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A SIMPLIFIED MODEL FOR DYNAMIC SIMULATION OF THE PRIMARY CIRCUIT OF PRESSURIZED WATER REACTORS

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Abstract. *In this paper, we present a mathematical model for the simulation of transient neutronic and thermohydraulic behaviour of the PWR core and the primary circuit of a nuclear power plant. The point kinetics equations are used to model the core neutronics and the improved lumped parameter formulation proposed by Regis et al. (2000) and Su and Cotta (2001) is used to model the fuel dynamics. A two-region model is used for the pressurizer and for the primary-side of the steam generator. A pump model is implemented to obtain the flowrate variation during total or partial loss of flow. Several transient events and accidents are simulated and qualitatively agree with the results of more detailed engineering models.*

keywords: *Nuclear reactor thermal-hydraulics, pressurized water reactors, simulators, lumped models, simplified models*

1. Introduction

The lumped parameter approach has been widely used in the thermohydraulic analysis of nuclear reactors. As in the analysis of other complex thermal systems, this classical approach is extremely useful and sometimes even mandatory when a simplified formulation of the transient heat conduction is sought. Together with the neutron point kinetics model, the lumped parameter approach for fuel rod heat conduction is essential in the simplified models of pressurized water reactors (PWRs) and in real-time simulators of nuclear power plants (Zanobetti, 1989). Accurate prediction of reactor core behaviour during transients and accidents is of major concern for safe operation of nuclear power plants of pressurized water reactors. Computer programs such as COBRA are available for analysis of transient core behaviour. Nevertheless, simplified approaches have been paralleled large system codes to provide understanding of the physical phenomena and to show consistency with the large code analysis. In simplified core thermohydraulic analysis lumped parameter models have been used widely to obtain transient temperature behaviour in fuel and cladding ((Tong and Weisman, 1979) and (Levy, 1999)).

Recently, the dynamics of chaotic instabilites in boiling water reactors has aroused increased interests. In such studies, the lumped parameter approach has been the unique option in the fuel dynamics models. For example, Rao et al. (1995) performed a linear stability analysis in the frequency domain to study the basic mechanism of coupled nuclear-thermal instabilities in a boiling channel, using a one-node lumped parameter model for the fuel dynamics. Even with the simplest fuel dynamics model, they found that the fuel-time constant was one of the parameters determining the density-wave instability. Chang and Lahey (1997) used one-dimensional homogeneous equilibrium assumptions for diabatic two-phase flow, a one-node lumped parameter approach for heated wall dynamics, and neutron point kinetics for the consideration of nuclear feedback in a boiling water reactor (BWR) loop. They found that a boiling channel coupled with a riser could experience chaotic oscillations. Lin et al. (1998) found a strip of limit cycle oscillation of a nuclear-coupled boiling channel with a two-node lumped parameter model for the fuel dynamics, where one node was for the fuel and the other for the cladding.

As an inherent limitation of the lumped parameter approach, moderate to low temperature gradients within the region are assumed, which through the associated problem parameters, governs the accuracy of such approximate formulations. As a rule of thumb, the classical lumped parameter approach, where uniform temperature is assumed within the region, is in general restricted to problems with Biot number less than 0.1. In most

nuclear reactor engineering problems, the Biot number is much higher. In other words, the moderate to low temperature gradient assumption is not reasonable in such applications, thus more accurate approach should be adopted. Cotta and Mikhailov (1997) proposed a systematic formalism to provide improved lumped parameter formulation for steady and transient heat conduction problems based on Hermite approximation for integrals that define averaged temperatures and heat fluxes. This approach has been shown to be efficient in a great variety of pratical applications (Aparecido and Cotta, 1989; Scofano Neto and Cotta,1993; Cheoroto et al., 1999).

In this paper, we present a mathematical model to simulate transient neutronic and thermohydraulic behaviour of the PWR core and the primary circuit of a nuclear power plant. The point kinetics equations are used to model the core neutronics and the improved lumped parameter formulation proposed by Regis et al. (2000) and Su and Cotta (2001) is used to model the fuel dynamics. A two-region model is used for the pressurizer and for the primary-side of the steam generator. A pump model is implemented to obtain the flowrate variation during total or partial loss of flow.

2. Analysis

2.1 Fuel Dynamics

We consider the transient heat conduction in a cylindrical nuclear fuel rod such as those that can be found in pressurized water reactors (PWRs), boiling water reactors (BWRs), or liquid metal cooled fast breeder reactors (LMFBRs). In order to illustrate the main idea of improved lumped analysis, we simplify the problem by assuming axisymmetry of temperature distribution and neglecting the axial heat conduction term and the spatial variation of the heat generation across the fuel rod. The thermal conductivities are assumed to be independent of temperature, while this assumption is not essential in the lumped parameter approach as can be seen further in the analysis. The heat generation in the cladding is neglected. With the above assumptions, we have the following governing equations with appropriate boundary and initial conditions for one-dimensional transient heat conduction in a fuel rod,

$$\rho_f c_f \frac{\partial T_f}{\partial t} = \frac{1}{r}\frac{\partial}{\partial r}\left(k_f r \frac{\partial T_f}{\partial r}\right) + g(t), \qquad 0 < r < r_{fo} \tag{1a}$$

$$T_f(r,0) = T_{f0}(r), \tag{1b}$$

$$-k_f \beta_f \frac{\partial T_f}{\partial r}\Big|_{r=r_{fo}} = hg\Big(T_f(r_{fo},t) - T_c(r_{ci},t)\Big), \tag{1c}$$

$$\rho_c c_c \frac{\partial T_c}{\partial t} = \frac{1}{r}\frac{\partial}{\partial r}\left(k_c r \frac{\partial T_c}{\partial r}\right), \qquad r_{ci} < r < r_{co} \tag{2a}$$

$$T_c(r,0) = T_{c0}(r), \tag{2b}$$

$$-k_c \beta_c \frac{\partial T_c}{\partial r}\Big|_{r=r_{ci}} = hg\Big(T_f(r_{fo},t) - T_c(r_{ci},t)\Big), \tag{2c}$$

$$-k_c \frac{\partial T_c}{\partial r}\Big|_{r=r_{co}} = h(T_f(r_{co},t) - T_m), \tag{2d}$$

where T_f and T_c are temperatures in fuel and cladding, ρ_f, ρ_c their densities, c_f, c_c the specific heats, k_f, k_c the respective thermal conductivities, g the volumetric heat generation in fuel, h_g the heat transfer coefficient for the gap and h the heat transfer coefficient between the cladding and the coolant, while r_{fo}, r_{ci}, r_{co}, $\beta_f = r_{fo}/r_{ci}$ and $\beta_c = r_{ci}/r_{fo}$ are geometric parameters of the fuel rod.

Introducing the following dimensionless variables,

$$\theta_f = \frac{T_f - T_m}{T_{f0} - T_{m0}}, \qquad \theta_c = \frac{T_c - T_m}{T_{f0} - T_{m0}},$$

$$R = \frac{r}{r_{co}}, \qquad \tau = \frac{k_f t}{\rho_f c_f r_{co}^2},$$

$$K = \frac{k_f \rho_c c_c}{k_c \rho_f c_f}, \qquad Bi = \frac{h r_{co}}{k_c},$$

$$Bi_{gc} = \frac{h_g r_{co}}{\beta_c k_c}, \qquad Bi_{gf} = \frac{h_g r_{co}}{\beta_f k_f},$$

$$G = \frac{r_{co}^2 g}{k_f (T_{f0} - T_{m0})},$$

we get the following dimensionless equations,

$$\frac{\partial\theta_f}{\partial\tau} = \frac{1}{R}\frac{\partial}{\partial R}\Big(R\frac{\partial\theta_f}{\partial R}\Big) + G(\tau), \qquad 0 < R < R_{fo} \tag{3a}$$

$$\theta_f(R,0) = \theta_{f0}(R), \tag{3b}$$

$$-\frac{\partial\theta_f}{\partial R}|_{R=R_{fo}} = Bi_{gf}\Big(\theta_f(R_{fo},\tau) - \theta_c(R_{ci},\tau)\Big), \tag{3c}$$

$$\frac{\partial\theta_c}{\partial\tau} = \frac{K}{R}\frac{\partial}{\partial R}(R\frac{\partial\theta_c}{\partial R}), \qquad R_{ci} < R < 1 \tag{4a}$$

$$\theta_c(r,0) = \theta_{c0}(r), \tag{4b}$$

$$-\frac{\partial\theta_c}{\partial R}|_{R=R_{ci}} = Bi_{gc}(\theta_f(R_{fo},\tau) - \theta_c(R_{ci},\tau)), \tag{4c}$$

$$-\frac{\partial\theta_f}{\partial R}|_{R=1} = Bi\theta_c(1,\tau). \tag{4d}$$

The corresponding spatially averaged dimensionless temperatures are defined by

$$\theta_{f,av}(\tau) = \frac{\int_0^{R_{fo}} 2\pi R\theta_f(R,\tau)dR}{\pi R_{fo}^2} = \frac{2}{R_{fo}^2}\int_0^{R_{fo}} R\theta_f(R,\tau)dR, \tag{5a}$$

$$\theta_{c,av}(\tau) = \frac{\int_{R_{ci}}^1 2\pi R\theta_c(R,\tau)dR}{\pi(1-R_{ci}^2)} = \frac{2}{(1-R_{ci}^2)}\int_{R_{ci}}^1 R\theta_c(R,\tau)dR, \tag{5b}$$

Operating Eq. (3a) by $(2/R_{fo}^2)\int_0^{R_{fo}} RdR$, and using the definition of average temperature, Eq. (5a), we get,

$$\frac{d\theta_{f,av}(\tau)}{d\tau} = \frac{2}{R_{fo}}\frac{\partial\theta_f}{\partial R}\Big|_{R=R_{fo}} + G(\tau). \tag{6}$$

Similarly, we operate Eq. (4a) by $(2/(1-R_{ci}^2))\int_{R_{ci}}^1 RdR$, using the definition of average temperature, Eq. (5b), which yields,

$$\frac{d\theta_{c,av}(\tau)}{d\tau} = -\frac{2K}{(1-R_{ci}^2)}\Big(R_{ci}\frac{\partial\theta_c}{\partial R}\Big|_{R=R_{ci}} - \frac{\partial\theta_c}{\partial R}\Big|_{R=1}\Big). \tag{7}$$

Now, using the boundary conditions Eqs. (3c, 4c, 4d), we get

$$\frac{d\theta_{f,av}(\tau)}{d\tau} = -\frac{2Bi_{gf}}{R_{fo}}\Big(\theta_f(R_{fo},\tau) - \theta_c(R_{ci},\tau)\Big) + G(\tau), \tag{8}$$

$$\frac{d\theta_{c,av}(\tau)}{d\tau} = -\frac{2K}{(1-R_{ci}^2)}\Big[(Bi_{gc}R_{ci}\Big(\theta_f(R_{fo},\tau) - \theta_c(R_{ci},\tau)\Big) - Bi\theta_c(1,\tau)\Big]. \tag{9}$$

The Eqs. (8) and (9) are equivalent integro-differential formulation of the original mathematical model, with no approximation involved. Supposing that the temperature gradients are sufficiently smooth over the whole spatial solution domain, the classical lumped system analysis (CLSA) is based on assuming that the boundary potentials can be reasonably well approximated by the averaged potentials, as

$$\theta_f(R_{fo},\tau) \cong \theta_{f,av}(\tau),$$
$$\theta_c(R_{ci},\tau) \cong \theta_{c,av}(\tau),$$
$$\theta_c(1,\tau) \cong \theta_{c,av}(\tau),$$

which leads to the simplified lumped formulation,

$$\frac{d\theta_{f,av}(\tau)}{d\tau} = -\frac{2Bi_{gf}}{R_{fo}}\Big(\theta_{f,av}(\tau) - \theta_{c,av}(\tau)\Big) + G(\tau), \tag{10}$$

$$\frac{d\theta_{c,av}(\tau)}{d\tau} = \frac{2K}{(1-R_{ci}^2)}\Big[Bi_{gc}R_{ci}\Big(\theta_{f,av}(\tau) - \theta_{c,av}(\tau)\Big) - Bi\theta_{c,av}(\tau)\Big], \tag{11}$$

to be solved with the initial conditions for the averaged temperatures,

$$\theta_{f,av}(0) = \theta_{f,av0}, \tag{12a}$$

$$\theta_{c,av}(0) = \theta_{c,av0}. \tag{12b}$$

We now seek improved lumped-differential formulations, in an attempt to offer enhanced characteristic to the approximation path previously proposed. The basic idea is to provide a better relation between the boundary potentials and the averaged potentials, which are to be developed from Hermite-type approximations of the integrals that define the average temperatures and heat fluxes.

In this formulation, the two-sided corrected trapezoidal rule ($H_{1,1}$ approximation) is employed in the averaged temperature integrals for both fuel and the cladding, and plain trapezoidal rule ($H_{0,0}$ approximation) is used in the averaged heat fluxes, in the following form:

$$\theta_{f,av}(\tau) = \frac{2}{R_{fo}^2}\Big[\frac{R_{fo}}{2}(R\theta_f)\Big|_{R=0} + \frac{R_{fo}}{2}(R\theta_f)\Big|_{R=R_{fo}} + \frac{R_{fo}^2}{12}\frac{\partial(R\theta_f)}{\partial R}\Big|_{R=0} - \frac{R_{fo}^2}{12}\frac{\partial(R\theta_f)}{\partial R}\Big|_{R=R_{fo}}\Big], \tag{13}$$

$$\int_0^{R_{fo}} \frac{\partial\theta_f(R,\tau)}{\partial R}dR = \theta_f(R_{fo},\tau) - \theta_f(0,\tau) = \frac{1}{2}\Big(\frac{\partial\theta_f}{\partial R}\Big|_{R=0} + \frac{\partial\theta_f}{\partial R}\Big|_{R=R_{fo}}\Big), \tag{14}$$

$$\theta_{c,av}(\tau) = \frac{2}{(1-R_{ci}^2)}\Big[\frac{1}{2}(R\theta_c)\Big|_{R=R_{ci}} + \frac{1}{2}(R\theta_c)\Big|_{R=1} + \frac{1}{12}\frac{\partial R\theta_c}{\partial R}\Big|_{R=R_{ci}} - \frac{1}{12}\frac{\partial R\theta_c}{\partial R}\Big|_{R=1}\Big], \tag{15}$$

$$\int_{R_{ci}}^1 \frac{\partial\theta_f(R,\tau)}{\partial R}dR = \theta_c(1,\tau) - \theta_c(R_{ci},\tau) = \frac{1}{2}\Big(\frac{\partial\theta_c}{\partial R}\Big|_{R=R_{ci}} + \frac{\partial\theta_c}{\partial R}\Big|_{R=1}\Big). \tag{16}$$

Using boundary conditions Eqs. (3c, 4c, 4d), the Eqs. (14,16) become

$$\theta_{f,av}(\tau) = \frac{5}{6}\theta_f(R_{fo},\tau) + \frac{1}{6}\theta_f(0,\tau)$$
$$+\frac{1}{6}(R_{fo})Bigf\theta_f(R_{fo},\tau) - \theta_c(R_{ci},\tau) \tag{17}$$

$$\theta_{c,av}(\tau) = \frac{1}{1+R_{ci}}\Big[R_{ci}\theta_c(R_{ci},\tau)+\theta_c(1,\tau)+\frac{1-R_{ci}}{6}\theta_c(R_{ci},\tau)-R_{ci}Bigc\Big(\theta_f(R_{fo},\tau)-\theta_c(R_{ci},\tau)\Big)+Bi\theta_c(1,t)-\theta_c(1,t)\Big]. \tag{18}$$

Eqs. (13,15,17,18) form a system of four linear algebraic equations for four unknowns, $\theta_f(0,\tau)$, $\theta_f(R_{fo},\tau)$, $\theta_c(R_{ci},\tau)$, and $\theta_c(1,\tau)$, that is solved to provide the seeked relations between boundary potentials and averaged potentials, besides an approximate relation for the central temperature, $\theta_f(0,\tau)$. These relations are then used in the Eqs. (8) and (9) to close the two ordinary differential equations for the averaged temperatures, to be solved with the initial conditions (12a, b).

2.2 Reactor Core Neutronics

The core neutronics is described by the point kinetics equations with six delayed neutron precursor groups (Onega and Karcher, 1977). The reactivity in the point kinetics equation depends upon the spatially averaged, time-dependent fuel and coolant temperatures, hence, it couples the core neutronics with the thermal-hydraulics. The point kinetics equations are written as

$$\frac{dP(t)}{dt} = \frac{\rho(t)-\beta}{\Lambda}P(t) + \sum_{m=1}^{G}\lambda_m C_m(t), \tag{19}$$

$$\frac{dC_m(t)}{dt} = \frac{\beta_m}{\Lambda}P(t) - \lambda_m C_m(t), \quad m = 1,2,...,G, \tag{20}$$

where P is the total reactor power, C_m the power equivalent of the mth delayed neutron precursor group, ρ the total reactivity, β_m the fraction of delayed neutrons in the mth group, Λ the neutron generation time, λ_m the decay constant of the mth delayed neutron group. The reactivity is the sum of an externally inserted reactivity $\rho_{ext}(t)$ and the feedback reactivities:

$$\rho(t) = \rho_{ext}(t) + \alpha_f(T_f)\delta T_f(t) + \alpha_m\delta T_m(t).$$

The δT_f and δT_m are deviations of the average fuel temperature, $T_f(t)$, and bulk coolant temperature, $T_m(t)$, from their equilibrium values T_{f0} and T_{m0}, respectively. The fuel temperature of reactivity α_f is given by

$$\alpha_f(T_f) = \frac{\gamma}{2\sqrt{T_f}} lnP(T_{f0}),$$

where γ is a constant that depends only on fuel composition and geometry. The coolant temperature coefficient is nearly a constant over a fairly wide range of operating conditions and is assumed constant in this work.

2.3 Primary Coolant Temperature

The primary coolant energy equation can be written as (Todreas and Kazimi, 1990)

$$M_c c_p \frac{dT_m(t)}{dt} = hS_c(T_c(r_{co}, t) - T_m(t)) - 2\dot{m}c_p(T_m(t) - T_{m0}), \tag{21}$$

where $T_m(t)$ is the average coolant temperature in the core, T_{m0} the inlet coolant temperature, $T_c(r_{co}, t)$ the temperature at external surface of cladding, M_c the total mass of primary coolant, c_p the specific heat of the coolant, h the heat transfer coefficient, and S_c the total heat transfer area.

2.4 Pressurizer

The governing equations for the dynamics of the pressurizer are written as

$$\frac{dE_p}{dt} = P_h - (h_{swp} - h_{spr}) + h_{swp}(W_{sg} + W_r) \tag{22}$$

$$\frac{dM_p}{dt} = W_{sg} + W_r \tag{23}$$

$$\tau_r \frac{dW_r}{dt} = G_r(H_(pc) - H_p) - W_r \tag{24}$$

$$E_p = h_{sp}M_{sp} + h_{swp}M_{swp} \tag{25}$$

$$M_p = M_{sp} + M_{swp} \tag{26}$$

$$V_p = v_{sp}M_{sp} + V_{swp}M_{swp} \tag{27}$$

$$H_p = \frac{M_{swp}}{\rho S_p} \tag{28}$$

where E_p, M_p, and V_p are respectively the total energy, mass and volume of the pressurizer, H_p is the height of liquid column and S_p is the cross-section area of the pressurizer.

2.5 Pump Transient

The governing equation for pump transient in a single phase flow loop is written as (Todreas and Kazimi, 1990)

$$(\sum_k \frac{L_k}{A_k}) \frac{d\dot{m}}{dt} = \frac{\Delta p_R}{\dot{m}_R^2}\dot{m}^2 - \frac{1}{2}\frac{R}{\rho_l}(\dot{m})^{2-n} \tag{29}$$

where the subsricpt R denotes rated conditions.

2.6 Primary Side of the Steam Generator

The energy balance equation for the primary side of the steam generator is given by

$$M_G c_p \frac{dT_1}{dt} = \dot{m}c_p(T_{1i} - T1o) - UA(T_1 - T_2) \tag{30}$$

where M_G is the total mass of the primary fluid in the steam generator, T_1 and T_2 are respectively the average temperatures of the primary and secondary sides of the steam generator, T_{1i} and T_{1o} are respectively the inlet and outlet temperatures of the primary side of the steam generator.

3. Results and Discussion

The Eqs. (10,11,19-24, 29, 20) form a set of 15 coupled ordinary differential equations. With specified initial equilibrium state, the ordinary differential equations are solved numerically as an initial value problem by using an algorithm for stiff systems (Press et al., 1992). In this work, we implemented the higher order improved lumped parameter formulation which uses two-side corrected trapezoid rule ($H_{1,1}$) for average fuel and clad temperature integrals and plain trapezoid rule ($H_{0,0}$) for heat flux integrals (Su and Cotta, 2001). For comparison purpose, we also implement a classical lumped formulation for fuel dynamics as that described in Levy (1999).

In Figs. 1 to 4 we show numerical results of a simulation for a step insertion of reactivity of 0.3 dollars. Transient behaviours of power, fuel temperature, clad temperature and average coolant temperatures simulated

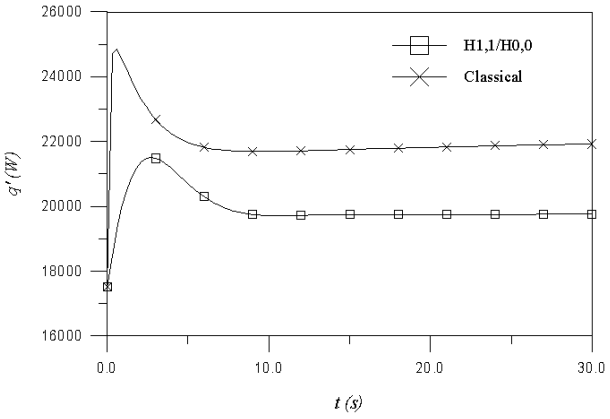

Figure 1: Transient behaviour of power for step insertion of reactivity.

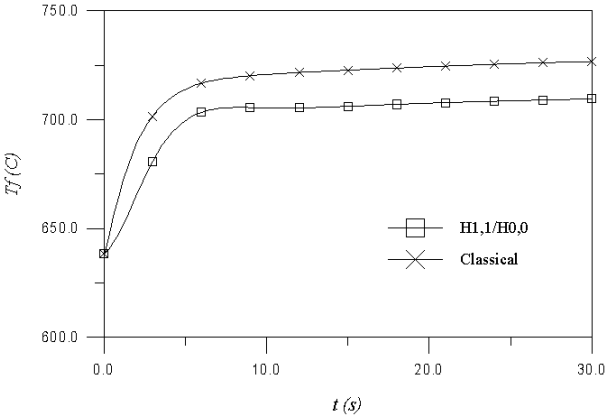

Figure 2: Transient behaviour of average fuel temperature for step insertion of reactivity.

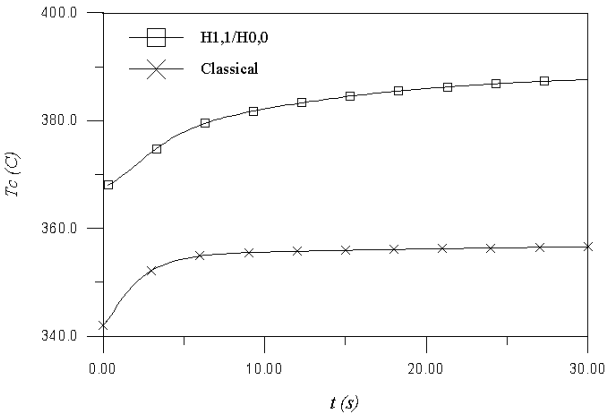

Figure 3: Transient behaviour of average clad temperature for step insertion of reactivity.

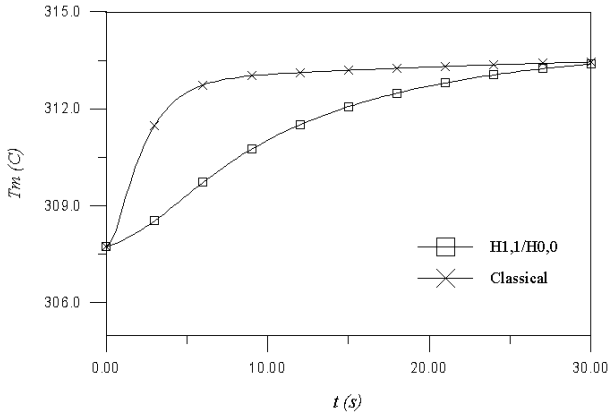

Figure 4: Transient behaviour of average coolant temperature for step insertion of reactivity.

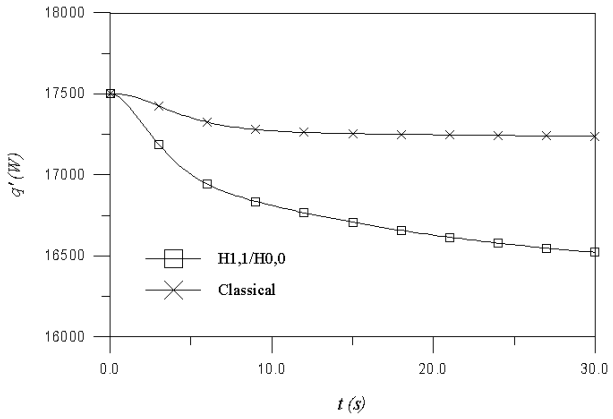

Figure 5: Transient behaviour of power for partial loss of flow.

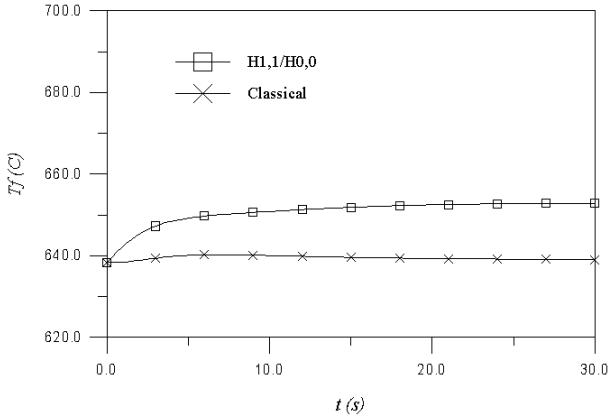

Figure 6: Transient behaviour of average fuel temperature for partial loss of flow.

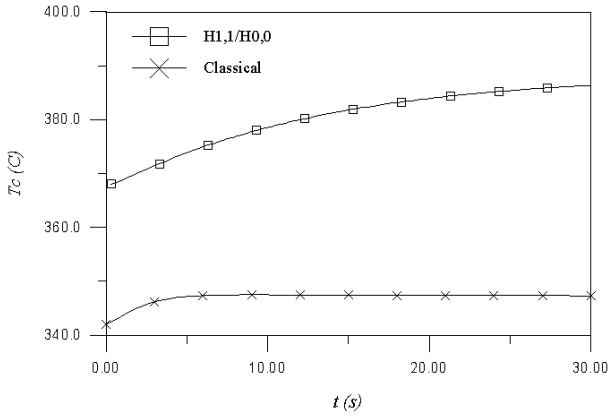

Figure 7: Transient behaviour of average clad temperature for partial loss of flow.

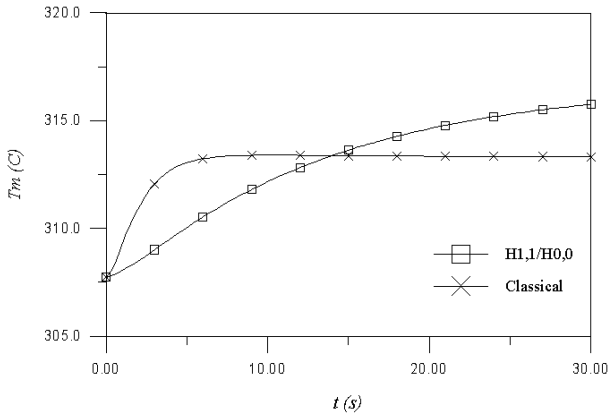

Figure 8: Transient behaviour of average coolant temperature for partial loss of flow.

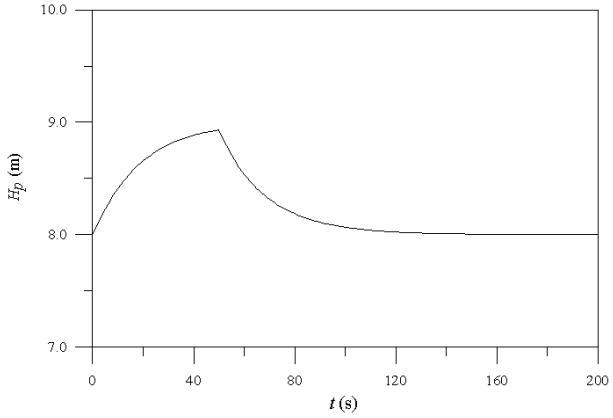

Figure 9: Transient behaviour of the liquid column height in the pressurizer during a insurge.

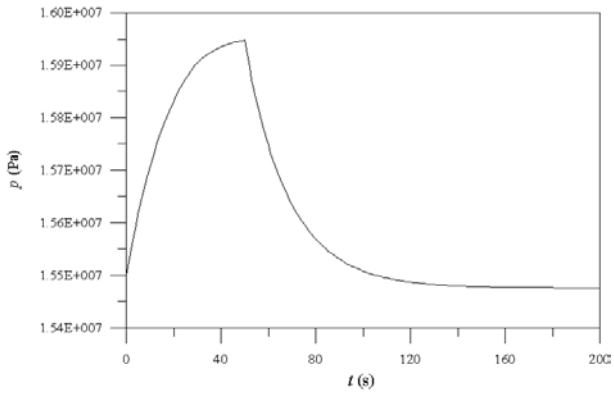

Figure 10: Transient behaviour of the pressure in the pressurizer during a insurge.

by both methods are given. In Fig. 1, we notice that the model with classical formulation for fuel dynamics predicts a larger power overshoot than predicted by the improved lumped formulation. Similarly, the classical formulation gives a higher fuel temperature than that given by the improved formulation as shown in Fig. 2. Su and Cotta (2001) have shown by comparison with more accurate solution obtained by finite difference method that the improved formulation gives more accurate fuel temperature than the classical lumped formulation as described in Levy (1999). On the other hand, the improved lumped model predicts a higher average clad temperature than the classical formulation as can be seen in Fig. 3. Although after a period of 30 seconds both formulation predict the same assymptotic value for average coolant temperature, the coolant temperature rising rate predicted by the improved formulation is slower as shown in Fig. 4.

Transients induced by a partial loss of one-fourth flow rate are simulated with the numerical results shown in Figs. 5 to 8. The improved formulation predicts a large power drop than the classical formulation as seen in Fig. 5. While the classical formulation gives a nearly constant fuel temperature, the improved formulation predicts a slight rise in fuel temperature that will induce a negative reactivity to reduce the power level. The improved and classical formulations predict quite different behaviours for the clad and coolant temperatures. The clad and coolant temperatures approach steady state quite rapidly as predicted by the classical formulation, while the improved formulation gives steadily rising clad and coolant temperatures as shown in Figs. 7 and 8.

Transient behaviour of the pressurizer during an insurge is shown in Figs. 9 and 10. We can see that the liquid column height and the pressure increase during the insurge but quickly return to the steady state operation conditions due to the auto-regulating capacity of the pressurizer.

4. conclusions

A higher order lumped parameter analysis is applied to the transient heat transfer in a nuclear reactor fuel rod to provide a simplified formulation that can be used in stability analysis of BWRs, simplified models of PWRs or real-time simulators of nuclear power plants.

In this work, we simulated the nonlinear dynamics of a pressurized water reactor by using the proposed improved lumped parameter formulation for the fuel dynamics. The model with improved lumped formulation predicts quite different transient behaviours for a partial loss of flow event. While the proposed model predicts reasonably well the main physical phenomena of the simulated events, its accuracy needs to be assessed in comparison with more accurate computational codes.

5. Acknowledgment

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