Tailoring the Response of Autonomous Reactivity Control (ARC) systems

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ABSTRACT

The Autonomous Reactivity Control (ARC) system was developed to ensure inherent safety of Generation-IV reactors while having a minimal impact on reactor performance and economic viability. In this study we present the transient response of fast reactor cores to postulated accident scenarios with and without ARC systems installed. Using a combination of analytical methods and numerical simulation, the principles of ARC system design that assure stability and avoids oscillatory behavior have been identified. A comprehensive transient analysis study for ARC-equipped cores, including a series of Unprotected Loss of Flow (ULOF) and Unprotected Loss of Heat Sink (ULOHS) simulations, were performed for Argonne National Laboratory (ANL) Advanced Burner Reactor (ABR) designs. With carefully designed ARC-systems installed in the fuel assemblies, the cores exhibit a smooth non-oscillatory transition to stabilization at acceptable temperatures following all postulated transients. To avoid oscillations in power and temperature, the reactivity introduced per degree of temperature change in the ARC system needs to be kept below a certain threshold the value of which is system dependent, the temperature span of actuation needs to be as large as possible.

Keywords: Safety, Inherent safety, ARC, Autonomous Reactivity Control, ATWS, Unprotected transients

1. Introduction

The Autonomous Reactivity Control (ARC) system was developed to ensure inherent safety performance of Generation-IV reactors while having a minimal impact on reactor performance and economic viability. The motivation and inspiration for the development of ARC systems have been covered in earlier publications [1] [2] [3] [4], and the principles of the design, operation and manufacturing of ARC systems has been presented in great detail in ref. [5]. We refer the reader interested in detailed information about the design and operational principles of the system to these previous publications.

In its standard configuration, the ARC system is installed as a modification to a conventional nuclear fuel assembly design. The “system” consists of two reservoirs, located at the top and bottom of the assembly, and two concentric tubes that link the reservoirs. The inner tube is open at both ends and connects the insides of both reservoirs, while the outer tube is open at the bottom (connected to the lower reservoir) and at the top connects to a closed gas-filled reservoir. During operation, the upper reservoir is completely filled with a liquid (Potassium), while the lower reservoir contains the same expansion liquid and, floating on top of it, a separate immiscible liquid (Potassium and Lithium). The remaining free volume between the two concentric tubes in the closed system is filled with an inert gas (Argon). The outer ARC-tube has the same outer dimension as the fuel rods. Installing an ARC-tube therefore implies replacing one of the fuel rods in the assembly. A schematic view of the operation of an ARC-installation is shown in Figure 1. During an accident/transient scenario in the reactor, the ARC-system responds in the following way, starting from standard operating conditions:

1. Some event raises the temperature in the core, which heats up the coolant.
2. The heated coolant flows to the top of the assembly and transfers heat to the expansion liquid inside the upper reservoir.
3. The expansion liquid in the upper reservoir thermally expands. Since the reservoir is completely filled and sealed at the top, this expansion is directed down the inner ARC-tube that connects the two reservoirs.
4. As expansion liquid enters the lower reservoir from the upper reservoir (through the inner ARC-tube), the level of absorber liquid rises toward (and finally in to) the active core, while compressing the inert gas above.
5. The absorber liquid, which has a high neutron capture cross-section, introduces negative reactivity by absorbing neutrons in the core, which in turn causes a reduction in power and temperature.

6. As the core cools down, the temperature of the expansion liquid starts to fall. Thermal contraction combined with the pressure of the inert gas again lowers the axial level of the absorber liquid until the system reaches a stable critical configuration.

Figure 1, Schematic view of the ARC system at different states/temperatures

The focus of this study is the transient behavior of ARC-equipped fast reactor cores, and their stability during and following accident scenarios. Using a combination of analytical methods and numerical simulation, the principles that avoid oscillatory behavior and assure stability of fast reactor cores with ARC systems installed have been identified and defined.

The design of any ARC-system starts with an informed guess on a number of parameters, which are then iteratively improved after obtaining simulation results. The parameters of the ARC-system depend primarily on the relative importance of different accident scenarios in the specific core in which it is to be implemented. The core state assumed following three general types of Anticipated Transients Without Scram* (ATWS) are summarized in *Table 1*.

* Unprotected here means that active shutdown (or Scram) systems do not function

Table 1, Definitions of core states following main ATWS events

Loss of flow (ULOF)	Loss of heat sink (ULOHS)	Transient overpower (UTOP)
<i>Primary system pumps are tripped and the flow rate transitions to an asymptotic state of lower flow. If all pumps fail, the flow rate transitions to that driven by natural circulation ($F=F_n$). The coolant inlet temperature is assumed to remain constant (i.e., $\delta T_{in} = 0$) and there is no external reactivity introduced ($\rho_{ext} = 0$)</i>	<i>The main path for heat removal from the primary cycle is lost. Power is reduced to the level matching passive heat removal systems ($P=P_d$), external reactivity is zero ($\rho_{ext} = 0$) and the primary coolant flow rate remains unchanged ($F=1.0$).</i>	<i>A positive external reactivity ($\rho_{ext} > 0$) is introduced, the coolant inlet temperature remains unchanged ($\delta T_{in}=0$) and the primary coolant flow rate remains unchanged ($F=1.0$).</i>

F = Normalized primary coolant flow rate,

P = Normalized power,

δT_{in} = Change in the coolant inlet temperature

ρ_{ext} = External reactivity

Other events that may occur can be defined as combinations of the events above. For instance, the loss of all external power (station blackout) event is effectively the LOF and LOHS events happening simulatenously. For these three general types of transients, there are essentially three different time-scales of importance:

1. Early phase

This phase typically spans the first few minutes of the transient. It is typically in this phase that the highest temperatures (and possible coolant boiling and/or fuel melting) will occur in the ULOF and UTOP scenarios. It is primarily to limit temperatures during this phase of the transients that the ARC-system was initially developed. In an otherwise well-designed core, the absorber liquid of

the ARC-system only enters the active core region during this phase. The analysis of the initial phase of a transient is not amenable to any known analytical approach, and must be simulated using transient analysis codes developed for this purpose.

2. Intermediate phase

The intermediate phase simply defines the range of time after the early phase ends and peak initial temperatures have been reached until the system has transitioned to a quasi-static state.

3. Quasi-static state

The definition of the quasi-static state, which is the point where continued transient simulation is no longer of interest, differs by the type of system and transient scenario. Depending on the development of the transient, the quasi-steady state can be defined a situation where the time derivative of fission power or the time derivative of reactivity is equal to zero. The quasi-static state is amenable to analytical analysis using a methodology developed in the early 1980's by Sevy, Cahalan and Su and summarized in [6] and [7].

The designer of an ARC system has the general goal of simultaneously creating a system that keeps temperatures below limits during and following applicable transients while also avoiding any potential oscillations or stability issues. If the derivative of ARC-system reactivity with respect to temperature is zero in the quasi-static state, there is no initiating force to drive oscillations in temperature, power and reactivity in the long term. Introducing this as a design requirement leads to the following three possible design objectives in the quasi-static state following any transient:

- The ARC system(s) are fully retracted (absorber below the extrapolated core)
- The ARC system(s) are fully engaged (absorber covers the extrapolated core)
- Dual (or more) systems are installed that operate differently but are all either fully engaged or retracted

The above criteria cannot be met by an ARC system design for all postulated transients. For example, if the ARC absorber liquid is completely inserted following a complete loss of flow event (where all forced coolant circulation is lost), the absorber liquid will only cover a fraction of the core following a partial ULOF event (such as a loss of 50% of forced flow). We study this issue in detail in Section 8.1.

Further complicating the issue, an ARC system designed specifically with the aim of handling one type of transient will have completely different actuation characteristics during and following other types of transients. Designing the system to ensure stability and acceptable temperatures for every postulated transient scenario therefore requires an iterative design and analysis approach. In general, stability is promoted by reducing both the total ARC system reactivity worth and specifically the worth change rate (as measured in reactivity change per unit of temperature difference). This general fact can be easily realized by studying the extremes. If the reactivity vested in the ARC system is very low, the core will respond almost as if it was not there at all (i.e. no additional stability issues), while a very high worth ARC system would rapidly cause very large differences in reactivity and power with subsequent (time-delayed) oscillations in temperatures.

The potential for dynamic oscillations is caused by the coupling between neutronics and thermal hydraulics. At least two oscillation modes were identified: A higher frequency mode associated with the reactor core itself and a lower frequency mode involving both the core and the primary heat-transport loop [8] [9]. In the reactor-only mode, oscillations in the reactor power induce slightly delayed oscillations in the temperature of the coolant at the reactor outlet and the temperature of the expansion liquid in the upper ARC reservoir, which closes the feedback loop by affecting the core power. With a longer delay, oscillations are propagated around the primary coolant circuit back to the reactor inlet, where they introduce reactivity changes by expanding the liquids in the lower ARC reservoir (and also introducing feedback by radial expansion of the core from heating of the grid plate).

In Section 2, we define the Quasi-static Reactivity Balance method as it applies to analysis of ARC-equipped cores. Further discussion on the stability of ARC systems during and following ULOF transients are given in Section 3, while the ULOHS and UTOP events are covered in Section 4 and 5 respectively. In Section 6 we outline the methods to determine the details of partial actuation of an ARC system following less severe transients than the limiting one that it was designed to handle. In Section 7 we apply the methods developed in the previous sections to selected Advanced Burner Reactor (ABR) core designs, and in Section 8, the results of transient analysis results for one of the ABR cores (with and without ARC systems) is presented. Finally, in

Section 9 we summarize the conclusions of this study and outline the focus of future work.

2. Quasi-static reactivity balance analysis

If ARC systems are to provide reactivity compensation in the long term following an unprotected transient, the required reactivity worth of such systems can be calculated using quasi-static reactivity balance based methods. The general reactivity balance for the core can be defined as [6] [7]:

$$\Delta p = (P - 1)A + \left(\frac{P}{F} - 1\right)B + C\delta T_{in} + \rho_{ext} \quad (1)$$

where A , B and C are measurable integral reactivity parameters, Δp is reactivity, P is the normalized power ($P = 1.0$ is full operational power), F is the normalized coolant flow rate ($F = 1.0$ is forced-flow at full pumping power), δT_{in} is the change in the coolant inlet temperature and ρ_{ext} is reactivity introduced by, for example, the motion of control rods. This method is exact for transitions between steady states, and approximately valid in transients slow enough to preclude non-equilibrium stored energy in the fuel pins.

The integral reactivity parameter A is directly dependent on the power level of the core and only includes reactivity parameters that are directly linked to the fuel temperature. Correctly defining A is a complicated issue which depends on both the type of fuel and the level of burnup. For metal fuel these complications are discussed in ref. [10]. In this study we define the parameter A according to convention as [6]:

$$A = (\alpha_D + \alpha_f)\Delta T_{fc} \quad (2)$$

where α_D is the fuel Doppler coefficient, α_f is the fuel axial expansion reactivity coefficient and ΔT_{fc} is the difference between the average fuel and coolant temperatures. The parameter A is thus the reactivity vested in the fuel above the average temperature of the coolant.

The parameter B includes coefficients that are directly impacted by changes in the coolant temperature rise through the core (and thus the power/flow ratio), and is defined as:

$$B = (\alpha_D + \alpha_f + \alpha_s + \alpha_{co} + X_r \alpha_r + 2\alpha_{rd}) \frac{\Delta T_{co}}{2} \quad (3)$$

Where α_s is the structure expansion reactivity coefficient (duct and cladding), α_{co} is the coolant temperature (density) reactivity coefficient, X_r is a geometry-factor related to where duct load pads are located axially, α_r is the reactivity coefficient associated with radial expansion of the core, α_{rd} is the control-rod driveline expansion reactivity coefficient and ΔT_{co} is the nominal coolant temperature rise:

$$\Delta T_{co} = T_{cin} - T_{cout} \quad (4)$$

where T_{cin} and T_{cout} are the inlet and outlet coolant temperature at standard operating conditions respectively. The coolant inlet temperature coefficient of reactivity C is defined as:

$$C = \alpha_D + \alpha_f + \alpha_s + \alpha_{co} + \alpha_r + \alpha_{rd} [K^{-1}] \quad (5)$$

To be used in the equations defined above, individual reactivity coefficients are typically linearized within some appropriate temperature interval(s). Due to the highly non-linear nature of the ARC-system response (which essentially follows the axial reactivity worth profile in the core), a “linearized” ARC-term cannot be added as another term in the integral parameters of eq. (1). Instead we treat the reactivity from the ARC system as an independent integral parameter called U_{ARC} . Eq. (1) can then be modified to include an ARC system as:

$$\Delta \rho = (P - 1)A + \left(\frac{P}{F} - 1\right)B + C\delta T_{in} + x_{transient} * U_{ARC} + \rho_{ext} \quad (6)$$

where $x_{transient}$ is a multiplier defining the fraction of total possible actuation that the ARC system experiences in the quasi-static state following a specific transient scenario. In whatever transient that is the limiting (most challenging or serious) event for a specific core, $x_{transient}$ will by design be exactly equal to unity. In other, non-limiting transients for the same core, $x_{transient}$ is somewhere in the range $0 \leq x_{transient} < 1.0$.

Using this definition of reactivity balance, it is possible to define the quasi-static state of the core following any transient scenario. The power of the core will adjust up or down to compensate any reactivity change caused by a change in the flow rate (F), inlet temperature (δT_{in}) or by the introduction of reactivity (ρ_{ext} , $x_{transient} * U_{ARC}$). Solving eq. (6) for the normalized power level in the quasi-static state of ($\rho = 0$) yields:

$$P = 1 + \frac{B \times \left[\frac{F-1}{F} \right] - C\delta T_{in} - x_{transient} * U_{ARC} - \rho_{ext}}{A + \frac{B}{F}} \quad (7)$$

3. The unprotected loss of flow (ULOF) event

The transient events with the largest potential to cause oscillatory or unstable behavior are those related to the coolant flow rate, since the flow rate directly impacts the time-lag of the ARC system response. If the temperatures in the ARC reservoirs and the core are out of phase, the ARC response could worsen the accident and send the core in to a serious and diverging oscillatory behavior. The power-to-flow ratio following the ULOF event can be expressed from eq. (7) as:

$$\frac{P_{ulof}}{F_n} = \frac{A + B - x_{ULOF} * U_{ARC}}{AF_n + B} \quad (8)$$

where F_n in the case of a complete loss of forced flow is the normalized natural-circulation flow rate, given in simplified analysis as [7]:

$$F_n = \left(\frac{gh\rho_c\beta_c\Delta T_{c0}P_{ulof}}{K_p} \right)^{4/11} \quad (9)$$

where,

g = gravitational constant

h = vertical separation of core and heat sink center

ρ_c = average coolant density

β_c = coolant volumetric expansion coefficient

K_p = pressure drop at full power

The change in coolant outlet temperature is a direct function of the power-to-flow ratio (P/F) and for the ULOF case this is defined as:

$$\begin{aligned} \delta T_{out} &= T_{ulof} - T_{cout} = \left(\frac{P_{ulof}}{F_n} - 1 \right) \Delta T_c \\ &= \left(\frac{A + B - x_{ULOF} * U_{ARC}}{AF_n + B} - 1 \right) \Delta T_c \end{aligned} \quad (10)$$

The mixed mean coolant outlet temperature in the quasi-static state following any transient must remain below a predefined level, typically given by the cladding material long-term temperature limit. If the ULOF event is the limiting transient of the given system ($x_{ULOF}=1.0$) and we define a maximum allowable *long-term* coolant outlet temperature of T_{LC} , it is possible to define the total required worth of the ARC actuation (U_{ARC}) as:

$$U_{ARC} = U_{ULOF} = A + B + (AF_n + B) \left[\frac{T_{cout} - T_{LC}}{\Delta T_c} - 1 \right] \quad (11)$$

where T_{cout} is the mixed-mean coolant outlet temperature before the accident initiates. If the value of U_{ARC} computed from eq. (11) is positive for a given value of T_{LC} , the core will only need to make use of the ARC systems in the early phase of the ULOF transient.

For the ULOF case, the following general conclusions about designing for system stability can be drawn:

1. If the core stabilizes following an ULOF event with an allowable coolant outlet temperature without the effect of the ARC system, then the temperature at which the ARC absorber liquid reaches the active core should be set *above* this value. The ARC system is then fully withdrawn in the later stages of the transient, and only impacts system performance in the early stages of the transient to keep temperatures below short-term constraints (coolant boiling point etc.). In such cores, a single ARC system design can be used and the value of total reactivity worth in the ARC system is a variable to be determined by transient analysis, applying the requirement to avoid coolant boiling or cladding damage early on in ULOF transients. Such cores are henceforth denoted QSA (Quasi-Static A).
2. If the core stabilizes following a ULOF event with the coolant outlet temperature significantly above what is allowed (above T_{LC}), the ARC system must be designed to add enough negative reactivity (U_{ARC}) to bring this temperature down to T_{LC} . In such a case, the temperature at which the ARC system is fully actuated (absorber covering the active core) must be set *exactly* equal to T_{LC} , and the total reactivity of this system must be equal to U_{ARC} .

Such cores are henceforth denoted as QSC (Quasi-Static C). If the reactivity vested in the ARC system is larger than U_{ARC} as defined by eq. (11), the system may exhibit oscillatory behavior. If it is smaller than U_{ARC} , the system response will be stable but will reach higher temperatures than allowed.

For the QSA cores, the ARC system fills the sole purpose of reducing temperatures early on in transients. One should note that for many such core designs, no ARC system is needed at all. For QSC cores, the ARC system provides both reduced temperature early on in the transient *and* acceptable quasi-static temperatures. For both QSA and QSC type cores, a single type of ARC system is sufficient.

There is a significant span of core designs where the quasi-static temperature is too high to be acceptable but low enough that the reactivity worth of the corresponding long-term ARC system is not enough to save the core from too high temperatures early on in the transients. Cores that are in between the two extremes of QSA and QSC may require a “double-tier” ARC implementation, which is henceforth denoted QSB, or Quasi-Static B. An ARC system that is designed to ensure low temperatures in the quasi-static state should not be designed to introduce more reactivity than what is needed just for this purpose (which for QSB-type cores may be a very small value). Therefore, such cores may require two separate ARC systems that operate with different reactivity worth and over different temperature spans: one to handle temperatures early on in transients and one to handle the quasi-static state. Naturally, if the core is close to being a true QSA-type core, it makes more sense to redesign it slightly and avoid the need for ARC systems in the quasi-static state altogether. Figure 2 shows the indicative ULOF temperatures and the corresponding recommended ARC temperatures range of actuation for the three types of cores defined above.

Figure 2, ULOF temperatures of different core types and the optimal temperature span of actuation of corresponding ARC systems

The resulting limitations and definitions are summarized in Table 2. Here we have introduced the constant T_{SC} to denote the maximum acceptable short-term coolant temperature in a transient. T_{SC} is typically set at some margin below the coolant boiling temperature, and is significantly higher than the long-term temperature limit T_{LC} .

Table 2, ARC temperature and reactivity limits for ULOF transients

Core type	Total reactivity vested in ARC (U_{ARC})	Start of actuation temperature (T_{ac})	Full-actuation temperature (T_f)
QSA	Determined by transient analysis	$T_{ac} \geq T_{cout} + \frac{A(1 - F_n)}{AF_n + B} \Delta T_{co}$	$T_{ac} < T_f < T_{SC}$
QSB (two separate systems)	$B \left[\frac{F - 1}{F} \right] - \left(A + \frac{B}{F} \right) \left[F \left(1 + \frac{T_{lmax} - T_{cout}}{\Delta T_c} \right) - 1 \right]$	$T_{ac} > T_{cout} + margin$	$T_f = T_{LC}$
	Determined by transient analysis	$T_{ac} > T_{LC}$	$T_{ac} < T_f < T_{SC}$

QSC	$B \left[\frac{F-1}{F} \right] - \left(A + \frac{B}{F} \right) \left[F \left(1 + \frac{T_{LC} - T_{cout}}{\Delta T_c} \right) - 1 \right]$	$T_{ac} > T_{cout} + margin$	$T_f = T_{LC}$
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Figure 3 shows a map of the required reactivity worth of ARC system installations (U_{ARC}) and the approximate division of cores in to QSA, QSB and QSC. For U_{ARC} values above zero (the green QSA region), there is no need for negative reactivity worth from the ARC system in the quasi-static state following a ULOF. This region contains mainly metal-fuelled cores, due their relatively lowered stored reactivity in the fuel at standard operating conditions. By virtue of its small size (8 MWt), lead (rather than sodium) coolant and low power density, the oxide-fuelled SEALER3 core also falls in to the QSA category. As the metal-fuelled reactors grow larger, their inherent reactivity feedback characteristics turn less favorable, and at large sizes, metal-fuelled cores may cross the line in to QSB (if operated at high power density) territory. All surveyed oxide-fuelled cores with power levels above 1 GWt fall in the QSC category.

The values of U_{ARC} shown for specific core designs in *Figure 3* have been calculated using eq. (11) and are approximate estimations rather than exact values (due to limits in available design information). The data used was sourced from references [11] [12] [13] [14] [15] [6] [16].

Figure 3, Map of quasi-static reactivity requirements for ARC system installations for a stabilization temperature of 700°C following a ULOF event

4. Unprotected loss of heat sink (ULOHS)

Following the ULOHS event, eq. (6) simplifies to:

$$C\delta T_{in} + x_{ULOHS} * U_{ARC} = (1 - P_d) \times (A + B) \quad (12)$$

The positive reactivity introduced by bringing normalized power from $P=1.0$ down to $P_d - (A+B)*(1 - P_d)$, is balanced by the negative reactivity introduced by raising the core average (nearly isothermal) temperature ($C * \delta T_{in} + x_{ULOHS} * U_{ARC}$). The coolant outlet temperature in the quasi-static state following a ULOHS event is given by:

$$T_{cout}(ULOHS) = T_{cin} + \frac{(1 - P_d) * (A + B) - x_{ULOHS} * U_{ARC}}{C} \quad (13)$$

where T_{cin} is the pre-transient coolant inlet temperature. If the ULOHS scenario is limiting for the system, the value of U_{ARC} is defined by setting $x_{ULOHS} = 1.0$ in eq. (13):

$$U_{ARC} = U_{ULOHS} = (1 - P_d) * (A + B) + C * (T_{cin} - T_{LC}) \quad (14)$$

Since primary coolant pumps are assumed to still function in an ULOHS event, problems with stability and delay of the ARC systems are not present in the same way as for the ULOF scenario. Even if the value of $T_{cout}(ULOHS)$ is within the actuation temperature span of the ARC system, the full flow conditions and slow transition of the event means that the upper ARC reservoir and the coolant temperature are in equilibrium, which limits the potential for oscillatory behavior to initiate.

5. Unprotected transient overpower (UTOP)

The UTOP scenario increases the reactor power output according to:

$$P_{UTOP} = 1 - \frac{\rho_{ext} + x_{UTOP} \times U_{ARC}}{A + B} \quad (15)$$

Eventually the feedbacks will compensate the positive added reactivity and bring the normalized power back down. The margins of the UTOP scenario are less straightforward to analyze since it is typically the fuel temperature early on in the transient rather than the long-term cladding limits or margin to coolant boiling that effectively sets the limits. The core-averaged fuel temperature of the reactor can be approximately calculated as:

$$T_{fuel} = T_{cin} + \left(\frac{P}{F}\right) \times \frac{\Delta T_{co}}{2} + P \times \Delta T_{fc} \quad (16)$$

Where ΔT_{fc} is the difference between the average fuel and average coolant temperatures at standard operating conditions. Applying a maximum allowable long-term fuel peak centerline temperature of T_{LF} , the required worth of the ARC system (setting $x_{UTOP} = 1.0$) becomes:

$$U_{ARC} = U_{UTOPf} = 2 * (A + B) \times \left(\frac{T_{cin} - \frac{T_{LF}}{p_r}}{\Delta T_{co} + 2\Delta T_{fc}} \right) - \rho_{ext} + A + B \quad (17)$$

where p_r is a peak-to-average fuel temperature multiplier. The requirement set by eq. (17) is effectively only applicable if a large amount of positive reactivity is added to the system slowly. The coolant outlet temperature in the quasi-static state following the UTOP transient is given by:

$$\begin{aligned} T_{cout}(UTOP) &= T_{cin} + \frac{P}{F} \Delta T_{co} \\ &= T_{cin} + \left(1 - \frac{\rho_{ext} + x_{UTOP} \times U_{ARC}}{A + B} \right) \Delta T_{co} \end{aligned} \quad (18)$$

If UTOP is the limiting scenario ($x_{UTOP} = 1.0$), the required ARC system reactivity worth to limit the coolant outlet temperature to T_{LC} is:

$$U_{ARC} = U_{UTOPc} = (A + B) \left(\frac{T_{cin} - T_{LC}}{\Delta T_{co}} + 1 \right) - \rho_{ext} \quad (19)$$

When UTOP is the limiting quasi-static state, whichever value of eq. (17) and (19) is the largest will determine the required ARC worth.

6. The ARC worth and temperatures following partial actuation

In equations (11) (14) (17), and (19), we have determined the expressions that give the required ARC reactivity worth for an acceptable quasi-static state following a set of transients. The ARC system will need to be designed for the transient requiring the highest worth, and it follows that the system (in its quasi-static state) will reach some state of incomplete actuation following any other transient than the one for which it was designed. An even more complete analysis would also include the states following a combination of the individual transients. Partially actuated states are not amenable to direct analytical treatment but may be found using a simple numerical iteration method.

An iterative solver was developed that determines the coolant outlet temperature in the quasi-static state of zero reactivity ($\rho=0.0$) given a guess for the relative actuation

(fraction of complete actuation, in which the ARC-absorber covers the axial extent of the core) of the ARC system. The initial guess of ARC system actuation is zero, i.e. un-actuated. The actual level of actuation is then calculated for the given coolant outlet temperature, and the calculated actuation value is plugged back in to the equation determining reactivity. Once the two expressions agree on the value of ARC worth that has been introduced in the quasi-static state (within some convergence criteria), the details of the partial actuation have been determined. The determination of the reactivity worth of a partial actuation requires some analytical expression or table of values for the axial reactivity worth distribution of the ARC absorber. For a uniformly enriched fast reactor, the axial worth distribution of the ARC actuation can be roughly approximated as [4]:

$$\rho_{ARC}(z) = - \left[\frac{\sin\left(\frac{\pi z}{\tilde{H}}\right) \cos\left(\frac{\pi z}{\tilde{H}}\right) \tilde{H} - \pi z}{\pi \tilde{H}} \right] \times \rho_{ARC} \quad (20)$$

where z is the axial elevation of the ARC liquid absorber from the core bottom, $\tilde{H}$ is the extrapolated core height and ρ_{ARC} is the reactivity worth of a complete actuation (absorber liquid covering the entire core height) at a specific radial location in the core. One should note that eq. (20) was developed using very simple analytical means for a bare cylindrical core of uniform enrichment and is only valid as a rough guide. For accurate results a more precise and system-specific worth-curve should be calculated. Expressions for axially dependent ARC reactivity worth profile are developed in the Section 7 from the results of neutron transport calculations for a specific core.

With the values for T_{ac} (the temperature at which the ARC absorber reaches the core) and T_f (the full actuation temperature) defined, the determination of the value of $x_{transient}$ can be done according to eq. (21):

$$x = \begin{cases} 0.0, T < T_{ac} \\ 1.0, T > T_f \\ - \left[\frac{\sin\left(\frac{\pi \left[\frac{T - T_f}{T_f - T_{ac}}\right]}{\tilde{H}}\right) \cos\left(\frac{\pi \left[\frac{T - T_f}{T_f - T_{ac}}\right]}{\tilde{H}}\right) \tilde{H} - \pi \left[\frac{T - T_f}{T_f - T_{ac}}\right]}{\pi \tilde{H}} \right], T_{at} < T < T_f \end{cases} \quad (21)$$

where T is the iteratively calculated coolant outlet temperature.

Eq. (21) can easily be modified by introducing more detailed expressions of the ARC system axial reactivity worth distribution if such are available. In order to use the method described above, one needs to determine the appropriate expressions of reactivity as a function of the coolant outlet temperature for each transient. In the case of ULOHS, such an expression is given to us directly by plugging in the appropriate values in to eq. (6) (as also seen in eq. (12)):

$$C\delta T_{in} + x_{ULOHS} * U_{ARC} = (1 - P_d) \times (A + B) \quad (22)$$

We may note that following the ULOHS transient, the temperature rise in the core collapses to zero, which gives:

$$T_{out}(ULOHS) = T_{cin} + \delta T_{in} = T_{cout} + \delta T_{out} \quad (23)$$

The ULOHS balance of reactivity may therefore be written as a function of the coolant outlet temperature as:

$$C[T_{out}(ULOHS) - T_{cin}] - (1 - P_d) \times (A + B) + x_{ULOHS} * U_{ARC} = 0 \quad (24)$$

For the ULOF scenario, we start by plugging in appropriate values in to eq. (6):

$$(P - 1)A + \left(\frac{P}{F_n} - 1\right)B + x_{ULOF} * U_{ARC} = 0 \quad (25)$$

This expression may be re-written using the relation:

$$\delta T_{out} = \left(\frac{P}{F_n} - 1\right) \Delta T_c \quad (26)$$

from which we can extract the multipliers of A and B in eq. (25) as:

$$P - 1 = \frac{F_n \delta T_{out}}{\Delta T_c} + F_n - 1 \quad (27)$$

$$\frac{P}{F_n} - 1 = \frac{\delta T_{out}}{\Delta T_c} \quad (28)$$

The coolant outlet temperature is given by:

$$T_{out}(\text{transient}) = T_{cout} + \delta T_{out} \quad (29)$$

which finally gives the expression for reactivity following the ULOF scenario as:

$$\left(\frac{F_n [T_{out}(\text{ULOF}) - T_{cout}]}{\Delta T_c} + F_n - 1 \right) A + \left(\frac{[T_{out}(\text{ULOF}) - T_{cout}]}{\Delta T_c} \right) B + x_{ARC} * U_{ARC} = 0 \quad (30)$$

For the UTOP scenario, plugging in appropriate values in eq. (6) gives:

$$(P - 1)A + (P - 1)B + \rho_{ext} + x_{ARC} * U_{ARC} = 0 \quad (31)$$

Using eq. (28) and (29) and making use of the fact that the normalized flow rate remains unchanged ($F=1.0$), gives:

$$\left(\frac{T_{out}(\text{UTOP}) - T_{cout}}{\Delta T_c} \right) (A + B) + \rho_{ext} + x_{UTOP} * U_{ARC} = 0 \quad (32)$$

7. Case studies for Advanced Burner Reactor (ABR) core designs

The methods developed in the previous sections have been applied to the oxide-fuelled cores developed in the Argonne National Laboratory (ANL) Advanced Burner Reactor (ABR) study [17]. The ABR core designs of ref. [17] were chosen because there is enough information available to enable a detailed study (both geometry, thermal-hydraulics and kinetic parameters are available) and the core designs span a wide range of inherent safety performance. One should note that the ABR designs

presented in ref. [17] are preliminary designs, and more mature versions of the ABR cores have been developed since [18]. The metal, nitride and carbide fuelled cores of the ABR study have not been analyzed since they already exhibit excellent passive safety behavior.

The ABR core designs are based on the 1000 MWt SuperPRISM (S-PRISM) reactor [19]. There are slight variations with conversion ratio but the basic ABR configuration consists of 144 fuel assemblies and between 9 and 22 primary control assemblies. The assemblies are designed to fit within the same geometry, but the size, number and composition of fuel pins within each assembly are significantly different in order to achieve the target conversion ratio while satisfying thermal-hydraulic limits. The general input data for the analysis of the oxide-fueled ABR cores are summarized in *Table 3*. The average fuel temperature rise over the coolant was estimated from eq. (33):

$$\Delta T_f = \left(\frac{2}{3}\right) * \frac{P_{lin}}{4\pi k} + \Delta T_{gap} + \Delta T_{clad} \quad (33)$$

where k is the fuel conductivity [~ 2.3 W/m/K [20]], P_{lin} is the average linear power of the fuel, ΔT_{gap} is the temperature drop across the initial gas gap within the pin and ΔT_{clad} is the temperature drop across the cladding. The value of $\Delta T_{gap} + \Delta T_{clad}$ for all of the ABR oxide cores at full power is $\sim 100^\circ\text{C}$.

Table 3, General input data for ABR oxide core quasi-static evaluation

Conversion ratio	1.0	0.75	0.50	0.25	0.00
Average linear power (kW/m)	17.5	18.3	15.3	15.2	15.2
Fuel temperature rise ($\Delta T_f, ^\circ\text{C}$)	503.7	519.8	452.9	452.9	450.6
Effective delayed neutron fraction (β_{eff})	0.0035	0.0033	0.0030	0.0027	0.0024
Transient overpower initiator (ρ_{ext}, ϕ) (withdrawal of the highest-worth control assembly)	2	28	60	59	78
Coolant temperature rise ($\Delta T_{co}, ^\circ\text{C}$)	155				
Normalized natural circulation flow rate (F_n)	0.03 (3%)				

Passive heat removal capacity (P_d)	0.005 (0.5%)
Temperature-change-to-core for the ARC system (ΔT_{ac})	50°C ($T_{ac} = 560^\circ\text{C}$)
Maximum allowable long-term temperature (T_{LC}) and full-actuation ARC-temperature (T_f)	750°C ($\Delta T_f = 190^\circ\text{C}$)

The reactivity feedbacks and corresponding required ARC system reactivity worth are summarized in Table 4. The calculation of U_{ARC} in Table 4 is based on the limiting (most serious) event, which for every core analyzed is the ULOF transient. This fact holds even when varying F_n , ρ_{ext} and P_d in wide ranges of values ($0.02 < F_n < 0.1$, $0.005 < P_d < 0.1$ and for ρ_{ext} up to double its given value).

Table 4, Reactivity feedback parameters of the ABR cores (oxide)

Conversion ratio		1.0	0.75	0.50	0.25	0.00
Radial expansion coefficient ($\phi/^\circ\text{C}$)	BOEC*	-0.28	-0.35	-0.41	-0.48	-0.57
	EOEC	-0.29	-0.35	-0.43	-0.50	-0.60
Fuel temperature coefficient [†] ($\phi/^\circ\text{C}$)	BOEC	-0.41	-0.52	-0.60	-0.69	-0.76
	EOEC	-0.41	-0.54	-0.63	-0.73	-0.81
Structure density coefficient ($\phi/^\circ\text{C}$)	BOEC	0.08	0.08	0.09	0.10	0.11
	EOEC	0.08	0.08	0.09	0.11	0.12
Sodium density coefficient ($\phi/^\circ\text{C}$)	BOEC	0.13	0.15	0.18	0.18	0.19
	EOEC	0.14	0.15	0.19	0.20	0.21
A (ϕ)	BOEC	-206.5	-270.3	-271.7	-312.5	-342.5
	EOEC	-206.5	-280.7	-285.3	-330.6	-365.0
B (ϕ)	BOEC	-47.1	-61.9	-71.8	-85.9	-99.9
	EOEC	-47.4	-63.5	-75.6	-88.9	-104.8
C ($\phi/^\circ\text{C}$)	BOEC	-0.48	-0.64	-0.74	-0.89	-1.03
	EOEC	-0.48	-0.66	-0.78	-0.92	-1.08
U_{ARC}	ϕ	-117.8	-160.9	-146.5	-167.7	-174.8
	pcm	-412	-531	-439	-453	-419

* BOEC: Beginning of equilibrium cycle, EOEC: End of equilibrium cycle

[†] The sum of fuel axial expansion and Doppler coefficients

The feedback parameters of Table 4 were calculated for the reference cores without the installation of an ARC system. However, since no installation requires more than one rod per fuel assembly to be replaced (out of a total of 271 to 324 total rods per assembly), the error introduced from this approximation is minor. In addition to the reactivity feedbacks listed in Table 4, there will be a negative feedback contribution from the expansion of control rod drivelines pushing the control rods further in to the core. Since this analysis is based on the feedback at the beginning of the cycle (when all controls can be assumed to be inserted the most) and at the end of cycle (when control rods are mostly withdrawn), the amount of reactivity gained from the expansion of control rod drivelines is expected to be rather small, at least in the cores with high conversion ratio. For conservatism, and due to the lack of specific information in the ABR reports, the control rod driveline feedback has been omitted in the simulations and calculations of this study. Future studies will include detailed modeling of the control rod expansion systems, since this feedback operates in a similar way as the ARC system and responds (with a time delay) to the coolant outlet temperature. Given that the control rod drivelines and ARC systems will have different time constants, it will be important to analyze their actuation characteristics with regards to stability and the potential for oscillatory behavior in the system.

In order to define the axial reactivity worth curve and to estimate the size of the ARC systems needed to provide the U_{ARC} values listed in Table 4, the worth of the ARC absorber (enriched lithium with 99% ^6Li) was calculated for the ABR CR=0.75 MOX-fuelled core. The calculation was performed using the Serpent v.2.1.24 code [21] with ENDF/B-VII.0 [22] neutron cross-sections. The system was modelled heterogeneously (no homogenization of materials) using the ADOPT code [23], separated in to nine axial regions in the active core. In the calculations, one fuel rod was replaced by an ARC rod in each assembly. A top-view of the ABR core model (with control-assemblies withdrawn) in the Serpent code is shown in Figure 4.

Figure 4, Top-view of Serpent-model of the ABR core

Although the analytical expression defined in eq. (20) approximates the lithium-worth-curve for the ABR cores fairly well, a slightly modified expression using a Fourier-type curve was developed to give a more exact profile for the MOX-fuelled ABR CR=0.75 core as:

$$\rho_{ARC}(z) = -[a_0 + a_1 * \cos(z * a_2) + a_3 * \sin(z * a_4)] \times \rho_{ARC} \quad (34)$$

where a_0 - a_4 are constants fitted to the data. The fraction of total worth added as a function of the elevation of the absorber liquid of the ARC system in the active core is shown in Figure 5 for a simultaneous actuation of all the ARC systems across the core (one in each fuel assembly).

Figure 5, Axial worth curve of ARC absorber (simultaneous actuation across the core)

For greater simulation accuracy, the ARC reactivity worth curves were also calculated for each of the three fuel regions separately, enabling better multi-channel simulations in transient analysis codes. While the channel division could be extended to each assembly, the computational cost associated with such a high fidelity approach is not justified by the objectives of the current study. The ARC absorber worth distribution (as a fraction of the total ARC-system worth) between the three fuel regions is 60.0% in the inner batch, 22.8% in middle batch and 17.2% in outer batch. While the multi-channel approach is more accurate and we have used it for the final transient analysis of this study (presented in Section 8), we have not seen any qualitative differences in the transient progression when using a single channel rather than three channels. The results show that the maximum difference in the peak core coolant outlet temperature at any time during a transient simulation when using a single channel and three channels is less than 20K (~8% of total temperature change), indicating that a single channel approach is acceptable for this type of scoping analysis.

Using the exact worth distribution expressions and the calculation approach described in Section 6, the quasi-static coolant outlet temperatures following each transient were calculated for the ABR cores and the results are summarized in Table 5.

Table 5, Quasi-static coolant outlet temperatures following transients in ABR oxide cores

ABR Oxide Core (conversion ratio)			1.0	0.75	0.50	0.25	0.00
ULOF	BOEC	Ref.	>Boiling	>Boiling	>Boiling	>Boiling	>Boiling

(°C)		ARC	750.0	738.4	739.8	735.3	735.7
	EOEC	Ref.	>Boiling	>Boiling	>Boiling	>Boiling	>Boiling
		ARC	750.0	750.0	750.0	750.0	750.0
ULOHs (°C)	BOEC	Ref.	880.6	871.5	679.1	675.9	782.3
		ARC	690.9	685.3	675.9	675.0	672.5
	EOEC	Ref.	881.3	873.9	815.4	808.7	787.8
		ARC	691.0	688.3	681.4	680.3	677.3
UTOP (°C)	BOEC		511.1	522.5	537.0	532.9	537.2
	EOEC		~510				

All of the ABR oxide core designs are limited by the ULOF-scenario in the quasi-static state (and all but the CR=1.0 core are limited at EOEC). It is clear from the data that all these cores are of the QSC type, at least when excluding the control rod driveline feedback contribution. The UTOP event is so benign in all cores that coolant outlet temperatures never reach values required for the ARC absorber to have an impact on reactivity, and the fuel temperature retains a large margin to melting. Given the benign UTOP scenarios and that temperatures in the ULOHS events rather slowly transition to their quasi-static values, the ULOF scenario is also the main concern in the short term.

8. Transient simulations with ARC systems for the ABR core

8.1. Modeling setup

In order to investigate the transient behavior of the ARC system during reactor transients, such as unprotected loss of flow (ULOF) and unprotected loss of heat sink (ULOHs), a dynamic model of the ABR (CR=0.75) with an ARC system installed was made using the CHD code. A detailed description of the code together with comparative benchmark of other established codes can be found in [24].

An overview of the structure of the model is given in Figure 6. Here, blue blocks correspond to flow components modeling the advective as well as conductive heat transfer in the coolant. Gray blocks correspond to solid components such as fuel rods, neutron reflectors and fuel assembly ducts, and model heat conduction and the fission source power. Red blocks are extra reactivity control objects, and the dashed lines indicate where they interact with the reactor model. The fuel rods as well as the coolant channels were divided into 101 axial nodes, and the fuel rods were further

divided into 50 radial nodes. In this paper three separate channels were used with each channel representing all fuel rods in the inner, middle and outer batches, respectively.

Figure 6. Block structure of the CHD code used to simulate the transient behavior of the ARC system in an ABR core. As indicated, the modeled core can contain an arbitrary number of coolant channels.

The CHD code uses a standard point-kinetic model with eight groups of delayed neutrons to calculate the core fission power. Spontaneous neutron emission is assumed to be dominated by Pu-240, and it is modeled from the concentration of Pu-240 in the fuel. The spontaneous neutron emission is irrelevant for any normal operation, but it is important when modeling extended loss of heat sink scenarios when the reactor is first neutronically shut down and subsequently becomes critical again. Delayed neutrons are calculated for each system analyzed using the 8 groups results obtained from Serpent calculations [21], and decay heat is calculated using the 23 group model for plutonium in [25]. For the ABR cores, the temperature feedback coefficients in Table 4 are used. The radial expansion feedback is given by eq. (35):

$$dk_R = \alpha_R \left[\Delta T_{\text{grid}} + \frac{h_1}{h_2} (\Delta T_{\text{pad}} - \Delta T_{\text{grid}}) \right] \quad (35)$$

where $h_1 = h_{rod}/2 - z_{nozzle}$, $h_2 = z_{pad} - z_{nozzle}$ (see Figure 6 for definitions) and ΔT_{grid} and ΔT_{pad} are the temperature deviations from the nominal operating conditions at the grid plate and the fuel assembly duct spacer pads, respectively. As explained in Section 7, the feedback due to the control rod drivelines expansion have been omitted in these simulations.

The main functions of the CHD code are in principle similar to that of other multi-channel point-kinetics based transient analysis codes such as SAS4A/SASSYS-1 [26], THACOS [27], SSC-L [28], MAT5-DYN [29], with the major addition being the ability to explicitly model the passive operation of ARC systems with great flexibility. The current version of the CHD code does not have the capability to analyze assembly-to-assembly heat transfer, which may have an impact on the above-core coolant temperatures.

Rather than explicitly calculating the heat transfer to the ARC liquids inside the CHD code at each time step, the heat transfer characteristics are pre-calculated using a full-detail computational fluid dynamics model for each specific ARC-equipped fuel assembly. For the simulations of this study, the heat transfer to the upper ARC reservoir from the coolant was calculated using the COMSOL Multiphysics software package for a wide range of coolant temperatures and different primary coolant flow velocities. The design of the upper ARC reservoir has been improved to the point where, at full flow in a typical fast reactor system, the time response is fast enough that very little lag is seen between the temperature driving the ARC system and the coolant outlet temperature. For the ABR core, for example, following a step change in the coolant temperature, 63% (a factor $1-e^{-1}$) of the initial temperature difference between the coolant and the volume-averaged temperature of the upper ARC reservoir liquid is erased in ~ 1.3 seconds, and 90% of the initial difference is gone after ~ 3 seconds. As the flow rate is reduced in a loss of flow event, the time lag increases.

This simulation data is fed to the CHD code in the form of a time-lag estimator equation which describes how the temperature driving the ARC system differs (lags) from that of the coolant outlet temperature. To quantify the time-lag between the coolant and the volume-averaged upper reservoir temperature through heat transfer to the reservoir liquid, we introduce a lag-parameter τ , defined as the value that best fits the simulation of a step-change in coolant temperature using the following type of equation:

$$T_R(t) = T_{Na} + (T_R(0) - T_{Na}) \times e^{-\frac{t}{\tau}} \quad (36)$$

where T_{Na} is the coolant temperature when it reaches the upper reservoir elevation, T_R is the volume-averaged liquid temperature in the reservoir and t is the time variable of the simulation. For the ABR-core ARC implementation, the following expression for the τ and the flow velocity has been developed from the CFD analysis data:

$$\tau(F) = \tau_{(F=1)} \times (4.246e^{-26.44F} + 2.074e^{-0.729F}) \text{ [s]} \quad (37)$$

where F is the normalized flow rate ($F=1$ means full flow) and $\tau_{(F=1)}$ is the value of τ at full flow conditions. At 15% flow (partial loss of flow), the value of τ is doubled, while down at 3%, it is at nearly 4 times the full-flow value. The CFD simulation results for the τ of the ABR upper ARC reservoir for 7 different flow rates is shown in Figure 7.

Figure 7, The impact of flow rate on the value of τ
(COMSOL CFD simulations results)

For each time-step, the code calculates the coolant temperature in each channel, which is used along with the flow rate and the data from the CFD analysis to estimate the volume-averaged temperature of the liquids in the ARC system. From this result, the code calculates the corresponding axial levels of the ARC absorber liquids in each modelled channel of the core. Pre-calculated worth tables or expressions (such as that of eq. (34), shown in Figure 5) are then used to estimate the impact on the reactivity of the core by the ARC systems.

A detailed model of the ABR fuel assembly with an ARC-system installed was developed using the ARCAD code [5], and used for the CFD calculations of heat transfer and time delay. A cut-through image of the full assembly is shown in Figure 8.

Figure 8, Cut-through view of the ARC-equipped ABR driver fuel assembly

The uppermost section of the assembly, containing the upper ARC liquid and gas reservoir, is shown in Figure 9. The lowermost section, with the lower ARC reservoir and its shielding, is shown in Figure 10.

Figure 9, Top section of the fuel assembly, with components of the ARC system marked

Figure 10. Lowermost section of the fuel assembly

The details of the ARC installation for the ABR cores are summarized in Table 6 - Table 9.

Table 6. Actuation characteristics and input parameters for ABR CR=0.75 MOX

Parameter	Value	Description
U_{ARC}	-161¢	ARC total reactivity worth
ΔT_{ac}	50°C	Temperature rise in the upper reservoir liquid for the ${}^6\text{Li}$ absorber to reach the active core from its initial position beneath the core in the lower reservoir.
T_{ac}	560°C	Temperature in upper ARC reservoir when absorber reaches core.
ΔT_f	190°C	Temperature rise of actuation
T_f	750°C	Temperature in upper ARC reservoir at full actuation
P_{max}	1 MPa	Maximum system pressure (at full actuation at EOL)
ϕt	1.8×10^{21} n/cm ²	Total volume-averaged neutron fluence in the ${}^6\text{Li}$ absorber liquid in the lower reservoir in the ABR core

Table 7, ARC tube geometry

Parameter	Value	Description
N_{ARC}	1	Number of ARC rods per fuel assembly
V_f	0.098%	The volume fraction of the core occupied by ARC ${}^6\text{Li}$ absorber at full actuation
R_{oARC0}	4.040 mm	Outer ARC tube outer radius (same as fuel rod)
R_{oARCi}	3.405 mm	Outer ARC tube inner radius (same as fuel rod)
R_{iARC0}	2.245 mm	Inner ARC tube outer radius (in the core region)
R_{iARCi}	1.610 mm	Inner ARC tube inner radius (in the core region)
R_{iARC0B}	2.405 mm	Inner ARC tube outer radius (below core)
R_{iARCiB}	1.770 mm	Inner ARC tube inner radius (below core)
N_w	14	The number of cylindrical wires (with a diameter of 1 mm) that are helically wrapped around the inner ARC tube in the below-core region
W_p	5.3 cm	The axial length of one full rotation of the wires

Table 8, Upper ARC reservoir

Parameter	Value	Description
V_{UR2}	486.4 cm ³	The volume of liquid in the upper ARC reservoir
L_{UR2}	14.5 cm	Axial length of the liquid-filled region of the upper ARC reservoir
V_{UR1}	77.7 cm ³	Volume of gas reservoir
L_{UR1}	5.5 cm	Axial length of the gas reservoir section of the upper

		ARC reservoir
L_{UR}	20.0 cm	Total axial length of upper ARC reservoir

Table 9, Lower ARC reservoir

Parameter	Value	Description
V_{LR1}	658.1 cm ³	The volume for liquid and gas in the lower ARC reservoir
L_{LR1}	5.3 cm	Axial length of the liquid-filled region of the lower ARC reservoir
L_{LR2}	10.0 cm	Axial length of shielding piece above lower reservoir
L_{LR}	15.3 cm	Total axial length of lower reservoir
m_a	14.9 g	Mass of lithium (99% ⁶ Li) loaded in the system per assembly

8.2. *Quasi-static state validation*

To ensure consistency between the numerical calculations and the analytical analysis presented in sections 4-6 (the results of which are summarized in Table 5 for the ABR), transients were run in the CHD code until the quasi-static state was reached. Decay heat was turned off in the simulations for the quasi-static validation to more quickly match the analytical results. In all cases, the quasi-static temperatures predicted matched those reported by the code simulation within an error margin of $\pm 1K$.

In Figure 11, the result of a ULOHS simulation in the ABR CR=0.75 oxide core, with an ARC-system designed to handle the ULOF event, is compared to the result as predicted using eq. (24). Coolant inlet and outlet temperatures are shown in blue and red, respectively, and the asymptotic quasi-static temperature is shown in dashed black. A passive heat removal rated at 0.5% of full power is included, which results in the reactor stabilizing at this power.

Figure 11. ABR ULOHS quasi-static temperature prediction and transient simulation results (QS: Predicted quasi-static temperature)

In Figure 15, the results of a ULOF simulation in the ABR CR=0.75 core is shown. The ARC system is designed to reach an asymptotic coolant temperature of 750°C. Here, primary coolant flow is reduced according to $F(t)=F_0/(1+t/\tau_{ULO\text{F}})$, where $\tau_{ULO\text{F}}$ is assumed to be 17 s. The coolant outlet temperature reaches the ARC operating range after about 12 s into the transient and a significant negative reactivity is introduced. This starts to bring down the reactor power faster and a coolant boiling is avoided with the outlet temperature asymptotically approaching the quasi-static design value.

8.3. Avoiding oscillatory behavior by design

The reactivity response of the ARC system is delayed with respect to the initiating event in a transient. For example, during an increase in power, the heat must first propagate from the fuel to the coolant, subsequently the coolant must travel from the core to the ARC system upper reservoir, and finally the coolant must transfer the heat to the upper reservoir coolant. Since the response of the ARC is delayed, this opens up for power and temperature oscillations of the core. To investigate the possibility of power oscillations, a systematic stability study was made for the MOX-fuelled ABR core (CR=0.75) that was modeled in the CHD code.

There are three main factors that contribute to the delay of the ARC response:

- First, there is a delay in communication of the temperature in the core to the upper ARC reservoir due to the finite flow velocity of the coolant. At nominal operating conditions in a sodium cooled reactor, the flow velocity of the coolant is of the order of 10 m/s. With a plenum length of about 1 to 2 meters, the flow delay of the coolant is rather small (~0.1 to 0.2 seconds). However, if the initiating event is a loss of flow accident, the flow velocity will decrease considerably to only a few percent of full flow, which means that the flow delay can reach several seconds.
- Second, any structural material above the core, but below the ARC system, has a thermal inertia. If the coolant leaving the active core is at a higher temperature than these structures, heat transfer to these structures will reduce the temperature of the coolant by the time it reaches the upper ARC reservoir, effectively introducing a “delay” in the communication between the core and the ARC system.
- The third and final delay is that due to heat transfer between the coolant and the liquid in the upper ARC reservoir. The details of this delay have been discussed in an earlier publication [5].

As described in Section 3, loss of flow events are the most susceptible to oscillatory behavior since the reduction of the primary coolant flow rate acts to increase the time delay between changes in the coolant temperature and the volume-averaged temperature in the upper ARC reservoir, which drives the actuation of the ARC system. In an ARC system designed to stabilize (in the quasi-static state) at a temperature far below what the stabilizing temperature would have been without an ARC system, the system has to provide a large amount of reactivity for every degree of temperature change. The higher the temperature at which stabilization is to be achieved with the ARC system engaged, the smaller the reactivity added per degree of change in temperature is, and consequently, the risk of introducing ARC-driven oscillations is reduced. In this lies a conflict in the ambition of the design of the ARC system and its response; the more benign the long-term (quasi-static) state is designed to be, the more difficult it becomes to ensure an acceptable transient response in the short term.

Following a ULOF event, the coolant outlet temperature of the ABR CR=0.75 (MOX-fuelled) core does not stabilize below the boiling point (see Table 5), and thus a significant amount of reactivity needs to be added to bring its neutronic stabilization temperature down toward acceptable levels. The oscillations that can be triggered in such a system are illustrated in Figure 12, which shows the transient behavior during an ULOF event with an ARC system deliberately designed to trigger oscillations.

Decay heat, using the 23 group structure described above, is included in this simulation in contrast to the illustration provided in Section 8.2 above. The ARC system in this simulation is designed to stabilize at 675°C, ($\Delta T_{ac} = 50^\circ\text{C}$, $\Delta T_f = 115^\circ\text{C}$), and the large amount of reactivity added per degree of temperature change (-5.6 pcm/K on average) causes severe oscillations. To quantify the level of the oscillations, we define ΔT_{env} as the maximum amplitude of the envelope that encompasses the oscillations. In this example, ΔT_{env} is about 130 K.

Figure 12, Oscillatory temperature following a ULOF event in core with an ARC system that is designed to stabilize at 675°C (948K)

To investigate under which conditions that power oscillations could be manifested, two systematic stability studies were made for the MOX-fueled ABR core (CR=0.75) using the CHD code.

First we varied the amount of steel between the active core and the location of the upper ARC reservoir. This was done for 4 different ARC designs with quasi-static stabilization temperatures (T_{QS}) of 675, 700, 725 and 750°C. The results are summarized in Figure 13. It can be seen that with no steel mass between the active core and the upper reservoir, none of the ARC designs introduce significant oscillations. However, adding 50 grams of steel per pin between the core and the upper reservoir, unacceptable oscillations with $\Delta T_{env} > 40^\circ\text{C}$ develop for the designs with $T_{QS} = 675$ and 700°C. In the reference ABR design with a 191.14 cm long gas

plenum, the mass of the cladding for the upper fission gas plenum alone is about 180 gram per pin. In addition, the duct wall above the fuel adds ~90 grams per pin of above-fuel steel. Unless the upper plenum is significantly reduced in size, ~270 grams of steel should be assumed as the lowest possible value of the above core mass (and heat capacity). The ARC design with $T_{QS} = 725^{\circ}\text{C}$ (-4.3 pcm/K average) shows small oscillations, and with $T_{QS} = 750^{\circ}\text{C}$ (-2.8 pcm/K average), no oscillations occurred for up to 400 grams of steel above the core per fuel rod (50% more than the reference design value).

Figure 13, Impact of above-core structures on the oscillatory behavior following ULOF events.

In the second systematic study, we varied the asymptotic flow rate to analyze partial loss of flow events. Flow rate values between 2.5% and 50% of full flow were studied for the same four ARC designs with $T_{QS} = 675, 700, 725$ and 750°C described above.

As discussed in Section 3, the ARC system need to be designed specifically to handle the most challenging transient scenario, leading to a full actuation in this event and partial actuations in all other events. Since a “complete” loss of flow down to asymptotic natural circulation flow levels is the most challenging for the ABR cores, the ARC systems are designed to be fully actuated following this event. Consequently, there is a risk for oscillatory behavior when the system is partially actuated in partial loss-of-flow events. This type of behavior is seen very clearly in

the transient analysis results shown in Figure 14. The worst oscillations occur at flow rates that are 15-25% of nominal; the lower flow is causing the time delays to increase, and the ARC system is partially actuated at a state where minor changes in temperatures leads to more significant reactivity impacts. Again, with a system designed to stabilize at 750°C, oscillations are not seen regardless of what asymptotic flow rate the transient levels off at.

Figure 14, Oscillatory behavior following ULOF events for varying levels of asymptotic flow rate.

8.4. Transient core response with and without ARC systems

In this section, we present the final transient analysis results for the ABR Oxide-fuelled core with conversion ratio of 0.75, for both the reference and the ARC-equipped version of the core. The ARC-installation simulated is the one defined in Table 7 - Table 9, and is configured to stabilize the core at an asymptotic mean coolant outlet temperature of 750°C following a ULOF event.

The temperature, power and net reactivity of the core following the initiation of the ULOF event are shown in Figure 15. The lithium of the ARC system reaches the bottom of the active core as the temperature of the upper reservoir reaches 560°C, which is the cause of the sharp drop in net reactivity just after 10 seconds in to the transient. After the first few minutes of the transient, the system slowly and

monotonously transitions toward its quasi-static critical state with a coolant outlet temperature (as designed) of 750°C.

Figure 15, Upper panel: ABR ULOF quasi-static temperature prediction and transient simulation results. Upper panel: Mean coolant outlet temperature (dotted black line: Quasi-static prediction), Lower panel: Power and net reactivity. Note the discontinuity in the reactivity after about 10 s, which is due to the ARC system being engaged.

Figure 16 shows the results of a ULOHS simulation. The reference core stabilizes at a temperature just 10K below the atmospheric-pressure boiling point of sodium, while the ARC-equipped core slowly transitions to a quasi-static critical state with a uniform coolant temperature just above 700°C (>180°C margin to boiling). About 2 hours in to the transient, the core is neutronically shut down, but the decay heat power level is still above the decay heat removal capacity (0.5% of full power) and the temperature of the core continues to increase, bringing it sub critical. After about 13 hours (46'000 s) the decay heat level falls below the capacity of the decay heat removal system and the temperatures start to decrease until the core becomes critical again after about 47 hours (170'000 s). Following a transient transition, the core stabilizes again, in a critical state, at a power level matching the capacity of the decay heat removal system. Since the normalized flow-to-power ratio reaches 200 ($F=1$, $P=0.05$), the coolant temperature rise at this point is less than 1°C.

Figure 16, ULOHS simulation results. Upper panel: Coolant temperatures (dotted lines are without ARC systems installed), Mid Panel: Reactivity components, Lower Panel: Power (total and decay) and flow rate

Finally, Figure 17 shows a “station blackout” scenario (SBO), which is essentially a combination of the ULOF and ULOHS events. In the reference case, the station blackout scenario leads to coolant boiling at 87 seconds in hottest channel. With the ARC system installed, the coolant temperature retains a ~80 degree margin to boiling throughout the transient, and stabilizes at around 720°C. Since the coolant inlet temperature increases in the SBO event, more negative reactivity is introduced than in the typical ULOF event (in which inlet temperature is assumed to remain constant). Because of this, the peak coolant temperature reached in the SBO event is in fact about 30°C lower in the ULOF event.

Figure 17, Station blackout simulation results. Upper panel: Coolant temperatures, the three red lines correspond to the three separate channels. Mid panel: Reactivity components, Lower Panel: Power and flow rate

The UTOP event was simulated but shows no differences between the ARC-equipped and reference-design cores, since the coolant temperature rise is not high enough for the lithium of the ARC system to reach the active core.

9. Conclusions and future work

The principles for the design and operation for ARC systems have been defined in detail in an earlier paper [5]. The present study expands on this work by providing the principles for how to tailor the response of an ARC system and, crucially, how to avoid the potential for oscillations induced by ARC systems in the transients. The main conclusions of the present work are:

- The upper reservoir of the ARC system can be designed to respond quickly enough to make a positive impact on safety in nearly any transient scenario, with the possible exception of a unprotected transient overpower event such a very rapid inadvertent withdrawal of control rods. At full flow conditions in a typical fast reactor, the volume-averaged liquid temperature in an annular-type upper ARC reservoir will follow the coolant temperature with a time-lag parameter (τ) of approximately 1.4 seconds.
- If ARC systems are designed to introduce too much worth per degree of temperature change, the time delay of actuation, especially in loss of flow events, may cause severe (and unacceptable) oscillations in power and temperature. Such behavior can be avoided entirely by accepting a higher quasi-static stabilization temperature, thus increasing the operating temperature range of the ARC system and decreasing its total reactivity worth. As seen in the simulations of this study, at some point for each system, the potential for oscillations die out.
- The presence of above-core structures such as upper axial reflectors increases the effective time-lag between the temperatures driving the ARC system and the coolant temperature. If no above-core structures are present, the system is much less prone to oscillatory behavior. Oscillations are seen for certain designs even when having small amounts of above-core structures, but these oscillations do not increase markedly as the amount of above-core structure is further increased. Again, reducing the reactivity worth added per kelvin from the ARC system can eliminate the risk of oscillations regardless of the amount of above-core structures.

- In systems designed for ULOF events according to the principles laid out in Figure 2, the risk for oscillations remains during partial actuations. In events where, for instance, some primary pumps fail while others continue to operate, the combination of partial ARC actuation and the time-lag from the lower flow velocity combine to increase the risk of initiating oscillations. In systems designed for full actuation and stability in complete loss of flow events (down to natural circulation), the worst oscillations are seen at flow rates 15-25% of nominal. It was found that this problem can also be eliminated by reducing the reactivity worth per temperature change in the ARC system.

In this study we have ignored the impact of control rod driveline feedback. In systems with relatively large amounts of reactivity vested in control systems, there may be important and complex dynamics between the response of ARC systems and the reactivity introduced from the expansion (and contraction) of the control rod drivelines, which needs to be further analyzed in future work.

While we have completed an extensive analysis using transient analysis codes in this study, a satisfactory all-encompassing analytical analysis method that defines at what point the system design will introduce un-acceptable oscillatory behavior has not yet been found, but is an interesting avenue for potential future work. It should be analyzed whether it is possible to perform more comprehensive stability analysis such as a root locus type analysis and applying Routh's criterion as has been done in the past for liquid metal reactor systems at Argonne National Laboratory [30] [9]. In addition, the transient analysis performed here with the CHD code will be supplemented by simulations with the well-known SAS4A/SASSYS-1 code system [26] for further validation of these results.

As can be seen in Figure 15-Figure 17, it is possible to design an ARC-system to respond to the most severe anticipated unprotected transients in a typical fast reactor core and maintain temperatures at acceptable levels throughout the events, without introducing any oscillatory behavior. The economic and neutronics penalty of replacing one fuel rod per assembly (out of 271) and installing the ARC reservoirs (thereby slightly increasing system pressure drop) is likely more than weighed up by the assured passive safety-performance of the ARC-equipped core. Perhaps more importantly, incorporating ARC-equipped fuel assemblies in to design process of new fast reactor cores may open up a design space that was previously inaccessible due to the requirements of passive safety performance relying solely on existing reactivity feedback components.

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11. References

- [1] S. Qvist och E. Greenspan, "The Autonomous Reactivity Control System," i *Proc. of ICAPP 2014*, Charlotte, NC, US, 2014.
- [2] S. Qvist och E. Greenspan, "An Autonomous Reactivity Control System for Improved Fast Reactor Safety," *Progress in Nuclear Energy*, vol. 77, pp. 32-47, 2014.
- [3] S. Qvist och E. Greenspan, "Inherent Safety of Breed and Burn Reactors," i *International Congress on Advances in Nuclear Power Plants*, Chicago, IL, US, 2012.
- [4] S. Qvist, "Safety and core design of large liquid-metal cooled fast breeder reactors," Berkeley, CA, USA, 2013.
- [5] S. A. Qvist, C. Hellesen, A. E. Dubberley, M. Gradecka, R. Thiele och E. Greenspan, "Autonomous Reactivity Control (ARC) — Principles, Geometry and Design Process," *Nuclear Engineering and Design (In Review)*, 2016.
- [6] D. C. Wade och E. K. Fujita, "Trends Versus Reactor Size of Passive Reactivity Shutdown and Control Performance," *Nuclear Science and Engineering*, vol. 103, nr 2, p. 182–195, 1989.
- [7] K. O. Ott, "Inherent Shutdown Capabilities of Metal-Fueled Liquid-Metal-Cooled Reactors During Unscrammed Loss-of-Flow and Loss-of-Heat-Sink Incidents," *Nuclear Science and Engineering*, vol. 99 , nr 1, p. 13–27, 1988.
- [8] E. V. Depiante, "Dynamic stability boundaries of a liquid metal cooled reactor system," i *Proceedings of the International Topical Meeting on Advanced Reactors Safety* , Pittsburgh, Pennsylvania, 17-21 April. , 1994.
- [9] E. V. Depiante, "Stability analysis of a liquid-metal reactor and its primary heat transport system.," *Nuclear Engineering and Design*, vol. 152, nr 1-3, p. 361–377, 1994.
- [10] S. Qvist, "Optimizing the Design of Small Fast Spectrum Battery-Type Nuclear Reactors," *Energies*, vol. 7, pp. 4910-4937, 2014.
- [11] D. C. Wade och L. Walters, "ARC-100: a sustainable, modular nuclear plant for emerging markets.," i *Proceedings of ICAPP 2010*, San Diego, CA, USA, 2010.

- [12] A. Lazaro och et. al., "Code assessment and modelling for Design Basis Accident analysis of the European Sodium Fast Reactor design. Part II: Optimised core and representative transients analysis," *Lazaro, A., Schikorr, M., Mikityuk, K., Ammirabile, L., Bandini, G., Darmet, G.*, vol. Nuclear Engineering and Design, nr 277, pp. 265-276, 2014.
- [13] S. R. Paranjpe, O. P. Singh och R. Harrish, "Influence of a positive sodium void coefficient of reactivity on the consequences of transient overpower and loss-of-flow accidents in a medium-sized fast reactor," *Annals of Nuclear Energy*, vol. 19, nr 7, p. 369–375, 1992.
- [14] R. N. Hill, "Fast Reactor Physics and Fuel Cycle," i *Fast Reactor Curriculum Workshop*, Argonne, IL, USA, 2010.
- [15] D. C. Wade, "A simplified analysis of uncertainty propagation in inherently controlled ATWS events," i *Annual NEACRP meeting*, Helsinki, Finland, 1987.
- [16] A. Vasile, "SFR Core Design and Performance," i *IAEA Education and Training Seminar/Workshop on Fast Reactor Science and Technology*, Centro Atómico Bariloche, Argentina, 2012.
- [17] E. A. Hoffman, W. S. Yang och R. N. Hill, "Preliminary Core Design Studies for the Advanced Burner Reactor over a Wide Range of Conversion Ratios," *ANL-AFCI-177*, 2008.
- [18] T. K. Kim, W. S. Yang, C. Grandy och R. N. Hill, "Core design studies for a 1000MWth Advanced Burner Reactor," *Annals of Nuclear Energy*, vol. 36, nr 3, p. 331–336, 2009.
- [19] A. E. Dubberley, K. Yoshida, C. E. Boardman och T. Wu, "SuperPRISM Oxide and Metal Fuel Core Designs," i *Proceedings of ICONE 8, 8th International Conference on Nuclear Engineering*, Baltimore, MD, USA, 2000.
- [20] P. H. Royl, J. E. Cahalan, G. Friedel, G. Kussmaul, J. Moreau, M. Perks och R. A. Wigeland, "Performance of Metal and Oxide Fuel Cores During Accidents in Large Liquid-Metal-Cooled Reactors," *Nuclear Technology*, vol. 97, nr 2, p. 198–211, 1992.
- [21] J. Leppänen, M. Pusa, T. Viitanen, V. Valtavirta och T. Kaltiaisenaho, "The Serpent Monte Carlo code: Status, development and applications in 2013," *Annals of Nuclear Energy*, vol. 82, pp. 142-150, 2015.
- [22] M. B. Chadwick, "ENDF/B-VII. 0: Next generation evaluated nuclear data library for nuclear science and technology," *Nuclear Data Sheets*, vol. 107, nr 12, pp. 2931-3060, 2006.
- [23] S. Qvist och E. Greenspan, "The ADOPT code for automated fast reactor design," *Annals of Nuclear Energy*, vol. 71, pp. 23-36, 2014.
- [24] C. Hellesen och S. A. Qvist, "Validation of the CHD code for transient analysis

of fast reactor systems,” *Submitted to Annals of Nuclear Energy*, 2016.

- [25] ANSI/ANS, ”ANSI/ANS-5.1-2014, Decay Heat Power in Light Water Reactors Current Standard, Revision of ANSI/ANS-5.1-2005,” 2014.
- [26] Argonne National Laboratory, Nuclear Engineering Division, ”The SAS4A/SASSYS-1 Safety Analysis Code System,” ANL/NE-12/4, Argonne National Laboratory, Argonne, IL, USA, 2011.
- [27] B. X. Hu, Y. W. Wu, W. X. Tian, G. H. Su och S. Z. Qiu, ”Development of a transient thermal-hydraulic code for analysis of China Demonstration Fast Reactor,” *Annals of Nuclear Energy*, vol. 55, pp. 302-311, 2013.
- [28] A. K. Agrawal, ”Advanced thermohydraulic simulation code for transients in LMFBRS (SSC-L code),” Brookhaven National Laboratory, Upton, NY, USA, 1978.
- [29] G. Darmet och S. Massara, ”Dynamical analysis of innovative core designs facing unprotected transients with the MAT5-DYN code,” i *Proceedings of ICAPP 2012, paper 12245.*, 2012.
- [30] E. V. Depiante, ”Nuclear reactor dynamic stability analysis using hybrid symbolic-numeric methods,” i *1993 MATLAB Conference*, Cambridge, MA, US, 1993.