglected and the thermal expansion and enthalpy transport are preserved, but the density change due to the fluid expansion is neglected in the second one. Both MI and SV models has been applied to simulate a number of transient like the inlet pressure drop transient and heat flux transient in both boiling and pressurized water reactors [8, 9].

There are 3D nutronic/thermal-hydraulic codes use finite elements while others use finite difference. The latter seemed to be simpler and accurate for our case. Both codes have been applied to analyze the power excursion accident initiated by ramp reactivity 1.2$. The results are compared and finally concluded that the reactor is inherently safe, i.e., no core melt occurs even if the safety rods do not fall in the core.

2. Reactor physics model (POWEX-K)

The BME-Reactor dynamics under transient conditions have been modeled using the neutron diffusion equations [4]:

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$$\mathbf{V}^{-1}\frac{\partial \mathbf{\Phi}(\mathbf{r},t)}{\partial t} = \nabla\big(\mathbf{D}(\mathbf{r})\nabla\mathbf{\Phi}(\mathbf{r},t)\big) - \mathbf{A}\mathbf{\Phi}(\mathbf{r},t) + (1-\beta)\boldsymbol{\chi}\mathbf{F}^{\mathrm{T}}(\mathbf{r})\mathbf{\Phi}(\mathbf{r},t) + \sum_{m=1}^{M}\boldsymbol{\chi}_m\lambda_m C_m(\mathbf{r},t) \tag{1}$$

$$\frac{\partial C_m(\mathbf{r},t)}{\partial t} = \beta_m\mathbf{F}^{\mathrm{T}}(\mathbf{r})\mathbf{\Phi}(\mathbf{r},t) - \lambda_m C_m(\mathbf{r},t), \qquad m = 1,2,...,M \tag{2}$$

where:

$\mathbf{r}$ = spatial coordinate

t = time (sec)

m = delayed neutron group index

M = total number of delayed neutron groups

G = total number of energy groups

$\mathbf{\Phi}(\mathbf{r},t)$ = vector of space and time dependent neutron fluxes [n/cm^2/sec]:

$$\mathbf{\Phi}(\mathbf{r},t) = \begin{bmatrix} \phi_1(\mathbf{r},t) \\ \vdots \\ \phi_G(\mathbf{r},t) \end{bmatrix} \tag{3.a}$$

$C_m(\mathbf{r},t)$ = space and time dependent delayed neutron precursor concentration for delayed neutron group m (atom/cm^3)

$\mathbf{V}^{-1}$ = diagonal matrix of inverse neutron velocities averaged over the energy groups (sec/cm):

$$\left[\mathbf{V}^{-1}\right]_{gg} = \frac{1}{v_g} \tag{3.b}$$

D = diagonal matrix of the neutron diffusion coefficients (cm):

$$\left[\mathbf{D}\right]_{gg} = D_g \tag{3.c}$$

A = total cross section plus slowing down matrix (cm^{-1}):

$$\left[\mathbf{A}\right]_{g,g'} = \begin{cases} \Sigma_g^{\mathrm{R}} + \Sigma_g^{\mathrm{a}} & g' = g \\ -\Sigma_{g'\to g} & g' \neq g \end{cases} \tag{3.d}$$

$\boldsymbol{\chi}$ = vector of the prompt neutron fission spectrum:

$$\left[\boldsymbol{\chi}\right]_g = \chi_g^{\mathrm{p}} \tag{3.e}$$

$\mathbf{F}$ = vector of neutron production cross sections (cm^{-1}):

$$\left[\mathbf{F}\right]_g = \nu\Sigma_g^{\mathrm{f}} \tag{3.f}$$

$\boldsymbol{\chi}_m$ = vector of the neutron spectrum for delayed neutron precursor group m:

$$\left[\boldsymbol{\chi}_m\right]_g = \chi_{mg} \tag{3.g}$$

β_m = delayed neutron fraction for group m

β = total delayed neutron fraction, $\beta = \sum_{m=1}^{M}\beta_m$

λ_m = decay constant for delayed neutron precursor group m (sec^{-1}).

Superscript "T" denotes matrix transpose.

The POWEX-K (POWer Excursion-Khaled) code solves Equations (1) and (2) by using the backward finite difference approximation for the time derivatives. The spatial derivatives are approximated by the usual finite difference scheme for XYZ-geometry [10]. The scheme had proven convergence and unconditionally stable, also we managed to prove that Gauss Seidel iteration converges in our scheme too [4].

3. SUB-CHANNELS THERMAL-HYDRAULIC MODELS (MI) AND (SV)

The heat transfer model in MI and SV inside the fuel region is based upon one-dimensional radial heat conduction. The conservation equations are written for one-dimensional axial homogeneous upward flow through the channel. On the other hand, constitutive equations as heat transfer coefficient and friction factor are used, taking into account the geometry as well as the convection regime (forced and natural). In the following, we shall discuss the basic models and equations used in both the fuel model and hydrodynamic model of the MI and SV modules.

3.1 Thermal heat conduction model

In both MI and SV thermal-hydraulic models, we solve the one-dimensional conductive heat transfer from fuel to the clad and the convective heat transfer from the clad to the coolant for nuclear fuel rods to compute the rod internal temperature distribution and the rod surface heat fluxes. The heat conduction equation inside the fuel element is written in one dimension [11]:

$$\rho_F C_{pF} \frac{\partial T_F}{\partial t} = \frac{1}{r}\frac{\partial}{\partial r}\left(r k_F \frac{\partial T_F}{\partial r} \right) + \dot{q}_v''' \tag{4}$$

while for the cladding with no internal heat generation the equation is written as:

$$\rho_C C_{pC} \frac{\partial T_C}{\partial t} = \frac{1}{r}\frac{\partial}{\partial r}\left(r k_C \frac{\partial T_C}{\partial r} \right). \tag{5}$$

For the EK-10 type fuel pellet, there is no actual gap between the fuel outside surface and the clad, hence the accompanied set of boundary conditions for equations (4) and (5) are given by:

$$\left.\frac{\partial T_F}{\partial r}\right|_{r=0} = 0 \text{ and, } q'' = -k_C \left.\frac{\partial T_C}{\partial r}\right|_{r=r_{co}} = h[T_{co} - T_m].$$

The heat conduction and energy conservation equation are coupled at the interface between the clad and the coolant, where:

q'' = Heat flux [W/m^2]
$T_F(r,t)$ = Fuel pellet temperature [K]
$T_m(z,t)$ = Moderator temperature [K]
$T_C(r,t)$ = Clad temperature [K]
$T_{co}(z,t)$ = Clad surface temperature [K]
$\rho_F C_{pF}(t)$ = Fuel pellet heat capacity [J/m^3 K]
$\rho_C C_{pC}(t)$ = Clad heat capacity [J/m^3 K]
$k_F(t)$ = Fuel pellet thermal conductivity [W/m K]
$\dot{q}_v'''(t,r,z)$ = Heat source per unit volume [W/m^3]
$k_C(t)$ = Clad thermal conductivity [W/m K]
r_{co} = Clad outside radius [m].
r = Radial coordinate [m]
h = Convective heat transfer coefficient (W/m^2 K)

The thermal conductivity in [W/m K] and heat capacity in [J/kg K] for both the EK-10 fuel material type and aluminum alloy clad has been taken as functions of fuel and clad temperatures respectively. The backward finite difference method is applied in radial direction and is used for time derivatives and radial space derivatives [5, 6].

In the actual calculations of both 3D models i.e., POWEX-K/MI and POWEX-/SV, the core was subdivided into 30 axial and 10 radial nodes per channel. Heat transfer in each fuel element is determined on the basis of one dimensional conduction solution up to 29 axial sections. The moderator in each axial section is not subdivided. For the heat transfer across the clad-coolant interface, local flow and temperature dependent correlations were used [5].

3.2 Hydrodynamic models (MI) and (SV)

In two phase flow the vapor and liquid move at the same velocity (no slip) and by neglecting the lateral variation of fluid properties and velocity, the conservation equation of mass, momentum and energy respectively for single heated channel can be written in the following form [9]:

$$\frac{\partial \rho_m}{\partial t} + \frac{\partial G_m}{\partial z} = 0 \tag{6}$$

$$\frac{\partial G_m}{\partial t} + \frac{\partial}{\partial z}\left(\frac{G_m^2}{\rho_m}\right) = -\frac{\partial P}{\partial z} - \frac{fG_m|G_m|}{2D_e\rho_m} - \rho_m g \tag{7}$$

$$\rho_m \frac{\partial H_m}{\partial t} + G_m \frac{\partial H_m}{\partial z} = \frac{\partial P}{\partial t} + \frac{q'' P_h}{A_z} + \frac{G_m}{\rho_m}\left[\frac{\partial P}{\partial z} + \frac{fG_m|G_m|}{2D_e\rho_m}\right] \tag{8}$$

where,

A_z = Channel flow area [m^2]
f = Friction factor
G = Mass flux [kg/m^2 sec]
D_e = Equivalent hydraulic diameter [m]
g = Gravitational acceleration [m/s^2]
H = Enthalpy [J/kg]
P = Pressure [M Pa]
P_h = Heated perimeter [m]
z = Axial distance [m]
ρ = Density [kg/m^3]

Furthermore, constitutive equations for ρ_m and f are required to complete definition of the problem. The equation of state for density, assumed differentiable with respect to h_m and ρ, is specified as:

$$\rho_m = \rho_m(H_m, P) \tag{9}$$

The friction factor can be specified as:

$$f = f(H_m, P, G_m, q'') \tag{10}$$

Under the assumption that the liquid and vapor can be considered as a homogenous mixture, the above equations (6), (7), and (8) are applicable for two phase flow as well as for single phase flow.

For both sub-channels thermal-hydraulic models MI and SV, the conservation momentum (7) equation was integrated axially over the channel, given by:

$$\frac{dG_{avg}}{dt} = \frac{1}{L}(\Delta P - F) \tag{11}$$

where:

$$\Delta P = -\int_0^L \frac{\partial P}{\partial z}\, dz = P_{inlet} - P_{oulet} \tag{12}$$

$$G_{avg} = \frac{1}{L}\int_0^L G_m\, dz \tag{13}$$

$$F = \left(\frac{G_m^2}{\rho_m}\right)_{outlet} - \left(\frac{G_m^2}{\rho_m}\right)_{inlet} + \int_0^L \frac{f|G_m|G_m}{2D_e\rho_m}\, dz + \int_0^L \rho_m g\, dz \tag{14}$$

where ΔP is the pressure drop and F is the friction term. By neglecting the pressure and friction term in the energy equation (8) the energy equation will be written in the following form:

$$\rho_m \frac{\partial H_m}{\partial t} + G_m \frac{\partial H_m}{\partial z} = \frac{q'' P_h}{A_z} \tag{15}$$

Furthermore assumption to be carried out in SV model is to consider the mass velocity constant i.e., the continuity equation (6) becomes:

$$\frac{\partial G_m}{\partial z} = 0 \tag{16}$$

In other words, it is assumed that the density change due to the fluid expansion is neglected. Further details can be found in references [8, 9]. Complete form of the finite differencing of the previous system of equations can be found in [5, 6].

The solution is completely determined by considering the closure relationships [5]; the initial and boundary conditions. The initial distributions of $G_m(t)$ and $\mathrm{H_m}(z,t)$ are assumed known from steady state solutions [9]. The heat flux $q''(z,t)$ in a reactor is dependent on the coolant and fuel thermal conditions. Hence, the specification of $q''(z,t)$ is obtained from the effects of the neutronic response and the transient heat conduction in the fuel. In the present article we shall assume the inlet and outlet pressures are specified.

When, for a given power distribution inside the core, the thermal-hydraulic module computes the average fuel, clad and coolant temperatures for every mesh point of the finite difference scheme, we are able to calculate the few group constants of the 3D diffusion equation by using an asymptotic slowing-down and thermalisation code WIMS-D4) see reference [12]. The thermal-hydraulic feedback is taken into account via the recalculation of the few group constants.

Now and together with some constitutive relations [5], Equations 1, 2, 4, 5, 6, 9, 10, 11, 15 or their corresponding finite difference forms are defining completely the integrated model POWEX-K/MI. Also, equations 1, 2, 4, 5, 9, 10, 11, 15, 16 are defining POWEX-K/SV model completely.

4. RESULTS AND DISCUSSION

For the calculations performed for BME-Reactor by using the model described in the previous sections, the input data used are dependant reference [1].

As a matter of fact a number of verifications of our 3D neutronic/thermal-hydraulic model namely, POWEX-K/MI has been applied either experimentally or theoretically [5] and further verifications is to be applied in this article. As an examples of tests were applied is the following: The neutronic part POWEX-K of both integrated codes POWEX-K/MI and POWEX-K/SV has been tested experimentally [5] by a start-up experiment during which the reactor power increases exponentially but slowly. The BME-Reactor was left alone in a supercritical state with a reactivity of 14 cents and the increase of the neutron flux was observed by means of a neutron detector. The calculation has been done without thermal-hydraulic feedback due to it runs at very low power. i.e., only the neutronic part of the program i.e., POWEX-K was used. The results showed a good agreement with the experimental data. The power increase corresponding to the actual excess reactivity (i.e., 0.82$) of the BME-Reactor has been simulated [5], while the real power excursion accident because of a sudden increase of the reactivity from critical to 1.2$ and its comparison with POWEX-K/SV and REMEG code will be discussed briefly in this article (Section 4.1). Since, according to the sense, such reactivities cannot be realized in the BME-Reactor, the presented calculated results cannot be verified experimentally. The comparisons give an idea about the improvement brought about by the 3D model with respect to the point kinetics model in addition to, it give a test of safety limits of the BME-Reactor as well.

4.1 Power excursion at prompt supercriticality

The desirable excess reactivity of BME-Reactor is 1.2 $, thus, the power excursion was analyzed for the ramp insertion of this reactivity. In this case, we adjusted a reactor size for which

$$k_{\mathrm{eff}} = 1 + 1.2 * 0.00786 = 1.009432$$

where 0.00786 is the effective delayed neuron fraction for BME-Reactor as calculated by the kinetic version of program GRACE [13]. The adjustment was achieved by adding some fuel rods to the reactor. This is corresponds to the reality because this excess reactivity will be realized in the same way.

Figure 1 shows our 3D models predictions for the total reactor power. The figure shows that, starting from 1 W, the power rises following a rapid exponential trend, increasing to 28.9 MW, 10.1 MW and 14.7 MW at 0.92 sec, 3.27 sec and 0.88 sec according to POWEX-K/MI, POWEX-K/SV and REMEG respectively then it decreases due to the temperature feedbacks. It is remarkable that the three curves deviate from each other very much, namely the 3D model POWEX-K/MI predicts the accident more severe than POWEX-K/SV and REMEG at the same time its observable that the power peak is reached in both POWEX-K/MI and REMEG much faster that POWEX-K/SV.

The total energy released during the transient is 23687 kW/s, 15514.2 kW/sec for POWEX-K/MI and POWEX-K/SV, while it is 10776.17 kW sec for REMEG. If we divide these numbers by the nominal power of BME-Reactor, we get 3.9 min, 2.5 min and 1.8 min, respectively. This means that the energy released corresponds to the energy released during the previous mentioned times of nominal operation. In this basis, we do not expect very high temperatures for all models.

Figure 2 shows our models predictions for the fuel temperature while Figures 3, 4 stands for the clad and coolant temperatures at a fuel rod located near the core center and in the axial position located at half length of the fuel pin. The fuel temperature goes up to 681 ^{o}C, 538 ^{o}C and to 716 ^{o}C according to POWEX-K/MI, POWEX-K/SV and REMEG respectively at 3.5 sec, 7.8 sec and 4.99 sec following the ramp reactivity insertion then it stabilize in both 3D models POWEX-K/MI and POWEX-K/SV. This remarkable stability is a favor of the 3D modeling with respect to point kinetic REMEG code. In Figure 3 the clad temperature increased to 46 ^{o}C, 110 ^{o}C in 2.2 sec, 6.8 sec according to POWEX-K/MI and POWEX-K/SV while it increases only to 71.8 ^{o}C in 4.9 sec according to REMEG. These figures are much lower than the melting point of aluminum (which is 660.2 ^{o}C). The moderator temperature for all codes is still less than 21 ^{o}C as can be seen from Figure 4. The calculations were done up to 3.25 sec, 19 sec and 5 sec for POWEX-K/MI, POWEX-K/SV and REMEG respectively. The difference of operating time between codes can be explained in view of when all the heat generated owing to the transient went into the moderator and fuel and clad temperatures were stabilized. Of course, it will be very time consuming if we continue expensive 3D calculations without need after the previous mentioned times. As in the case of the reactor power, the fuel temperatures calculated during transient using the POWEX-K/MI and REMEG model are higher than those calculated by POWEX-K/SV. While Clad Temperatures calculated using POWEX-K/SV and REMEG models are higher than calculated using POWEX-K/MI model. The coolant temperatures calculated using 3D models still much below 21 ^{o}C. It can be easily seen the dramatic behavior of fuel, clad and coolant temperature using REMEG compared with the sophisticated 3D models. Although the deviations observed between the predications of 3D models (it will explained in section 5) all temperatures are below its severe limits and no core melt can be expected even safety and control rods fail to operate.

Figure 5 shows the time dependence of the thermal flux during the 1.2 $ transient for three different points inside the core: near the core center, near the reflector and near a water gap. These three curves are roughly proportional to each other: their ratios are nearly constant in time. This indicates that time dependence and space dependence of the flux are nearly separable from each. Taking into account that this separability is the fundamental assumption of the point kinetic model, the deviations between the results obtained by POWEX-K/MI, POWEX-K/SV and REMEG in Figures 1-3 cannot be explained by the differences of the 3D and point kinetic descriptions. Consequently, the differences are mainly due to a better thermal-hydraulic description of the power excursion by 3D models. Discrepancy between 3D models itself will be described in the next section.

Figure 6 shows the development of the normalized mass flux i.e. $G_m(t)/G_m(0)$ of the hottest channel during the 1.2 $ transient. It can be seen in this figure that no axial dependence according to both MI and SV model and the mass flux reaches to more than 16 times its initial value in almost 2 sec in POWEX-K/MI, while it reaches 14 times its initial value in 17 sec according to POWEX-K/MI. This is a natural convection initiated by the transient. It is an important difference with respect to the REMEG calculation which does not take into account convection.

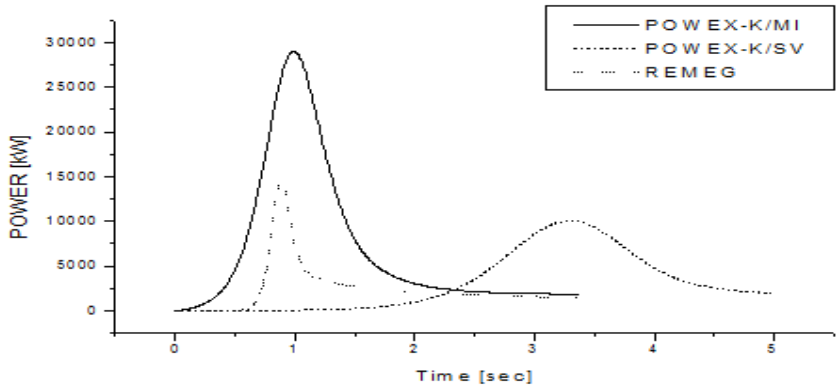

Figure 1: Time behavior of power for ramp reactivity of 1.2$

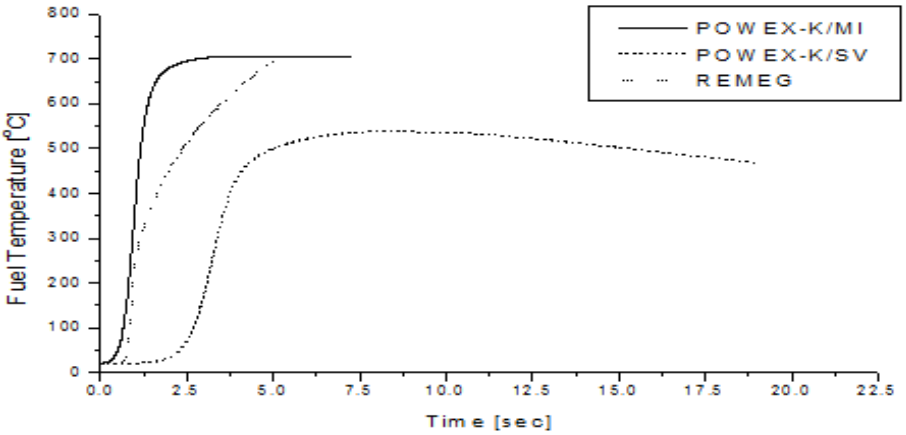

Figure 2: Time behavior of fuel temperature for ramp reactivity of 1.2$

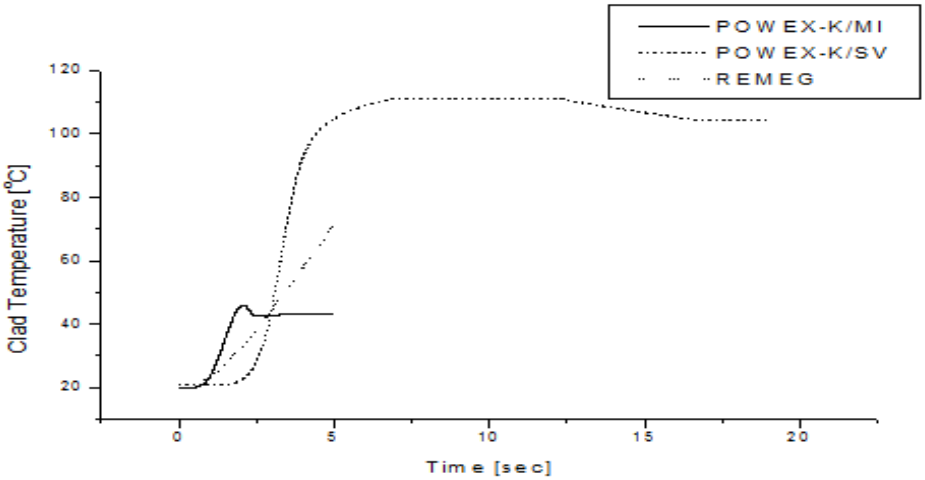

Figure 3: Time behavior of Clad Temperature for ramp reactivity of 1.2$

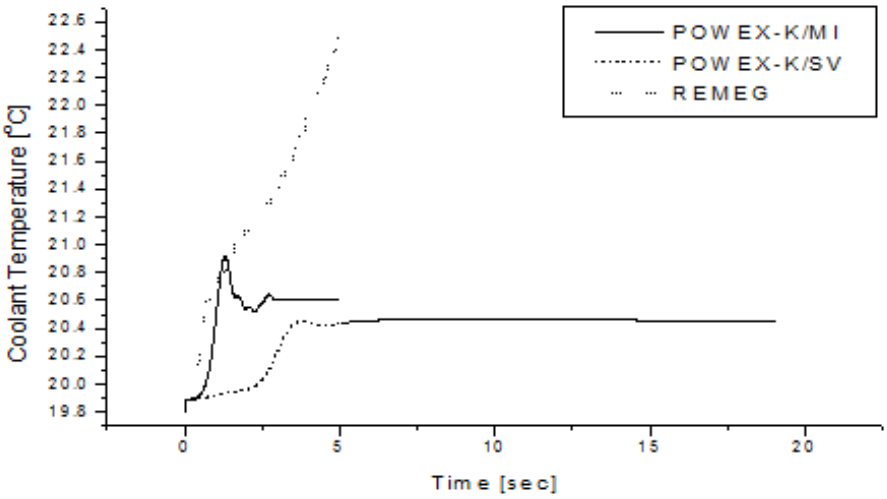

Figure 4: Time behavior of coolant temperature for ramp reactivity of 1.2$

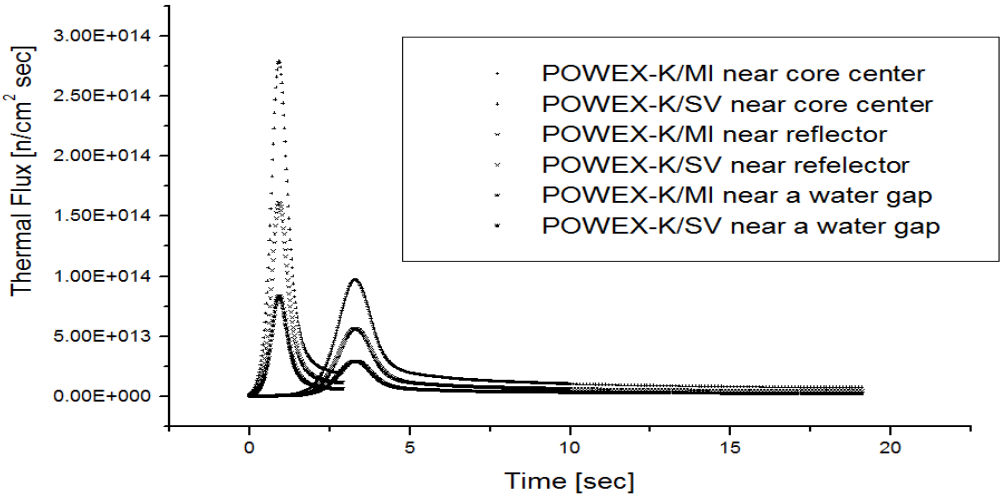

Figure 5: Thermal flux as a function of time in different points inside the core for ramp reactivity of 1.2$

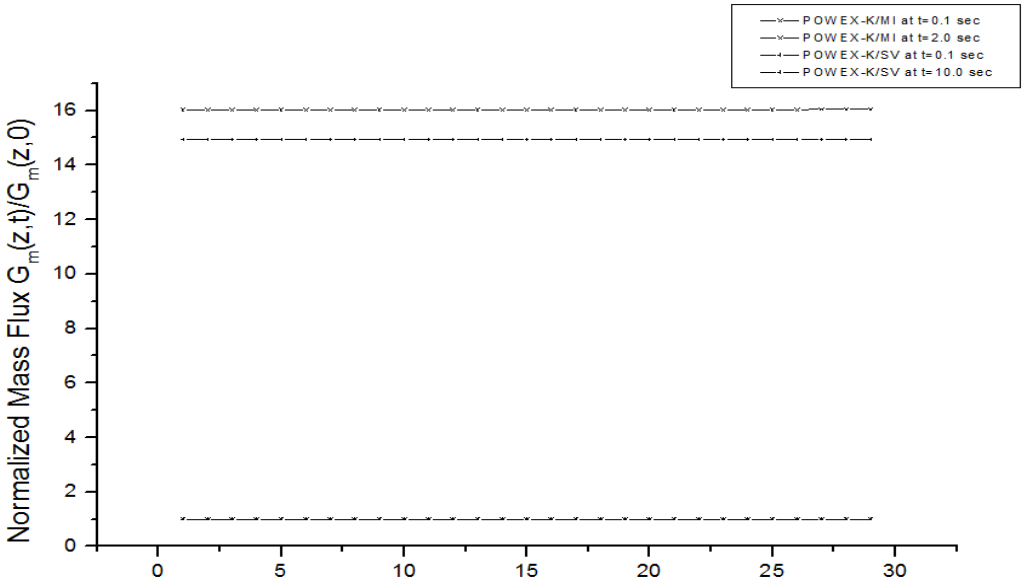

Figure 6: Time Normalized mass flux of hottest channel for ramp reactivity of 1.2$

5. CONCLUSION

From the results presented in the previous section, we can draw the following conclusions:

- The neutronic part of both integrated codes POWEX-K has been compared experimentally with the start-up data for 14 cents reactivity insertion [5]. It is noticed from the calculations that the exponential trend of the thermal flux at the measuring channel of the BME-Reactor and the results of simulation performed by POWEX-K are in a very good agreement to each other. The discrepancy noticed in this article in the simulations of the ramp reactivity insertion of 1.2 $ between the 3D models POWEX-K/MI and POWEX-K/SV is refer to the thermal-hydraulic models MI and SV and the different features and assumptions carried out in each model especially the assumption of constant mass flux of SV model. In addition to treating the momentum balance in an integral sense in both MI and SV thermal-hydraulic models.

- Although the discrepancy observed in calculation of fuel, clad temperature and the reactor power peaks (Figures 1-3) both of the hydrodynamic model has been found to be valid especially for small core and the very low coolant velocity like the one occurring in BME-Reactor. Moreover, both codes can compute the flow and enthalpy distribution in nuclear fuel bundles and core for both steady and transient conditions which is favor of 3D modeling with respect to the REMEG point kinetic code.

- Also, the MI and SV models codes are very flexible for modeling a reactor core and fuel assemblies. Moreover, both are easy to couple with the neutronic dynamics part of the problem.

- From the comparisons with the results of the point kinetic REMEG code, we may conclude that the discrepancy observed between 3D model predictions of the reactivity accident of 1.2$ and REMEG code are due to a better thermal-hydraulic description by 3D space-time POWEX-K/MI and POWEX-K/SV codes with respect to point kinetic code REMEG.

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- It can be noticed form Figure 2-4 that the fuel and clad temperatures during the accident are much below their melting point of both and no boiling of coolant will occur (i.e., single phase fluid assumption of both MI and SV is acceptable). Consequently, Even for 1.2 $ reactivity insertion, we need not reckon with the melting of the fuel or clad and boiling of the moderator.

- Finally, fuel failure and release of radioactivity will not occur.

ACKNOWLEDGMENTS

The authors are indebted to Professors Attila Aszódi and Zoltán Szatmáry for their guidance and valuable help during this work. Furthermore, the author wishes to express their sincere thanks to Dr. Mujid S. Kazimi for his support.

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