An intrinsically safe facility for forefront research and training on nuclear technologies —Kinetics and dynamics

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1 Neutron kinetics

The treatment of the proposed ADS neutron kinetics is a complex task, due to the subcriticality level of the system, $k_{\text{eff}} \sim 0.95$. In such a system the neutron population cannot be simply described by means of the well-assessed point kinetic models, which are known to be valid for systems near criticality. Higher modes have been proved to be required, even far from the source [1]; on the other hand, Monte Carlo (MC) simulations are excessively time demanding, due to the large differences in neutron fluxes expected in different zones of the system. Being the expected ratio of the fluxes between inner and outer core zones of the order of 10, or more, either a very large number of neutrons injected into the system is required to obtain a sufficient statistics in the outer zones, or, alternatively, complex variance reduction methods are to be employed. Consequently some hours of computer time are required anyway for each time bin needed. A qualitative or semi-quantitative approximate treatment of the kinetic behaviour of the system is needed, at least to identify definite time intervals and/or geometrical constraints for accurate Monte Carlo simulations.

It has been shown [2] that, for $k_{\text{eff}} \sim 0.95$, more than $\sim 99\%$ of the total power produced in the system comes from prompt neutrons: the transients are then expected to be very fast, the system practically reaching the steady state within few tens of microseconds. It is then reasonable to describe neutron kinetics in a simple prompt neutrons only, multigroup diffusive approach for an equivalent homogeneous system. This problem is solvable almost analytically [3]: the solution has been validated within 1–2% on the estimate of k_{eff} against the corresponding static exact solution for the same simple system obtained by means of MC simulation; therefore, it is then assumed as a starting point for a preliminary study of the AdS neutron kinetics. Two remarks should be made about the validity of such an approach:

- The first concerns the expected errors for the neutron fluxes: since neutron flux is proportional to $1/(k_{\text{eff}} - 1)$, the expected (absolute) error on the flux (or, equivalently, on the power) from this kind of calculations is an underestimation of $\sim 20\%$ for $k_{\text{eff}} \sim 0.95$ (that would become $\sim 50\%$ for $k_{\text{eff}} \sim 0.98$), which can be easily corrected by proper renormalization of the fluxes.
- The second concerns the time scales involved: time scales for neutron kinetic effects lie in the range of some microseconds (or, at the most, tens of microseconds), which is several orders of magnitude less than the thermal-hydraulics and mechanics characteristic time constants. Consequently the quoted approximation can be considered acceptable.

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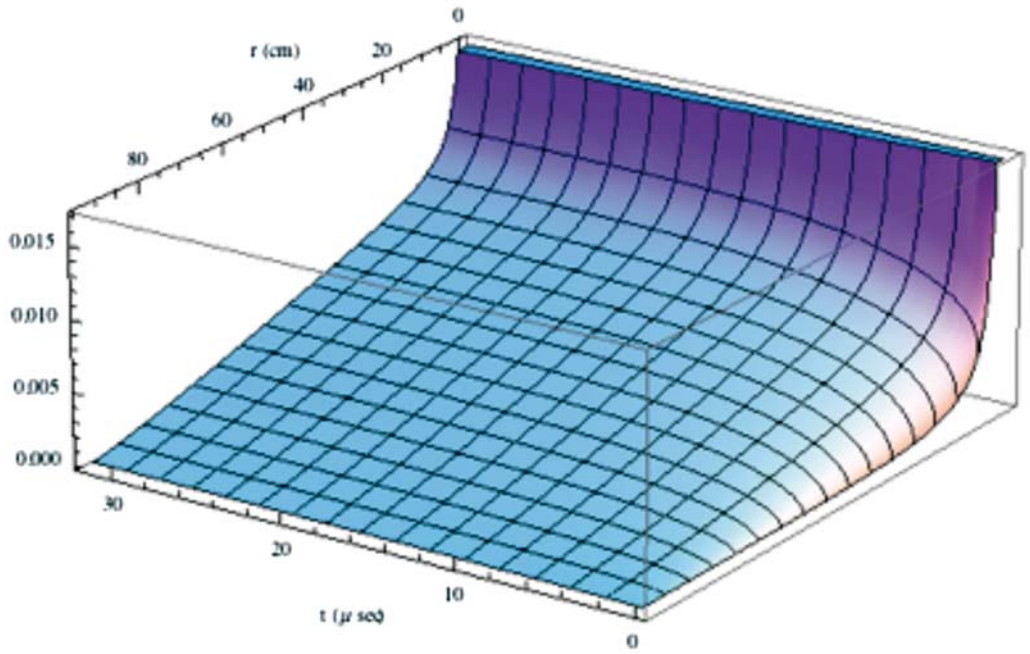

Fig. 1. Time-dependent total neutron flux obtained with a 20 group calculation for a monochromatic isotropic central neutron source of $E = 20\,\mathrm{MeV}$: at $t = 30\,\mu s$ the flux reaches the static configuration. The flux is proportional to the source intensity, here assumed to be unit.

A drawback affecting this approach consists in the approximation of equal extrapolation radii for each energy group. Such an assumption simplifies the calculations considerably, but, at the same time, prevents a proper description of the effects due to the presence of the large reflector on the system: neutrons coming back to the core from the reflector are slower and then they should give rise to somehow longer prompt neutron transients. In virtue of the previous observation this fact should not bring dramatic consequences, but a more refined calculation scheme, presently under development, is highly recommended to quantitatively assess the impact of the reflector on the system neutron economy and transient response. In fig. 1 a typical time-dependent flux is depicted when a point-like 20 MeV neutron source is switched on in the centre of a homogeneous sphere: from the figure it is clear that the flux saturates the static configuration at time $t \sim 30\,\mu s$. As it can be seen from this typical behaviour the possible occurrence of complex time eigenvalues in a fast system [2] is hardly detectable, both because of the small amplitude of the flux oscillations induced and because of the very short duration of the transients. In view of these considerations in the following neutron transients are assumed practically instantaneous, if not explicitly indicated otherwise.

2 Thermal and mechanical aspects

The most peculiar aspect of this system compared with conventional reactors lies in the existence of a solid lead matrix, whose thermal and mechanical behaviour has been analysed by using detailed FEM (finite element) models. Two different FEM models have been developed to perform accurate steady state numerical calculations by means of the code ANSYS 13.

The first one consists of a thermal 3D brick model, which has been employed to calculate the temperature distribution inside the basic fuel assembly, which is composed of a solid lead block, containing a square array of 81 fuel rods, disposed in a 9×9 matrix and each surrounded by a stainless steel AISI 304L cylindrical cladding. An array of 8×8 cooling channels is also bored in the lead matrix; each channel is surrounded by an aluminium alloy cylindrical cladding; finally, the whole fuel assembly is contained within an external square box, made of the same stainless steel as the one employed for fuel rod cladding.

Only three different materials are considered in the FEM model (*i.e.*, stainless steel, aluminium alloy and solid lead) since the enriched uranium fuel rods have not been included yet. Temperature-dependent thermal and mechanical properties are defined for all of the three materials included in the model.

Two fundamental hypotheses are assumed, aiming to simplify thermal FEM calculations:

1) no void is present between the lead and the other two contacting materials;
2) a double symmetry is supposed to exist with respect to two mutually orthogonal planes, both containing the longitudinal central axis of the fuel assembly; this assumption leads to a quarter-model thermal simulation.

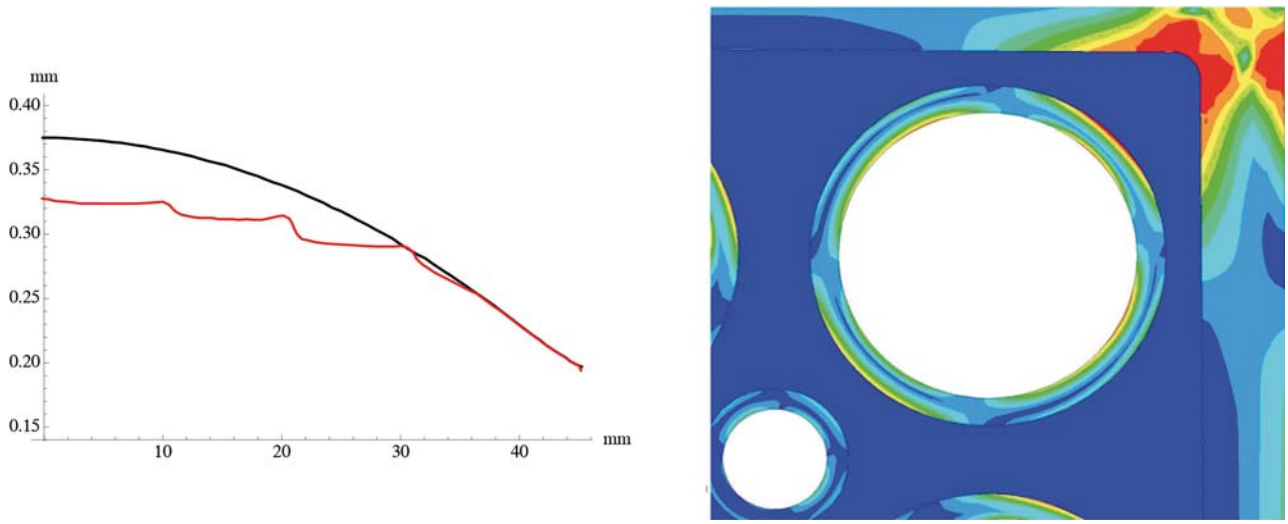

Fig. 2. Left: Transverse displacements for steel (black line) and for lead (red line) for a typical fuel pin as functions of the distance from the box centreline. Right: Von Mises equivalent stress for the fuel pin near the corner of the steel container. In the area in red the stress is 128 Mpa, in the area in blue the stress is .128 MPa.

Thanks to the first assumption, only thermal conduction governs heat exchange at lead-to-steel and at lead-to-aluminium interfaces. Therefore, if a proper heat flux distribution is defined all over the steel cylindrical cladding inner surfaces, a full 3D temperature distribution may be easily calculated inside the basic fuel assembly volume.

As a second step, among all of the nodal planes normal to the longitudinal central axis, the most critical planar temperature distribution is employed as an input datum necessary to calculate thermal-induced displacements and stresses, by using a structural 2D plane stress quarter model.

This structural 2D plane stress model is characterized by a FEM mesh much finer than that of thermal 3D brick element mesh, and permits to evaluate the void size at interfaces between different materials, since several surface-to-surface contact elements have been included in the model. In addition an elasto-plastic kinematics hardening behavior is assumed for steel and aluminium materials, while solid lead is supposed to exhibit elastic-perfectly plastic behavior. A total of 1115200 brick elements have been employed.

All contact elements have zero gaps at room temperature (no void initial conditions); afterwards the temperature distribution is supposed to rise gradually to a maximum, and then lower down again. No actual temperature distribution is available at the moment; so, a uniform temperature distribution has been assumed, rising from 25 °C to 275 °C. The main results that have been obtained at maximum temperature conditions may be summarized as follows:

1) A maximum gap of 49748 μm occurs between solid lead and the external stainless-steel square box, at the box centerline (*i.e.*, on the symmetry planes) as shown in fig. 2(a): displacements for steel (black line) and for solid lead (red line) are plotted as a function of the distance from the box centerline.
2) At the steel square box centerline the transversal displacement is maximum (377 μm).
3) Near the centerline of the fuel rod assembly, the gap between cylindrical cladding and solid lead is maximum (28 μm).
4) The Von Mises equivalent stress is maximum (128 MPa) at the fillet placed in the corner zone of the steel external square box (fig. 2(b): in red areas the stress is 128 MPa, in blue areas is 0.128 MPa).

More realistic results will be easily obtained as soon as a proper heat flux distribution is available all over the steel cylindrical cladding inner surfaces, allowing the accurate evaluation of an actual temperature distribution.

3 Thermal-hydraulics

Preliminary thermal-hydraulic calculations have been carried out aimed at providing the actual temperature field in the fuel assembly (FA), which has a fundamental impact on both neutronic and thermal-mechanics calculations due to temperature feedback effects.

An overall core calculation has been first performed in order to determine the representative channel coolant flow conditions by postulating that all core channels are characterized by the same mass flow rate.

An enthalpy balance has been taken by imposing the total nominal thermal power (*i.e.*, 190 kW) and the corresponding temperature difference between inlet and outlet (*i.e.*, 35 °C), with He specific heat capacity $c_p = 5195\,\mathrm{J\,kg^{-1}\,K^{-1}}$.

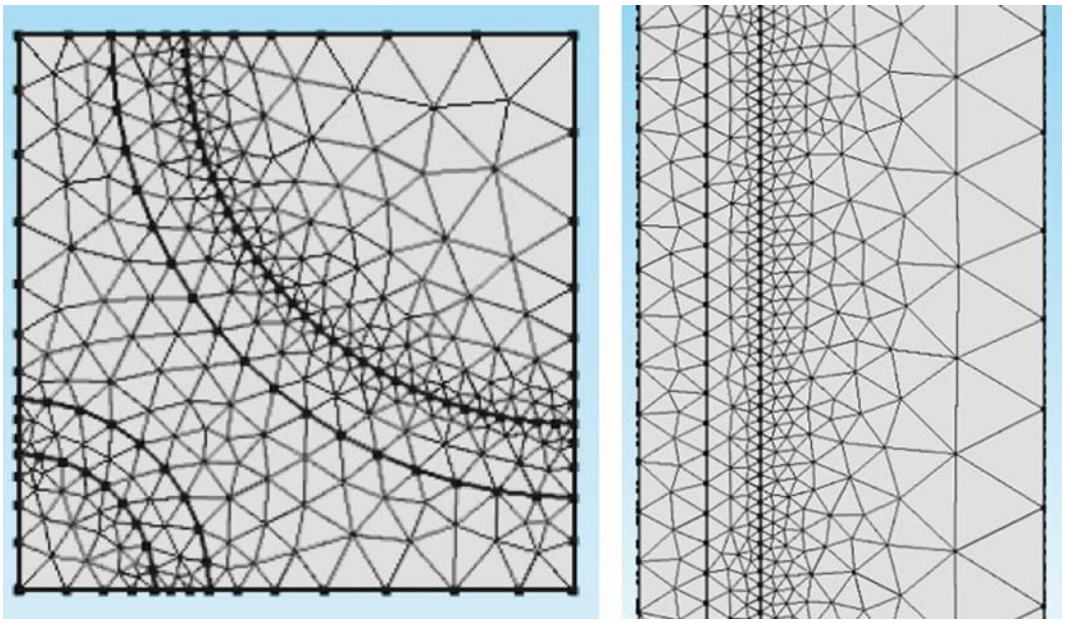

Fig. 3. Computational mesh (x-y plane view, left; axial slice view, right).

Once the total core mass flow rate has been determined, $\Gamma = 1.11\,\mathrm{kg\,s^{-1}}$, the actual single channel mass flow rate was determined by dividing the latter by the total number of channels (64 channels per FA $\times$ 60 FAs), resulting in $\Gamma_{\mathrm{ch}} = 0.00029\,\mathrm{kg\,s^{-1}}$, corresponding to a He average speed of $37.7\,\mathrm{kg\,s^{-1}}$. Anyway, since each FA contains 84 fuel pins, the actual channel mass flow rate has been reduced by the ratio 64/81, so as to obtain the effective mass flow rate pertaining to each fuel pin $\Gamma_{\mathrm{eff}} = 0.000229\,\mathrm{kg\,s^{-1}}$, corresponding to a He average speed of $29.9\,\mathrm{kg\,s^{-1}}$.

Materials thermo-physical properties have been calculated in correspondence with the average nominal core temperatures and kept constant, once their low variability in the range of interest has been verified. In particular, $7\,\mathrm{W\,m^{-1}\,K^{-1}}$, $16\,\mathrm{W\,m^{-1}\,K^{-1}}$, $35\,\mathrm{W\,m^{-1}\,K^{-1}}$, and $237\,\mathrm{W\,m^{-1}\,K^{-1}}$ have been assumed, respectively, for UO_2, stainless steel, solid lead, and aluminum thermal conductivities.

As far as the He convective heat transfer coefficient is concerned, it has been determined by employing the well-known Dittus-Boelter correlation, resulting in $h = 1970\,\mathrm{W\,m^{-2}\,K^{-1}}$.

Afterwards, a quarter of a representative fuel rod with associated helium coolant channel has been modeled in a three-dimensional geometry by imposing symmetry (*i.e.*, no heat flux across the boundary) conditions for the temperature field at the lateral surface boundaries, and thermal insulation conditions (*i.e.*, no temperature gradient across the boundary) at the lower and upper boundaries of conductive (*i.e.*, solids) elements. As far as the helium coolant is concerned, a normal inflow ($v_z = 30\,\mathrm{m\,s^{-1}}$) condition has been set at the channel inlet, whereas an outflow condition with given pressure ($P = 15$ bar) has been selected for application at the channel outlet[1].

An extra fine mesh option has been necessarily required for an appropriate and accurate resolution of the temperature distribution, as depicted in fig. 3. In particular, the degree of refinement has been set, on the basis of a sensitivity analysis on the elements size, as the threshold beyond which numerical results are independent of the mesh, thus making any further refinement an unnecessary computational cost.

A finite-element model has been then developed based on the coupling of equations describing the heat transfer process in solids and in fluids. As far as the former are concerned, heat transfer by conduction has been simply described; regarding the latter, both conduction and advection have been taken into account to properly treat the heat transfer process in forced convection. More specifically, the flow field has been calculated opting for a segregated solver option, and the heat transfer problem has been addressed afterwards based on the flow distribution solution previously accomplished[2].

[1] This condition provides a suitable boundary condition for convection-dominated heat transfer at outlet boundaries in a model with convective heat transfer, this condition states that the only heat transfer over a boundary is by convection. The temperature gradient in the normal direction is zero, and there is no radiation. This is usually a good approximation of the conditions at an outlet boundary in a heat transfer model with fluid flow.

[2] It has been possible to adopt such a calculation scheme to save some computational effort, since in the specific case under examination, the heat transfer process is not tightly coupled with the fluid flow problem.

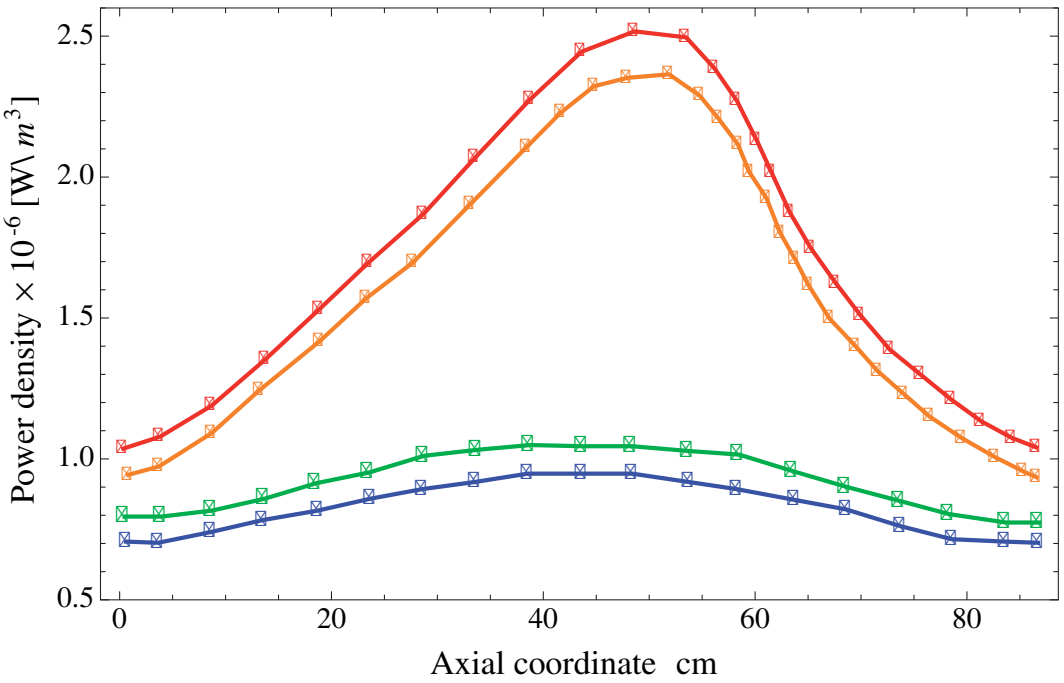

Fig. 4. Power density axial profiles calculated by means of MC simulations and imposed as a heat source for the thermal model.

A heat source has been imposed to describe the heat generation within the fuel domain by specifying the heat per volume (power density) at each computational node, coherently with the power profiles obtained as outputs from MC calculations. In particular, four cases have been considered (fig. 4):

1) peripheral fuel rod, neutronic properties evaluated at 600 K;
2) central fuel rod, neutronic properties evaluated at 600 K;
3) peripheral fuel rod, neutronic properties evaluated at 300 K;
4) central fuel rod, neutronic properties evaluated at 300 K.

Initial homogeneous conditions have been imposed for both solids and fluid temperatures, consisting in a uniform value of 453.15 K (He coolant nominal inlet temperature), and the static temperature field has been eventually calculated. Hereafter the main results are collected and discussed.

Case 1: Peripheral fuel rod, neutronic properties evaluated at 600 K.

In the case of a representative fuel rod at the core periphery, the total thermal power produced results of the order of 29 W. By assuming a constant uniform coolant speed of 30 m s^{-1}, the enthalpy balance brings to an average helium outlet temperature of 477 K.

The temperature difference between the fuel centreline and the coolant bulk results to be less than 1 K, and therefore it may be concluded that, for a subassembly, the assumption of a uniform temperature distribution in the x and y directions is acceptable for both neutronic and thermal-mechanics calculations. Conversely, the temperature field is more variable as a function of the axial coordinate, as the ΔT between inlet and outlet is of the order of 25 K.

In fig. 5 the overall 3D temperature distribution is depicted: the respective x-y plane profiles are represented in correspondance with ten different axial quotes.

The axial temperature profile is characterized by a linear increase from 180 °C to 205 °C with few degrees of variation between the different elements

Case 2: Central fuel rod, neutronic properties evaluated at 600 K.

In the case of a representative fuel rod in the vicinity of the neutron source, the total thermal power produced results considerably higher, being of the order of 55 W. By assuming again a constant uniform coolant speed of 30 m s^{-1}, the enthalpy balance brings to an average helium outlet temperature of approximately 500 K.

The temperature difference between the fuel centreline and the coolant bulk results again to be around 1 K, whereas the ΔT between inlet and outlet is approximately 60% higher than in the previous case, coherently with the higher integrated power, resulting equal to 38 K.

In fig. 6 the overall 3D temperature distribution is depicted: the respective x-z plane profiles are represented in correspondence with three different y coordinates.

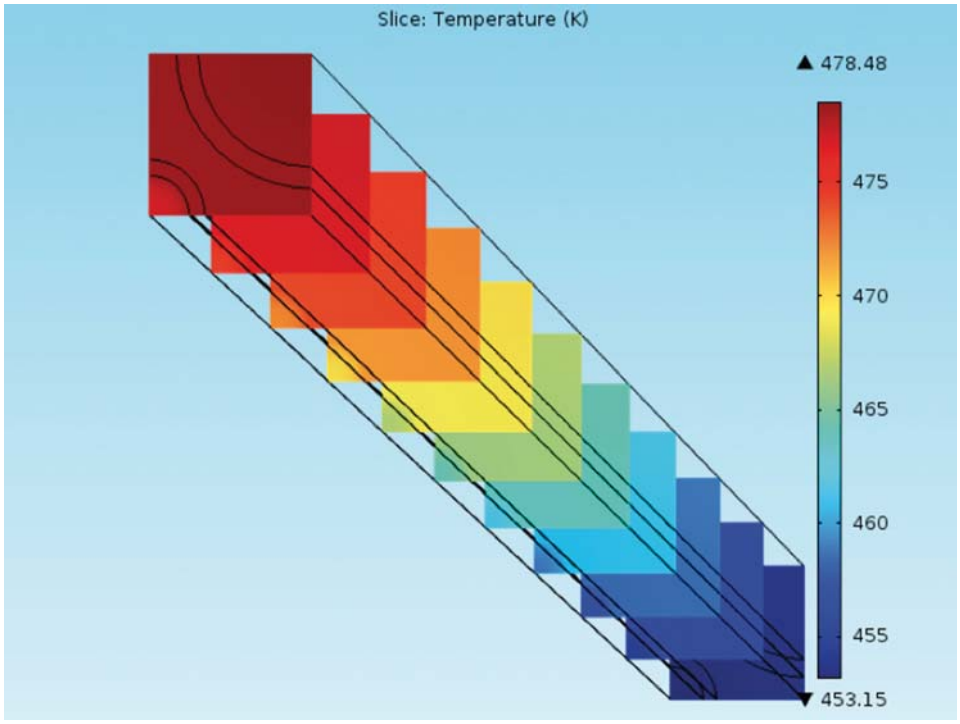

Fig. 5. Subassembly 2D (x-y) temperature field at different axial quotes (external rod, neutronics properties evaluated at 600 K).

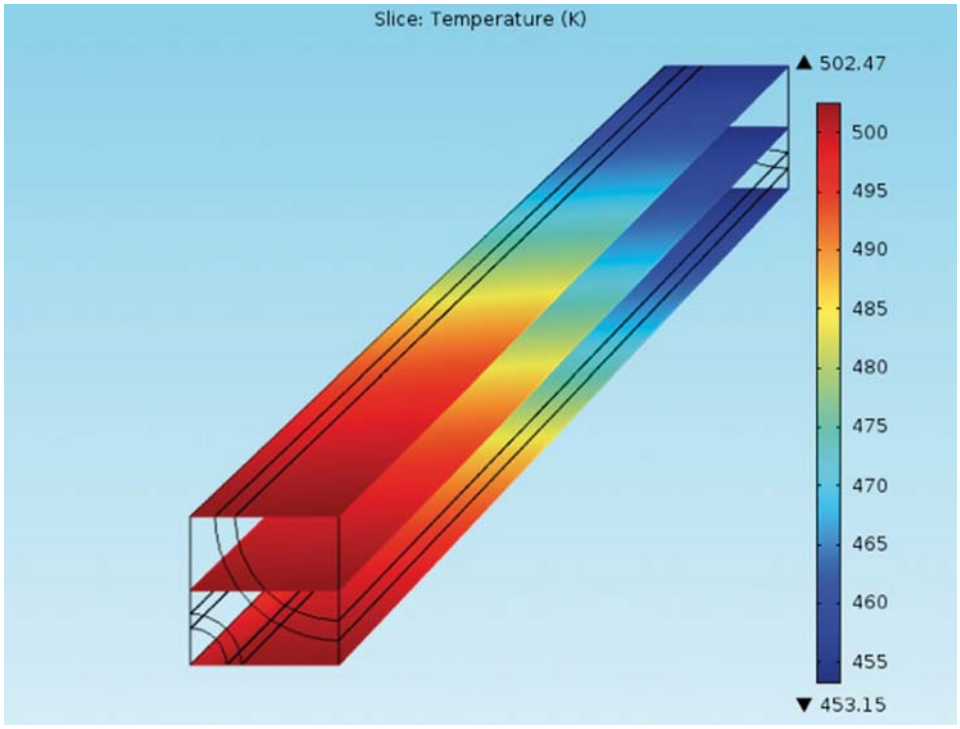

Fig. 6. Subassembly 2D (x-y) temperature field at different axial quotes (internal rod, neutronic properties evaluated at 600 K).

As a general result of the presented preliminary thermal-hydraulics evaluations, it may be concluded that the temperature distribution in the subassembly —and therefore in the whole core— is strongly dependent on the coolant bulk temperature, being essentially determined by the latter: indeed, being the core linear power extremely low, very slight temperature differences occur between the coolant and the fuel centreline, despite the UO_2 low thermal conductivity (approximately $7\,W\,m^{-1}\,K^{-1}$). As a consequence, a uniform temperature distribution (equal to the coolant temperature value) might be assumed in correspondence with each x-y plane normal to the longitudinal (z) axis. In

this way, the temperature difference between the core inlet and outlet sections would allow to use the coolant mass flow rate as a possible means to steer the temperature. distribution within the reactor, and therefore to impose *a priori* well-determined perturbations on both fuel, lead and structure, so as to measure the effects of such variations on reactivity.

We do not present here the results for cases 3) and 4) because they are not significative, being the fuel temperature around 600 °K.

4 Dynamics

The study of the dynamics of the proposed ADS represents a key issue for the development of the entire system, as it will lead to a preliminary assessment of both the feasibility and the actual point in building such an experimental facility. In particular, in this early phase of the reactor conceptual design, it is fundamental to determine the real possibility to measure reactivity feedbacks, and therefore the first purpose of developing a coupled neutronics, thermal-hydraulics and thermal-mechanics model consists in providing both a qualitative and quantitative estimation of temperature effects.

As far as feasibility is concerned, the need of developing a dedicated verifiable computational tool, able to provide a high level of knowledge about the plant dynamic behavior following any externally induced perturbation, has been recognized as a top priority. This capability would enable analysts to compare operational and safety characteristics of design alternatives, and to evaluate relative performance advantages with a consistent, quantitative measure, resulting in a fundamental feedback for the designer. Accordingly, a very flexible, straightforward and fast-running (*i.e.*, without significant computational burden and implementation-related effort) dynamics simulator is to be sought expressly meant for this phase of the ADS conceptual design, in which all the system specifications are still considered open design parameters and thus may be subject to frequent modifications. Such a tool must be specifically conceived for i) evaluating the robustness and stability of the dynamic system itself on its entire power range thanks to the possibility of linearizing the constitutive equations around different working conditions, and for ii) predicting the reactor response to typical transient initiators.

In fact, even if the inherent safety characteristics of and ADS are commonly believed to be guaranteed by the sub-criticality level of the system and by the ability to shut-off the external neutron source (*i.e.*, the proton beam) on demand instantaneously, the impact of important safety parameters such as reactivity feedback coefficients and kinetic parameters on the peculiar system transient behaviour must be evaluated, in order to guarantee that the system operates in stable and safe conditions in any situation beyond nominal. Moreover, depending on the fuel composition change with burn-up, the system might exhibit a continuously altering behavior in its transient response at different stages of operation, which constitutes an additional aspect that cannot be left aside when investigating the plant dynamic behavior.

In order to firstly examine the intrinsic kinetic and dynamic characteristics of the system, and secondly to assess the nature and impact of the variation of safety parameters during operation, a dedicated dynamic simulation model needs to be developed including the external neutron source due to the accelerator supplied proton beam, and also the most important temperature-dependent reactivity feedback effects by incorporating a dynamic, full-scope thermal-hydraulic and thermal-mechanic model for the transient calculation of the fuel, lead matrix, cladding and coolant temperatures and consequent expansions. Such a coupled model is to be understood as a simple tool allowing to perform a systematic analysis of the system transient behaviour by assessing the impact of different phenomena, such as sub-criticality level, external neutron source strength (proton beam), besides the occurrence of typical operational plant transient initiators (*e.g.*, variation of coolant mass flow rate, loss of heat sink, either protected and unprotected, accidental reactivity insertion, etc.).

In the early phase of the ADS system definition, such a tool could help in narrowing down the open parameter field by allowing an early elimination of those design variations for which the transient behavior exhibit an obvious, unsatisfactory plant transient response, providing an essential feedback to the designer.

In this perspective, based on the study of the system transient response (ensuing from the value of reactivity coefficients and kinetic parameters) and on the expected measurements, a series of feasible experiments could be planned depending on the most suitable control parameter for each specific evaluation (*e.g.*, modulation of either the coolant mass flow rate or the neutron source to perturb the nominal steady state by varying the temperature field, the system power level, etc.). In this sense the study of the system dynamics will provide important insights concerning the actual usefulness and need of such a machine in the current LFR technology R&D scenario, resulting in a fundamental step towards its possible realization.

In order to achieve the above-mentioned goals, the simulation tool must include a neutronic block, connected to a thermal-hydraulics and a thermal-mechanics block by reciprocal feedbacks, as schematically described in fig. 7, so as to properly account for all the main phenomena affecting the system reactivity and, consequently, power level.

In fact, it is expected that core dimensional changes (axial and radial expansions, and void formation) following a temperature variation could very likely contribute to any reactivity change significantly (fig. 8). On the other hand, it is desired that strong feedbacks on neutronics ensue from any user-controlled temperature variation following a coolant mass flow rate or a source term well-determined deviation from the respective nominal figures.

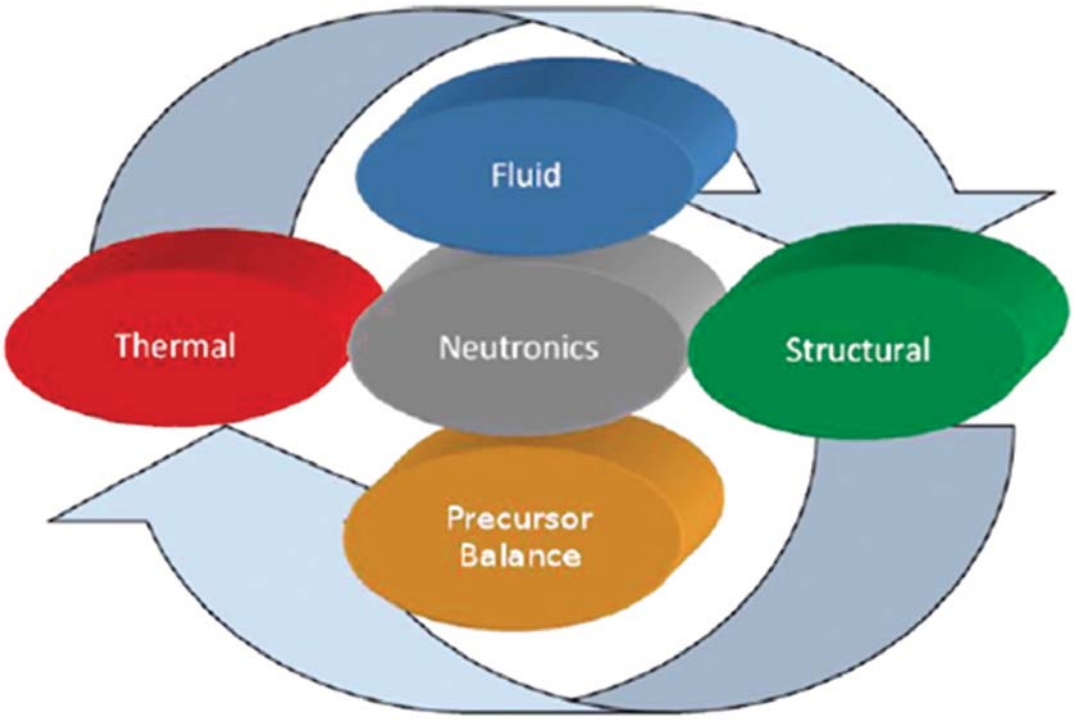

Fig. 7. Multiphysics approach schematic representation.

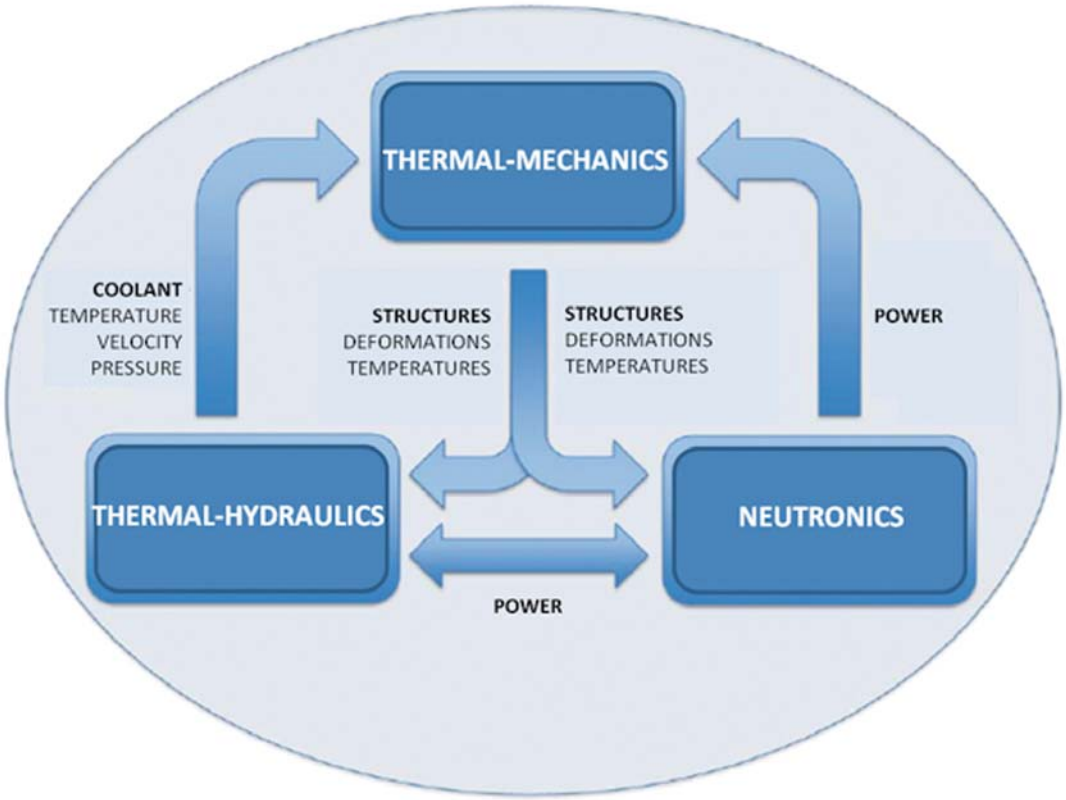

Fig. 8. Mutual interdependences between neutronics and thermal-hydraulics and thermal-mechanics.

According to the project present status of development and to the objectives and open issues well discussed above, the following future steps are considered to be required, very schematically:

1) Accomplishment of a complete static neutronics characterization, in particular evaluation of fuel composition change during the core lifetime (burn-up calculations) and calculation of reactivity coefficients and kinetic parameters (*i.e.*, Doppler, radial and axial expansions, void, materials density changes, etc., effects) in correspondence with different time situations (*e.g.*, Beginning of Life, Middle of Life, End of Life core configurations) and different power levels.
2) Accomplishment of a complete static thermal-hydraulics characterization from the zero-power condition to nominal power.
3) Accomplishment of a complete static thermal-mechanics characterization from the zero-power condition to nominal power.
4) Development of a coupled model incorporating neutron kinetics, thermal-hydraulics and thermal-mechanics.
5) Stability analysis as a function of both power level and neutronics parameters.
6) Transient scenarios simulation.
7) Quantitative estimation of reactivity effects and their measurability.
8) Preliminary proposal and assessment of feasible experimental tests.

References

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