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Lumped parameter model for the safety assessment of the IAEA 10MW benchmark reactor during a protected loss of flow accident

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Abstract: The loss of flow accident in the IAEA 10Mw benchmark reactor is treated in this study through a coupled solution of the point kinetic and thermal hydraulic models. By employing the lumped parameter technic, a one-dimensional thermal hydraulic model is derived and solved beside the point kinetic model with the continuous reactivity feedback effect of the coolant and fuel temperatures. This coupled solution, allows us, to determine, the transient variation of the maximum fuel and coolant temperatures, furthermore, to the safety criteria (ONBR, OFIR, CHFR and BOR) which must be greater than the imposed safety limits, to avoid any nuclear reactor safety issue. Finally, the obtained results of our simulation were validated after a satisfactory comparison with the results of other codes system and developed computer codes.

1. Introduction

Since the early age of nuclear reactor uses, code systems played a crucial role in nuclear reactor safety analysis. Through, the simulations of the nuclear reactors, during the transient operating conditions, such as, the loss of flow and reactivity insertion accidents. In order, to check that, even during these accidents, the reactor core thermal hydraulic parameters are kept under control and don't exceed the imposed safety limits, to avoid the occurrence of some critical phenomenon that will probably lead to radioactive releases from the nuclear fuel.

So, the simulation of the nuclear reactor during transient operating conditions by the code systems is the most effective way to establish a nuclear reactor safety assessment. But despite these code systems advantages in the safety analysis of nuclear reactors, their uses still required a high skill level and a lot of time for nuclear facility modelling. Moreover, they are not always suitable for the MTR nuclear research reactor safety analysis. For these reasons, the need of developing simplified and efficient computer codes for MTR research rector uses has received more attention in the last few decades. Firstly, based upon the lumped parameter approach as given by Gaheen et al. (2007), Housiadas (2002), Lashkari (2015), and El-Khatib et al. (2013) and secondly, based upon the transport equations of fluid flow and heat transfer as given by Lu et al. (2009), Bousbia-Salah and Hamidouche (2005) and Al-Yahia et al. (2013). In order, to predict more accurately the behavior of the MTR nuclear research reactor during transient operating conditions.

More recently, the loss of flow accident is treated in the nuclear power plants by using code systems, the code system ATHLET3.1A, is used by Abdel-Latif (2021) to model the Germany Pressurized Water Reactor (GPWR), to predict the thermal hydraulic behavior of this reactor, for the trip of one, two, three and four of the Main Coolant Pumps, at the operation of 100% reactor power, while Corzo et al.

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(2023) used the code system Relap5/mod3.3, to simulate the loss of flow accident in the Atucha II nuclear power plant for single and total main coolant pump (MCP) trips, the results of his simulation showed a good agreement with recorded data of a real single-MCP trip event.

Regarding the fact that the loss of flow accident is a real event, which can occur in nuclear facilities through pump shaft break or trips, so the occurrence of this accident must be studied during nuclear reactor design, but the commercial codes widely used to study this accident is destined for nuclear power plants uses instead of a research reactor. The aim of this study is to develop a simple computer code for the transient analysis of MTR research reactors during a protected loss of flow accident, this can be achieved only through a coupled solution of point kinetic and thermal hydraulic models. In this study, the point kinetic model is solved by an efficient method, Kinard and Allen (2004) and Yamoah et al. (2013), to evaluate the thermal reactor power as a function of time. Thereafter, the lumped parameter thermal hydraulic model, is solved by the Range-Kutta method, which allows us, to carry out, the transient behaviour of the reactor. Finally, our simulation results were validated after a good agreement with elsewhere published results of other codes system and developed computer codes.

2. The Protected Loss of Flow Benchmark Problem Description

The loss of flow benchmark problem includes both fast and slow losses of flow, practically can be initiated by many postulated initiating events, such as pump failure or piping breaking and blockage. The occurrence of one of these initiating events leads to an exponential decay of the mass flow rate as indicated by Eq. 1, Housiadas (2000),

$$v = v_0\, e^{\left(-\frac{t}{\tau}\right)} \tag{1}$$

With, τ is the time constant for the pump coast down which is respectively equal to 1s and 25s for fast and slow loss of flow.

Theoretically, during the loss of flow accident, the mass flow rate will decrease according to Eq. 1 and when the mass flow scram condition is reached, after we lose an amount of 15% from the initial mass flow rate. then a reactivity rate of -10$/0.5s is inserted with a delay time of 0.2s, so the reactor power decreased from the initial operating power, which is taken 12 MW (120% of the nominal power) as a function of reactivity until the fission generated power is completely stopped and the reactor has sustained in a safe state.

3. Mathematical Model

To predict the reactor core behavior during transient operating conditions, due to, a reactivity insertion or loss of flow accidents. This always requires a simultaneous solution of both kinetic and thermal hydraulics models, which are given respectively in this study by the classical point kinetic equation with six groups of delayed neutrons and also single phase heat transfer equations of coolant and fuel temperatures, as presented below.

3.1. Neutron kinetic model

The reactor power is calculated from the point reactor kinetics model with six groups of delayed neutrons. The neutron balance inside the reactor core is expressed by, Al-Yahia et al. (2013) and Altamimi et al. (2017):

$$\frac{\mathrm{dP(t)}}{\mathrm{dt}} = \beta\left(\frac{\rho_e(t)-1}{\Lambda}\right) P(t) + \sum_{i=1}^{6} \lambda_i C_i\,(t) \tag{2}$$

$$\frac{\mathrm{dC}_i(t)}{\mathrm{dt}} = \frac{\beta_i}{\Lambda}\, P(t) - \lambda_i C_i(t) \;\; ; \qquad i = 1, \dots, 6 \tag{3}$$

Where P is the core average power, β is the total delayed neutron fraction, Λ is the mean neutron generation time, C_i is the number of delayed neutron precursors in group i and λ is the precursor decay constant.

The initial conditions for these differential equations are; $P(0) = 12\, MW$ and $C_i(0) = \frac{\beta_i}{\lambda_{i\,\Lambda}} P(0)$.

The reactivity $\rho_e(t)$, generated inside the reactor core consists of the external reactivity produced by the control rode insertion $\rho_{ext}(t)$ further to the different reactivity feedback effects. These lasts are due to the change of fuel temperature (Doppler effects) and coolant temperature (spectrum effects only), times their corresponding reactivity feedback coefficients α_f and α_l, as given by Eq. 4. It could be noted that for the values of the reactivity feedback coefficients we have used those of the work of Bousbia-Salah and Hamidouche (2005).

$$\rho_e(t) = \rho_{ext}(t) - \alpha_l\left(T_l - T_{l,0}\right) - \alpha_f\left(T_f - T_{f,0}\right) \tag{4}$$

3.2. Lumped parameter model

For an MTR type fuel element with a rectangular geometry formed by a cooling channel of width W and thickness 2b and a fuel plate of thickness 2d. During the fuel cooling a convective heat transfer is take place between the fuel and the coolant so the local temperatures equations of the coolant and the fuel can be expressed as follow, Housiadas (2002).

$$\rho_c c_c \frac{\partial \widehat{T_c}}{\partial t} + \rho_c c_c v_c \frac{\partial \widehat{T_c}}{\partial z} = \frac{h}{b}\left(\widehat{T_f} - \widehat{T_c}\right) + \widehat{P_c} \tag{5}$$

$$\rho_f c_f \frac{d\widehat{T_f}}{dt} = -\frac{h}{d}\left(\widehat{T_f} - \widehat{T_c}\right) + \widehat{P_f} \tag{6}$$

Where, ρc is the volumetric heat capacity, v_c is the coolant velocity [m/s], $\widehat{T_c}$ and $\widehat{T_f}$ is respectively the coolant and the fuel temperatures [°C] and the subscripts f and c denote, respectively, the fuel and the coolant. $\widehat{P_f}$ the local power per unit of fuel volume [w/m^3], $\widehat{P_c}$ the local power per unit of coolant volume [w/m^3].

The local power P(z) is assumed to have the chopped cosine shape in the axial direction and is described as follows, Gaheen et al. (2007):

$$P(z) = TPF.\bar{P}.cos\left(\frac{\pi\left(z-\frac{H}{2}\right)}{H_e}\right) \tag{7}$$

With TPF is the total peaking factor, $\bar{P}$ is the average core power [w], H and H_e are respectively the active fuel and extrapolated lengths [m].

To reduce the axially dependent quantities of our model to average one, the integral rule below is used, Housiadas (2002).

$$F(t) = \frac{1}{H}\int_0^H \hat{F}(z,t)\, dz \tag{8}$$

By applying the previous integral rule on both sides of (Eq. 5) and (Eq. 6) and after considering $T_c(0)$ is equal to the pool temperature (T_p), then the following ordinary differential equations system is obtained.

$$\rho_c c_c \frac{dT_c}{dt} + \frac{\rho_c c_c v_c}{H}\left[T_c(out) - T_p\right] = \frac{h}{b}\left(T_f - T_c\right) + P_c \tag{9}$$

$$\rho_f c_f \frac{dT_f}{dt} = -\frac{h}{d}\left(T_f - T_c\right) + P_f \tag{10}$$

To determine the temperature variation along the fuel active length, we have assumed that the shape of functions $\widehat{T_c}(z,t)$ and $\widehat{T_f}(z,t)$ remains unchanged with time, and identical to the profile corresponding to static conditions. Considering the steady-state solutions of Eq. 5 and Eq. 6 it can be shown that the axial profiles can be expressed as follows, Housiadas (2002).

$$\widehat{T_c} = T_p + \left(T_c - T_p\right)\left[1 - cos\left(\frac{\pi z}{H}\right)\right] \tag{11}$$

$$\widehat{T_f} = T_p + \left(T_c - T_p\right)\left[1 - cos\left(\frac{\pi z}{H}\right)\right] + \frac{\pi}{2}\left(T_f - T_c\right)sin\left(\frac{\pi z}{H}\right) \tag{12}$$

The above expressions permit to approximate the axial temperature distributions of coolant and fuel element with the help of the mean temperatures T_c and T_f, i.e. the lumped parameters. More specifically, (Eq. 11) permits to write, Housiadas (2002).

$$T_c = \frac{T_{out}+T_p}{2} \tag{13}$$

which enables us to express the Eq. 5 in terms of the lumped parameters T_c and T_f as follows, Housiadas (2002).

$$\rho_c c_c \frac{dT_c}{dt} + \frac{2\rho_c c_c v_c}{H}\left(T_c - T_p\right) = \frac{h}{b}\left(T_f - T_c\right) + P_c \tag{14}$$

where, $h(w/m^2c^0)$ is the convective heat transfer coefficient is calculated by using the correlation of Dittus & Boelter given in the work of Al-Yahia et al. (2013), for the turbulent regime, while is considered a constant for the laminar regime, Altamimi et al. (2017),

$$Nu = \begin{cases} 7.63 & R_e \leq 2300 \\ 0.0243\ \mathrm{Re}^{0.8}\ Pr^{0.4} & R_e \geq 2300 \end{cases}$$

In the present simulations, the core inlet temperature (or pool temperature) T_p is normally a constant specified as an input parameter. However, the option of pool heating has been also accommodated to analyse conditions in which pool temperature rises because of simultaneous loss of secondary cooling. This can be accomplished by introducing an additional differential equation, based on a simple heat balance over the pool volume, Housiadas (2002).

$$\rho_c c_c V_p \frac{dT_p}{dt} = V_f P \tag{15}$$

4. The Safety Criteria

To check out the safety state of the nuclear reactors, which depends completely on the occurrence of some critical phenomenon in the reactor core, especially the boiling of the fluid flow, which can be developed if the cladding temperature keeps rising and exceed the coolant saturated temperature, to form a vapor layer over the cladding surface which can be led to failure of the fuel cooling process, and therefore a complete loss of the fuel integrity. So, to estimate the effect of the occurrence of these critical events on the reactor core safety, many safety criteria are used, among them, in this work we have considered, the onset of nucleate boiling ratio (ONBR), the onset of flow instability ratio (OFIR), the critical heat flux ratio (CHFR) and finally the burn out ratio (BOR).

4.1. The onset of nucleate boiling ratio

The onset of nucleate boiling ratio (ONBR), is defined by the ratio between the onset of nucleate boiling heat flux (q''_{ONB}) and the local heat flux (q''_z),

$$\mathrm{ONBR} = \frac{q''_{ONB}}{q''_z} > \begin{cases} 1.3, fo\ steady\ condtions \\ not\ used\ for\ transien\ conditions \end{cases}$$

The wall temperature at the onset of nucleate boiling is calculated from the correlation of Bergles and Rohsenow, Jo et al. (2014),

$$T^n_{w,ONB} = T_{sat} + \frac{1}{1.8}\left[\frac{q''^{n-1}_{ONB}}{1082P^{1.156}}\right]^{\frac{P^{0.0234}}{2.1605}} \tag{16}$$

With, $T_{w,ONB}$ and T_{sat} is the ONB and the saturation temperatures in [°C], P is the coolant inlet core pressure in bar. T_{sat}, is evaluated as a function of the inlet core pressure (1.7 bar) which is about 115°C. While the determination of the ONB temperature required another equation to evaluate the q''_{ONB} heat flux, this equation is given by, INVAP (2001),

$$q''^{n}_{ONB} = q''^{n-1}_{ONB} + 0.75\, h\left(T^n_{w,ONB} - T^{n-1}_{w,ONB}\right) \tag{17}$$

The 0.75 is the calculation relaxation factor and h is the heat transfer coefficient in [W/m2°C], after we take the max cladding temperature and the average heat flux of steady solution as initial guesses respectively for $T_{w,ONB}$ and q''_{ONB}, then we did as needed of iterations until the increment in the q''_{ONB} heat flux become less than 0.001.

4.2. The flow instability ratio

The flow instability ratio (OFIR), is defined as the ratio between the heat flux of the onset of flow instability (q''_{OFI}) and the local heat flux (q''_z),

$$\mathrm{OFIR} = \frac{q''_{OFI}}{q''_z} > \begin{cases} 2, for\ steady\ condtions \\ 1.3, for\ transien\ conditions \end{cases}$$

To determine the heat flux of the onset of flow instability, Whittle and Forgan correlation is used, Umbehaun and Torres (2003),

$$q''_{OFI} = \frac{R\, \rho_c\, c_c\, v_c\, D_h}{4L_h}\, (T_{sat} - T_{inlet})$$

$$R = \frac{1}{1+\eta\frac{D_h}{L_h}} \tag{18}$$

Where $q''_{OFI}\left(\frac{w}{m^2}\right)$ is the onset flow instability heat flux, $D_h\,(m)$ is the hydraulic diameter, $L_h(m)$ is the channel heated length, and η is a coefficient that can be tacked equal to 25 or calculated by $\eta = 3.15\,(1.08\,\mathrm{G})^{0.29}$, INVAP (2001).

4.3. The critical heat flux ratio

The critical heat flux ratio (CHFR), is defined as the ratio between the critical heat flux (q''_{CHF}) and the local heat flux (q''_z),

$$\mathrm{CHFR} = \frac{q''_c}{q''_z} > \begin{cases} 2, for\ steady\ condtions \\ 1.5, for\ transien\ conditions \end{cases}$$

To evaluate the critical heat flux, the Kaminaga Correlation given in the work of Jo et al. (2014), is used,

$$q''^{*}_{CHF,1} = 0.005|G^*|^{0.611}\left(1 + \frac{5000}{|G^*|}\Delta T^*_{sub,out}\right) \tag{19}$$

$$q''^{*}_{CHF,2} = \frac{A_f}{A_h}\Delta T^*_{sub,in}|G^*| \tag{20}$$

$$q''^{*}_{CHF,3} = 0.7\frac{A_f}{A_h}\frac{\sqrt{\frac{W}{\lambda}}}{\left[1+\left(\frac{\rho_g}{\rho_f}\right)^{\frac{1}{4}}\right]^2}\left(1 + 3\Delta T^*_{sub,in}\right) \tag{21}$$

Where $q''^{*}_{CHF} = \frac{q''_{CHF}}{\left(h_{fg}\sqrt{\lambda(\rho_f-\rho_g)\rho_g g}\right)}$, $G^* = \frac{G}{\sqrt{\lambda(\rho_f-\rho_g)\rho_g g}}$, $\Delta T^*_{sub} = \left(\frac{C_p\,\Delta T_{sub}}{h_{fg}}\right)$, $\lambda = \left(\frac{\sigma}{(\rho_f-\rho_g)\rho_g}\right)^{1/2}$, and A_f with A_h are respectively the flow area and the heated area.

For downward flows

If $G^* \geq G^*_1$, then $q''_{CHF} = \min(q''_{CHF,1}, q''_{CHF,2})$

If $G^* < G^*_1$, then $q''_{CHF} = \max(q''_{CHF,2}, q''_{CHF,3})$

For upward flows

If $G^* \geq G^*_1$, then $q''_{CHF} = \min(q''_{CHF,1}, q''_{CHF,2})$

If $G^* < G^*_1$, then $q''_{CHF} = \max(q''_{CHF,1}, q''_{CHF,3})$

With $G^*_1 = \left(\frac{0.005}{\left(\frac{A_f}{A_h}\right)\Delta T^*_{sub,in}}\right)^{\frac{1}{0.389}}$, and when, $G^* < G^*_1$, $\Delta T^*_{sub,out}$ in the (Eq. 19) should be set to zero.

4.4. The burn-out ratio

The Burn-out ratio (BOR), is defined as the ratio, between the burn-out heat flux (q''_{BO}) and the local heat flux (q''_z),

$$\mathrm{BOR} = \frac{q''_{BO}}{q''_z} > \begin{cases} 2, for\ steady\ condtions \\ 1.3, for\ transien\ conditions \end{cases}$$

Regarding the not availability of a correlation to calculate the heat flux at the onset of burnout, so in this study, we evaluated the Burn out heat flux at top flooding conditions by using the Mishima correlation, Cheng (1990),

$$q''^{*}_{BO}=\frac{A_f}{A_h}\left\{\left(\frac{C_p\,\Delta T_{sub}}{h_{fg}}\right)G^{*}+C^{2}\frac{\sqrt{\frac{2w}{\lambda}}}{\left[1+m\left(\frac{\rho_g}{\rho_f}\right)^{\frac{1}{4}}\right]^{2}}\right\} \tag{22}$$

$$q''^{*}_{BO}=\frac{q''_{BO}}{h_{fg}\sqrt{\lambda(\rho_l-\rho_g)\rho_g g}}\;;G^{*}=\sqrt{\frac{G}{\lambda(\rho_l-\rho_g)\rho_g g}}$$

$$\lambda=\sqrt{\frac{\sigma}{(\rho_l-\rho_g)g}};\;C=0.66\left(\frac{w}{\delta}\right)^{0.25}$$

$$m=0.5+0.0015\,Bo^{1.3};Bo=\left(\frac{\delta\,w}{\lambda^{2}}\right)$$

With, q''^{*}_{BO} is the dimensionless burnout heat flux, G^{*} is the dimensionless mass flux, q''_{BO} is the Burnout heat flux [W/m^2], h_{fg} is the latent heat of evaporation [J/kg]. λ is the characteristic length [m], $\rho_l\,,\rho_g$ are respectively the density of water and water vapor [Kg/m^3], g is the acceleration of gravity [m/s^2] and σ the surface tension [N/m].

4.5. The critical velocity

For a given plate assembly there is a critical flow velocity at which the plates become unstable and large deflections of the plates can occur. To avoid that, the coolant velocity of coolant should verify the following condition, Khedr (2008),

$$v_c<\frac{2}{3}v_{critic}$$

To determine the critical velocity the formula derived by Miller is used, Khedr (2008),

$$V_{critic}=\left[\frac{15\;10^{5}E\;(t_p^{3}-t_m^{3})t_w}{\rho_c w^{4}(1-\nu^{2})}\right]^{\frac{1}{2}} \tag{23}$$

With, t_p, t_m and t_w in (cm) are respectively the plate thickness, the meat thickness and the coolant channel thickness, w in (cm) is the coolant channel width, E in (bar) is the elastic Young modulus and ν is the Poisson ratio.

5. Results and Discussion

In the present work, both protected fast and slow losses of flow accidents are simulated in the hottest channel of the LEU IAEA 10 MW benchmark reactor, by solving numerically the coupled kinetic and thermal hydraulics models. To determine, the transient variation of the maximum fuel and coolant temperatures, further to, the considered safety criteria, which allow us, to verify the loss of flow accident effect to the reactor safety.

Table 1. The average heat flux for steady state operating conditions.

	ONB[w/cm^2]	OFI [w/cm^2]	CHF [w/cm^2]	BO[w/cm^2]
Present study	89.44	132.65	229.72	698.84
Khedr (2008)	41.97	208	265.6 (Mirshak)	NA

Table 2. Safety criteria for steady state operating conditions.

	ONBR	OFIR	CHFR	BOR	V_{critic}
Present study	1.70	2.48	4.3	13.08	11.40
Khedr (2008)	1.95	3.88	4.95 (Mirshak)	NA	10.94

Before going forward in our study, in this section, a verification of the correlations used to evaluate the safety criteria, ONBR, OFIR, DNBR and BOR is performed separately, throughout the uses of steady state solution. So, after we have calculated the different safety criteria, thereafter is compared with the results of Khedr (2008), as given in tables 1 and 2.

In table 1, we present a comparison between the average heat fluxes of our calculation with those of Khedr (2008) at steady conditions, where a significant difference between both results is noticed may be due to the difference of used correlations.

Despite the significant difference in the average heat fluxes, an acceptable difference is obtained for the safety criteria as presented in table 2, below.

Also, in figure 1, we present the variation of the ONB and the saturation temperatures along the active length during steady state conditions; it's obvious that the difference between both temperatures is about 10°C. And the onset of nucleate boiling can occur only if the cladding temperatures exceed the ONB temperature (124°C).

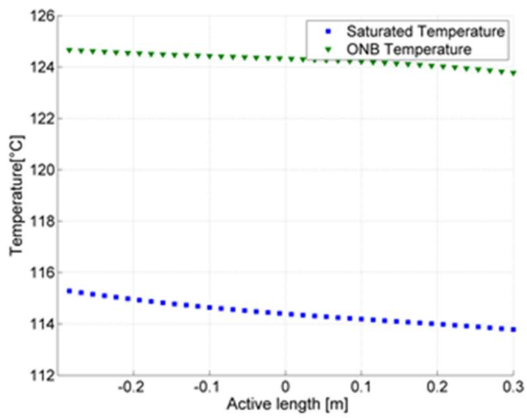

Fig. 1. The variation of the ONB and the saturation temperatures along the active length.

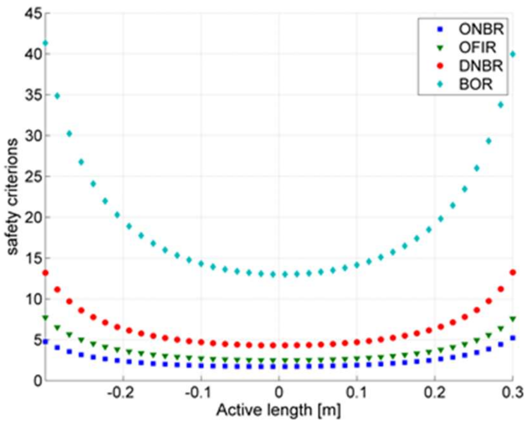

Fig. 2. The variation of the safety criteria along the active length.

While, in figure 2, the variation of all the safety criteria along channel active length are presented, where all of them have a parabolic profile with a minimum value at the fuel plate center, because at this position the local heat flux released by the fuel is maximal.

5.1. The protected loss of flow accident

In this accident, the mass flow rate decreased exponentially as mentioned in Eq. 1, with a time constant for the pump coast down equal to 1s and 25 s, respectively for fast and slow loss of flow, and when the mass flow scram condition is reached, after we lost 15% of the nominal mass flow rate, then the control rode are fully inserted with a reactivity rat of (-10$/0.5s) and a delay time of 0.2s, to stop and bring the reactor back to a safe state.

During this accident, the reactor power decreased according to three different phases, in the first one, the reactor power decreased slightly by the reactivity feedback effect until the mass flow scram condition is reached, then the reactor power variation enter in the second phase, where is decreased rapidly as a function of the negative reactivity generated by the insertion of the control rode, but after the reactor is completely scrammed, the reactor power variation inter in the third and last phase, where it becomes produced only by the decay heat, as presented in figures 3 and 4.

As well, for the temperatures of fuel and coolant their transient variation is also can be defined by three different phases, in the first one, the temperatures increased until reach their maximum values at the reactor scram point, followed by a rapid decrease proportional to the reactor power, thereafter, is slightly increased again at the end of last phase, as shown in figures 3 and 4.

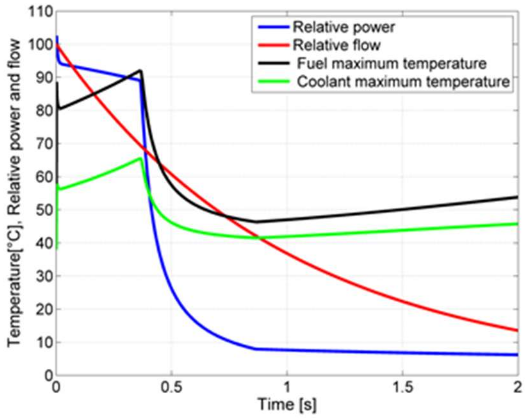

Fig. 3. The reactor power and the hottest channel thermal hydraulic parameters transient variation during FLOF.

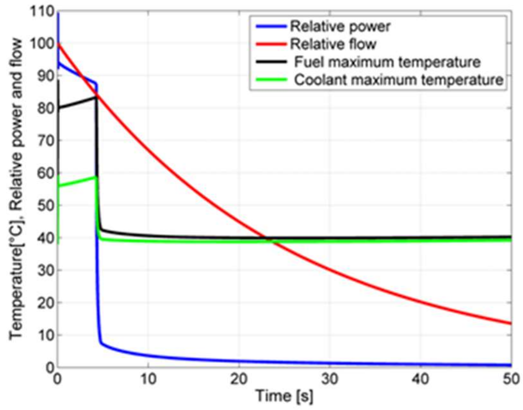

Fig. 4. The reactor power and the hottest channel thermal hydraulic parameters transient variation during SLOF.

In the figures 5 and 6, we showed, the transient variation of the minimum value of safety criteria (OFIR, DNBR and BOR), for fast and slow losses of flow accidents respectively, where it's evident, their change is also proportional to the reactor power, so firstly, they are decreased slightly to reach their lowest values at the scram point, succeeded by a sharp increase in the second phase and when they reach the third phase, are decreased again, but anyway never fall below to the considered safety limits.

In the tables 3 and 4, a comparison between this model and other models or code systems results are presented, where we interested in the reactor power, only at the scram point, and also, the fuel and coolant temperatures, firstly at the scram point where they reach their highest value and finally at the end of the forced convection cooling mode, when the mass flow become equal to 15% of the nominal mass flow rate. After the analysis of the results, an acceptable difference is remarked between our results and the results of the other models or code systems, so we can consider that the proposed model has a good ability to predict accurately the reactor behavior during a protected loss of flow accident.

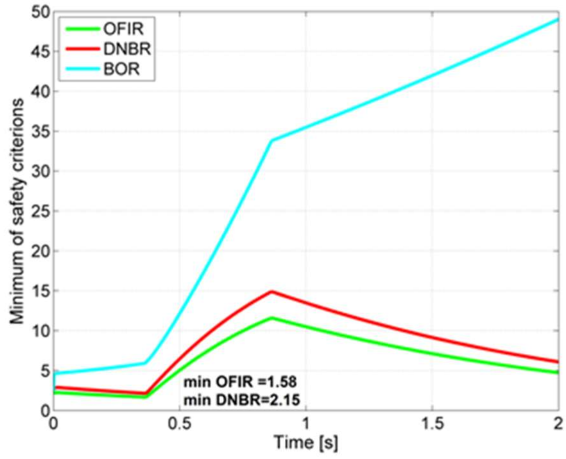

Fig. 5. The transient variation of the minimum safety criteria during FLOF.

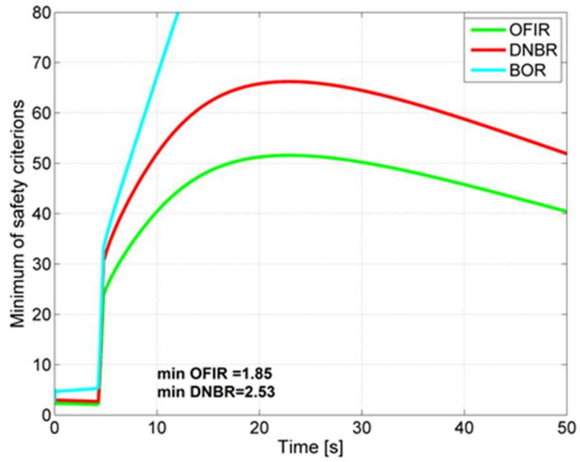

Fig. 6. The transient variation of the minimum safety criteria during SLOF.

Table 3. Results comparison and analysis of fast loss of flow accident.

	Present model	Gaheen et al. (2007)	RELAP5/3.2, Gaheen et al. (2007)	PARET, Gaheen et al. (2007)
Fast loss of flow (LEU)				
Power (MW) at scram	10.69	11.7	11.83	NA
Temperatures (°C) at scram point				
Fuel	91.94	92.75	NA	90.3
Cladding	NA	88.27	92.58	87.5
Coolant	65.42	59.46	59.50	60.3
Temperatures (°C) at the end of forced convection cooling mode				
Fuel	52.97	58.29	NA	58.5
Cladding	NA	57.78	NA	58.2
Coolant	45.34	47.01	46.70	46.5

Table 4. Results comparison and analysis of slow loss of flow accident.

	Present model	Gaheen et al. (2007)	RELAP5/3.2, Gaheen et al. (2007)	PARET, Gaheen et al. (2007)
Fast loss of flow (LEU)				
Power (MW) at scram	10.52	11.69	11.56	NA
Temperatures (°C) at scram point				
Fuel	83.03	89.48	NA	86.8
Cladding	NA	84.78	88.41	83.7
Coolant	58.32	58.19	57.97	58.8
Temperatures (°C) at the end of forced convection cooling mode				
Fuel	39.75	47.42	NA	48.4
Cladding	NA	47.23	49.38	48.3
Coolant	38.58	42.66	43.50	43.3

6. Conclusion

The code systems are an essential calculation tool for nuclear reactor safety analysis, but their complicated uses, especially for modelling MTR research reactors, favorite in the last few decades, the development of more simplified thermal hydraulic codes, with the same capability to provide an accurate prediction of the reactor behavior during steady state and transient operating conditions.

In the present work, a simplified thermal hydraulic computer program based on the lumped parameter approach is developed, to investigate, that the protected loss of flow accident will not lead to any reactor core safety issues, where the safety criteria, should not exceed the imposed safety limits.

So after we have evaluated the transient variation of the maximum fuel and coolant temperatures, furthermore, to the safety criteria (ONBR, OFIR, CHFR and BOR), our main Remarks about the obtained results are summarized as follows:

An acceptable difference is remarked between the results provided by the proposed lumped parameter model and the other results carried out by different models or code systems, so the proposed model is sufficiently accurate to predict the transient behavior of the reactor core, if the fluid flow remained homogeneous.

According to, the transient variation of the minimum value of the safety criteria, they are completely satisfied with the imposed safety limits, so the protected loss of flow accident will not lead to any reactor core safety issues.

The lumped parameter approach is applied successfully in this study to deal with the LOFA accident only, but can be also applied to study the reactivity insertion accident as well.

Disclosures

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Nomenclature

Symbol

A	Areas, m2
D	Hydraulic Diameter, m
L	Length, m
G	Mass flux, Kgm-2s-1
g	Gravitational Acceleration, ms-2
P	Core Average Power, W
Ci	Delayed Neutron Precursors
ρe	Total Reactivity, $
ρext	Reactivity of Control Rode, $
vc	Coolant velocity, ms-1
T̀	Local Temperature, °C
T	Average Temperature, °C
P̀	Local power per unit of volume, Wm-3
H	Fuel length, m
h	Convective Heat Transfer Coefficient, Wm-2°C-1
q''	Local Heat Flux, Wm-2
V	Volume, m3
t	Time, s

Subscript

c,l	Coolant
f	Fuel
g	water vapor
h	Heated
P	Pool
z	Z axis
*	Dimensionless
W	Wall
Sat	Saturation
Sub	Sub cooled
critic	Critical

Greek Letter

ρ	Density, Kgm-3
α	Reactivity Feedback Coefficient, $\$°C^{-1}$
β	Total Delayed Neutron Fraction
Λ	Mean Neutron Generation Time, s
λ	Precursor decay constant, s^{-1}
σ	Water Surface Tension, Nm^{-1}

Abbreviation

Nu	Nusselt Number
Re	Reynolds Number
Pr	Prandtl Number
ONB	Onset of Nucleate Boiling
ONBR	Onset of Nucleate Boiling Ratio
CHF	Critical heat flux
CHFR	Critical heat flux Ratio
OFI	Onset of Flow Instability
OFIR	Onset of Flow Instability Ratio
BO	Burn Out
BOR	Burn Out Ratio
MCP	Main coolant pump

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