**Autonomous Reactivity Control (ARC) —
Principles, Geometry and Design Process**

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ABSTRACT

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. Here we present in detail the principles of how the ARC system operates, what materials should be used, what components make up the system and how they are interconnected. The relevant equations regarding how to design the system for a certain response are developed and defined, and the most important aspects determining the speed of actuation of the systems are analyzed. Thus, this study serves as the general reference material for all of the fundamental principles behind the ARC idea. Finally, we present a step-by-step guide to how a fast reactor fuel subassembly with an ARC system installed would be manufactured, using a full 3D-CAD model. For an ARC installation in a 1000 MWth sodium-cooled oxide-fueled fast reactor core, the system constitutes a relatively minor adjustment to a typical fuel assembly, increasing its total axial extent by ~5-10% and the total primary coolant pressure drop by ~1%. The main finding of this study is that it is possible to design, manufacture (using existing methods) and implement ARC systems in the fuel assemblies of fast reactor cores to provide inherent safety in all anticipated unprotected transients with only a modest increase in the length of the assembly and the pressure drop across the core.

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

1. Introduction

The next generation of nuclear energy systems must be licensed, constructed, and operated in a manner that will provide a competitively priced supply of energy, keeping in consideration an optimum use of natural resources, while addressing nuclear safety, waste, and proliferation resistance, and the public perception concerns of the countries in which those systems are deployed (Generation-IV Forum, 2001). These issues are tightly interconnected, and the implementation of passive and *inherent* safety features is a high priority in all modern reactor designs since it helps to tackle many of the issues at once. To this end, the Autonomous Reactivity Control (ARC) system was developed to ensure inherent safety performance of Generation-IV reactors even if they feature relatively large positive coolant density reactivity feedback, while having a minimal impact on reactor performance and economic viability (Qvist, Safety and core design of large liquid-metal cooled fast breeder reactors, 2013) (Qvist & Greenspan, An Autonomous Reactivity Control System for Improved Fast Reactor Safety, 2014).

This paper gives a comprehensive overview of many of the most important aspects of the ARC systems as well an example implementation. We define the general principles of how the system works, material choices, the geometries of all components and how to use these principles to automate the design process. Upcoming studies are focused on analysis methods and principles for tailoring the ARC system response to promote stability and provide adequate safety margins in any type of accident scenario. Finally, the results and methods of the first studies will be combined to design, implement and analyze full-detail ARC installations in the fuel assemblies of existing reference fast reactor cores. This study is meant to serve as the general reference material for the fundamental principles behind the ARC idea.

The motivation and inspiration for the development of ARC systems have been covered in earlier publications (Qvist, Safety and core design of large liquid-metal cooled fast breeder reactors, 2013) (Qvist & Greenspan, Inherent Safety of Breed and Burn Reactors, 2012) (Qvist & Greenspan, The Autonomous Reactivity Control System, 2014) (Qvist & Greenspan, An Autonomous Reactivity Control System for Improved Fast Reactor Safety, 2014), and will just be briefly introduced here.

The ARC system in its standard configuration 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 (henceforth the “expansion” liquid), while the lower reservoir contains the same expansion liquid and, floating on top of it, a separate immiscible liquid (henceforth the “absorber” liquid). The remaining free volume between the two concentric tubes in the closed system is filled with an inert gas. 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 fuel assembly with an ARC-installation is shown in Figure 1. The components relating to the ARC (upper and lower reservoir and tubes) are in approximate scale to one another, while the midsections of the assembly (that contains the fuel bundle) have all been reduced in axial length by about a factor of 10 compared to the ARC-components in the figure for greater clarity. The design of the components of the ARC system will be covered in detail in the following section.

Figure 1, An ARC-equipped fuel assembly (not to scale)

To complement the many highly simplified drawings of this paper, a full-detail 3D CAD model of an ARC-equipped fuel assembly has been developed. Figure 2 shows both the top and bottom part of the assembly straight from the side, while Figure 3 and Figure 4 shows an angled view of the top and bottom parts respectively. Most of the more intricate mechanical components of the assembly (grid mounting rails, stabilization rods, wire-wraps, shielding etc.) have been omitted from the models here for clarity.

Figure 2, CAD model of an ARC-equipped fuel assembly (seen from the side)

Figure 3, CAD model of the top of the ARC equipped fuel assembly

Figure 4, CAD model of the bottom part of the ARC-equipped fuel assembly

During an accident/transient scenario in the reactor, the ARC-system responds in the following way, starting from a standard operating condition:

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 in 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 into) 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.

A schematic view of how the liquid levels change in the lower ARC reservoir as the temperature increases is shown in Figure 5.

A = Shutdown
B = Standard operation
C = High temperature transient

Figure 5, Schematic view of the lower ARC reservoir at different states (color code identical to that of **Figure 1**)

Properly designed, the ARC-system can act as a thermostat in the core, autonomously controlling temperature without the need for any operator action, electrical systems or any moving mechanical parts. This actuation responds to temperature and relies solely on the laws of physics, and is therefore an inherent feedback mechanism (akin to the core thermal expansion feedback).

2. ARC-system component design principles

2.1. The upper liquid reservoir and the ARC-tubes

The upper reservoir of the ARC-system provides the main driving force for the actuation. Its design is so tightly linked to that of the ARC-tubes that they cannot be treated separately. Therefore, the designs of both are covered in this subsection. The upper reservoir consists of two compartments, separated by a steel plate. The lower compartment (or section) is a gas-filled volume that connects to the outer ARC-tube, while the upper section is a liquid-filled volume that connects to the inner ARC-tube. The gas plenum in the lower section of the upper reservoir helps to keep the pressure low while the gas is compressed from below by the expansion of the ARC liquids. The parameters of the upper reservoir design are shown in Figure 6.

Figure 6, Geometry of the upper ARC reservoir (not to scale)

The inside-volume of the compartment that contains the expansion liquid (Section 2 in the figure), is given by:

$$V_{UR2} = L_{UR2} \left[\frac{\sqrt{3}}{2} FTF_i^2 - \left(\frac{D_{COT}}{2} \right)^2 \pi \right] - V_{URis} \quad (1)$$

where FTF_i is the inner flat-to-flat distance of the hexagonal fuel assembly ducts, D_{COT} is the outer diameter of the cylindrical coolant outlet tube, and V_{URis} is the volume taken up by the steel of the inner ARC tube. The value of V_{URis} is typically so small that it can be ignored completely in calculations without loss of accuracy.

The total volume of the liquid in the upper reservoir is determined by the requirements for reactivity worth and temperature-span of actuation of the system. The first step in the design process is to calculate the worth of the absorber liquid in the core, as measured in reactivity per volume fraction of absorber in the active core.* Assuming that we implement the same type of ARC-systems in all fuel assemblies of the core, this worth can be expressed by a single value, defined as:

$$\Delta k_{ARCA} = \frac{(k_{ARC} - k_{ref})}{\left(\frac{V_{ARC}}{V_{core}}\right)} \left[\frac{\text{reactivity deviation}}{\text{vol. frac absorber in core}} \right] \quad (2)$$

where k_{ARC} is the effective multiplication factor of the core with the ARC-tubes at full actuation (the absorber liquid inside the ARC-tubes covers the entire extrapolated axial extent of the core), k_{ref} is the reference value for the multiplication factor and V_{ARC}/V_{core} is the volume fraction of the core that consists of absorber liquid when k_{ARC} is calculated. The value of Δk_{ARCA} is used to define the impact on core reactivity by introducing the liquid absorber into the core. The unit of Δk_{ARCA} is either pcm or \$. The required volume-fraction of available ARC absorber space inside the active core is defined as:

$$V_f = \frac{\Delta k_{ARCA}}{\Delta k_{requirement}} \quad (3)$$

where $\Delta k_{requirement}$ is the total reactivity that any specific ARC-system must be able to supply at full actuation. If for example Δk_{ARCA} is equal to -4% (4000 pcm) per percent absorber volume fraction in the core, the effective delayed neutron fraction is 380 pcm (=1\$), and the assumed requirement for the ARC-system is -1\$ of reactivity,

* This approach works well for core-wide ARC implementations in fast reactors, where the long mean free path means that local heterogeneities are of small importance. If ARC-systems are installed in specific locations in the core (not in each fuel assembly) or if the system is operating in a softer spectrum than that of typical fast reactors, more sophisticated calculations of reactivity worth is required.

the total available ARC absorber volume fraction within the core must be equal to $V_f = 380/4000 \sim 0.1\%$.

When the value of V_f is set, it is possible to begin designing the ARC-tubes and reservoirs accordingly. V_f relates to the radius of the inner and outer ARC-tubes as:

$$V_f = \frac{V_{ARC}}{V_{assembly}} = \frac{N_{ARC}\pi(R_{oARCi}^2 - R_{iARCo}^2)}{\left(\frac{\sqrt{3} \times FTF_o^2}{2}\right)} \quad (4)$$

where R_{oARCi} is the inner radius of the outer ARC tube, R_{iARCo} is the outer radius of the inner ARC tube in the active core region and FTF_o is the outer flat-to-flat distance of the entire fuel assembly (including half the inter-assembly space) and N_{ARC} is the number of equal-worth ARC-tubes installed in each assembly.

An obvious and simple choice for the outer ARC-tube is to design it to be identical to the fuel rod tubes in the assembly, creating a homogenous lattice that from the outside looks identical to a conventional fuel assembly. While the ARC-tubes could for instance be incorporated in the duct walls or occupy a corner of the assembly with a customized design (with the corner fuel rod removed), such design alterations may render the wealth of empirical data on typical assembly geometries invalid for the new assembly design. Initial studies indicate that relatively little can be gained in terms of performance by highly customized fuel assembly designs. Assuming the fuel rod outer cladding radius (R_{fco}) and the cladding thickness (t_c) are known and the thicknesses of the ARC-tubes are identical to that of the fuel rod cladding, the only remaining unknowns in eq. (4) are R_{iARCo} and N_{ARC} . The outer ARC-tube geometry is then defined as:

$$R_{oARCo} = R_{fco} \quad (5)$$

$$R_{oARCi} = R_{oARCo} - t_c = R_{fco} - t_c \quad (6)$$

The outer radius of the inner ARC tube in the active core region can be calculated by solving eq. (4) as:

$$R_{iARCo} = \sqrt{R_{oARCi}^2 - V_f \left(\frac{\frac{\sqrt{3}}{2} FTF_o^2}{\pi N_{ARC}} \right)} \quad (7)$$

If the second term under the square root in eq. (7) is larger than the first, the complex resulting value for R_{iARC0} indicates an impossible design combination. In such cases, either the value of N_{ARC} must be increased, the value of V_f decreased or a combination of both. The inner radius of the inner ARC-tube in the active core region is simply defined as:

$$R_{iARCi} = R_{iARCo} - t_c \quad (8)$$

The volume available for absorber liquid introduction in the active core for the given ARC system (corresponding to V_f) can be described in absolute terms as:

$$V_{ARC} = N_{ARC} L_c \pi * (R_{oARCi}^2 - R_{iARCo}^2) \quad (9)$$

where L_c is the axial length of the extrapolated core.

By definition, the expansion of the liquid in the upper ARC-reservoir must bring the upper surface of the absorber liquid from the bottom to the top of the extrapolated core over a set temperature increase, defined as ΔT_f . This means that the volumetric expansion of the liquid initially present (before actuation) in the upper reservoir driven over the temperature span ΔT_f must equal V_{ARC} . The total required amount of liquid volume in the upper reservoir can then be estimated as:

$$V_{UR2} = A * B * V_{ARC} + \frac{B * V_{ARC}}{2} \quad (10)$$

where the parameter A is the inverse of the relative volume expansion (i.e. density reduction) of the expansion liquid between the “at-core temperature” (T_{ac}) and the full actuation temperature (T_f):

$$A = \left(\frac{1}{\frac{\rho_E(T_{ac})}{\rho_E(T_f)} - 1} \right) \quad (11)$$

The parameter B accounts for the difference in liquid volume as the expansion liquid thermally contracts (due to heat transfer) between the temperature T_{mf} , defined as the temperature in the upper reservoir at “middle-of-actuation”, and the coolant inlet temperature T_i , when moving from the upper to the lower reservoir:

$$B = \left[\frac{\rho_E(T_i)}{\rho_E(T_{mf})} \right] \quad (12)$$

The first term of eq. (10), $(A * B * V_{ARC})$, describes the volume of liquid that needs to thermally expand between temperatures T_{ac} and T_f in order to get a total expansion volume V_{ARC} , measured at the coolant inlet temperature (T_i). The second term is added to make up for the liquid that leaves the upper reservoir during the expansion process and thus can no longer contribute to the continued expansion.

This calculation strategy assumes that the hot expansion liquid that has left the upper reservoir immediately cools down to the lower reservoir temperature (with associated thermal contraction). In reality, the total volume that the expansion liquid occupies will be larger than the value calculated by this method, making it a conservative simplifying assumption. This assumption is useful to guide the initial stages of the *design* of the system, but a detailed transient *analysis* involving an ARC-system may require a more realistic model. Eq. (10) is based on the following assumed behavior of the system:

1. The expansion liquid inside the upper section of the reservoir heats up.
2. A fraction of the expansion liquid is expelled out of the upper reservoir, through the inner ARC-tube and finally into the lower reservoir.
3. The volume of expansion liquid corresponding to the *mass* of expansion liquid that has left the upper reservoir is calculated at the lower reservoir temperature.

The axial length of the liquid-filled section of the upper ARC reservoir is given by:

$$L_{UR2} = \frac{V_{UR2}}{A_{UR2}} = \frac{V_{UR2}}{\frac{\sqrt{3}}{2} F T F_i^2 - \left(\frac{D_{COT}}{2} \right)^2 \pi} \quad (13)$$

In addition to the criteria that the absorber should traverse the active core during a temperature increase of ΔT_f , the system must also simultaneously be designed to bring the absorber liquid from the lower reservoir up to the bottom of the active core during a set temperature increase in the upper ARC reservoir, defined as ΔT_{ac} .

With the in-core ARC-tube geometry and upper reservoir volume defined, the expansion volume between the ARC-tubes below the core must be adjusted to achieve

the desired response. This can be done either by tailoring the temperature spans of actuation, increasing the outer diameter of the inner ARC-tube in the below-core region, decreasing the inner diameter of the outer ARC-tube, or by introducing some form of blockage/extrusion between the tubes in this region.

The desired volume inside the annular region between the ARC-tubes in the below core region is defined as:

$$V_{bc} = V_{UR2} \times \frac{\left(\frac{\rho_E(T_{ac})}{\rho_E(T_o)} - 1 \right)}{\left(\frac{\rho_E(T_i)}{\rho_E(T_{mac})} \right)} \quad (14)$$

where T_{mac} is the temperature in the upper reservoir when the absorber is halfway to the bottom of the active core ($T_{mac} = [T_{ac} + T_o]/2$). The simplest possible way to design an ARC system is to follow the relation:

$$\frac{L_c}{L_{bc}} = \frac{\rho_E(T_o) - \rho_E(T_{ac})}{\rho_E(T_{ac}) - \rho_E(T_f)} \quad (15)$$

where L_{bc} is the axial distance between the bottom of the ARC-tube annulus (located in the lower reservoir) and the bottom of the active core region. Since the density of expansion liquids, such as potassium, changes approximately linearly with temperature, eq. (15) can typically be simplified as:

$$\Delta T_f \times L_{bc} = \Delta T_{ac} \times L_c \quad (16)$$

Thus, if the axial length of the active core and the distance between the bottom of the active core and the lower reservoir are approximately equal, the temperature span of actuation in the core (ΔT_f) and the temperature-rise for the absorber to reach the core (ΔT_{ac}) should also be approximately equal.

For designs following eq. (16), the inner ARC tube is of a uniform diameter and there is no need for any flow blockages or complications of any type in the below-core

region. However, in many cases, there is a requirement to rapidly reach the core (small ΔT_{ac}), while the span of actuation can be as large as 150°C (which minimizes ARC reservoir volumes and lengths). Such actuation characteristics could be achieved by an adjustment of the inner ARC-tube outer radius in the below-core region. For such designs, the ARC tube radius below the core (R_{iARC0B}) becomes:

$$R_{iARC0B} = \sqrt{R_{oARCi}^2 - \frac{V_{bc}}{\pi L_{bc}}} \quad (17)$$

If the value of $\frac{V_{bc}}{\pi L_{bc}}$ is small compared to R_{oARCi}^2 , the manufacture and installation of the system may be difficult due to the very small distance between the tubes in the below-core region. In addition, at very small distances between the pipes, severe capillary action may complicate the analysis of system operation.

The most promising design implementation when one requires the absorber to reach to core rather quickly (low ΔT_{ac}) from a lower reservoir quite far away from the core (high L_{bc}) is to implement a number of wire-wrap spacers around the inner ARC-tube in the below-core region. The wire-spacers fulfill the dual task of spacing the tubes (to avoid wear) and reducing the available volume per unit length. When using wire-wraps, the ARC-tube annulus width in the below core region can be larger than otherwise (since the wires occupy area and volume), but there may still be a need for a smaller annulus than in the in-core region. The area relation in the tube-annulus below the core is given by:

$$A_{bc} = \frac{V_{bc}}{L_{bc}} = \pi(R_{oARCi}^2 - [R_{iARCo} + x_r]^2) - A_w \quad (18)$$

where x_r is the increase to the inner ARC tube outer radius (compared to the in core region) and A_w is the axially averaged area fraction of the wire. Using the same notation as in eq. (17): $R_{iARC0B} = R_{iARCo} + x_r$. The wire diameter is defined (at operating temperature) to be exactly equal to the distance between the ARC-tubes:

$$D_w = R_{oARCi} - R_{iARCoB} \quad (19)$$

The helix-diameter of the wires wrapped around the inner ARC-tube is:

$$D_{hxd} = D_{iARCoB} + D_w \quad (20)$$

The area taken up by the wires is given by:

$$A_w = \frac{N_w \pi D_w^2}{4} \times \sqrt{\frac{\pi^2 L_{bc}^2}{\left(\frac{H_w}{D_{hxd}}\right)^2} + L_{bc}^2} \quad (21)$$

where N_w is the number of cylindrical wires and H_w is the axial height of one complete wire rotation around the inner ARC-tube. The geometric components described in this section are defined in *Figure 7*.

Figure 7, Geometry of the upper ARC-reservoir and ARC-tubes

2.2. The lower ARC reservoir and the absorber liquid

The lower reservoir is “created” by extending the nosepiece tube (here called the coolant inlet tube), a set distance into the hexagonal part of the fuel assembly structure. The lower reservoir is the closed volume created between the hexagonal

assembly inner walls and the inlet tube outer wall. The main function of the lower reservoir is to accommodate the volume changes in the ARC system liquids between shutdown (when the “liquids” may in fact be solids) and operation temperature. It should be noted that in some fuel assembly designs, this could be accommodated within the ARC-tubes themselves, thus precluding the need for a lower reservoir. The lower reservoir design approach as presented here is more general, since it is applicable regardless of the specifics of the fuel assembly design.

Like the upper reservoir, the geometry of the lower reservoir is divided in two sections as shown in Figure 8. The upper part of the lower reservoir, inside which the coolant inlet tube transitions from a circular shape out to the larger hexagonal shape that confines the fuel assembly bundle, contains a neutron shielding material (absorber or reflector) that protects the absorber liquid from excessive depletion during operation.

Figure 8, Geometry of the lower ARC reservoir

The volume of the lower section of the reservoir that is filled with liquid and/or gas is:

$$V_{LR1} = L_{LR1} \left[\frac{\sqrt{3}}{2} FTF_i^2 - \pi \left(\frac{D_{CIT}}{2} \right)^2 \right] - V_{LRis} \quad (22)$$

where D_{CIT} is the outer diameter of the coolant inlet tube and V_{LRis} is the volume taken up by the steel of the inner ARC tube inside this section. Again, the value of V_{LRis} is so small compared to the other volumes that it can effectively be ignored.

The total volume of absorber liquid in the system must be large enough to cover the entire extrapolated core at full actuation (V_{ARC}). However, additional absorber volume is required since otherwise the absorber liquid would start to leave the bottom-most part of the core if temperatures during a transient go above T_f .

To make sure the system cannot reduce its negative reactivity worth up until some set maximum system temperature (T_{max}), additional absorber volume is added that corresponds to the volume added by the upper reservoir expansion liquid between full actuation (T_f) and the set maximum temperature (T_{max}). Finally, an additional margin is added on top of that to account for uncertainties, expansion of liquids already present in the lower reservoir and the depletion of absorber liquid during the operational cycle (which should be kept at a minimum by shielding design). The required absorber liquid volume at full actuation then becomes:

$$V_{\text{Af}} = V_{\text{ARC}} + V_{\text{UR2}} \left[\frac{\rho_E(T_f)}{\rho_E(T_{\text{max}})} - 1 \right] + V_{\text{margin}} \quad (23)$$

Note that for conservatism in eq. (23), we have not taken into account the volume reduction by cooling of the liquid that leaves the upper reservoir (as was done in eq. (10)). Eq. (22) ensures that a certain fraction of the total absorber liquid volume remains within the lower reservoir and below-core ARC-tubes even at full actuation. We assume that in the early stages of most accident scenarios, the coolant inlet temperature remains unchanged while the coolant outlet temperature increases.[†] The volumetric average temperature of the absorber liquid at the state of full actuation is therefore:

$$T_{\text{Af}} = \frac{T_{\text{caf}} \times V_{\text{ARC}} + T_i \times \left[V_{\text{UR2}} \left[\frac{\rho_E(T_f)}{\rho_E(T_{\text{max}})} - 1 \right] + V_{\text{margin}} \right]}{V_{\text{A}}(\text{full actuation})} \quad (24)$$

[†] This is true of loss of flow (LOF) and transient overpower (TOP) scenarios, but is not accurate in loss of heat sink (LOHS) events, where in fact the transient initiates with a rise in coolant inlet temperature. However, since the LOHS event slowly and monotonically transitions toward its asymptotic state, its behavior early on in a transient is of lesser importance.

where T_{caf} is the average coolant temperature in the core at the full actuation state: $T_{\text{caf}} = (T_f + T_i) / 2$. From equations (23) and (24), it is possible to determine the required mass loading of ARC-liquid per reservoir as:

$$m_A = \rho_A(T_{\text{Af}}) \times V_{\text{Af}} \quad (25)$$

It is the volume increase in the liquids of the ARC-system going from cold shutdown state up to standard operating temperatures that determine how large the volume available in the lower section of the lower reservoir needs to be. The lower reservoir must accommodate all liquid expansion so that the top level of the absorber remains at a predetermined distance from the active core (to avoid excessive depletion) during standard operation. The absorber liquid must also at all times be kept at some distance above the lower “mouth” (opening) of the inner ARC-tube inside the lower reservoir, to avoid expelling absorber liquid up through the inner ARC tube. We define the minimum level (measured from the bottom of the lower reservoir) of the interface between the expander and absorber liquids by the parameter L_0 . A reasonable value for L_0 in most assembly designs is ~3-5 cm. The volume of expander liquid that is *always* present in the bottom of the lower reservoir (up to level L_0) is therefore:

$$V_{L0}(T_{\text{rt}}) = L_0 \times A_{\text{LR1}} \quad (26)$$

where T_{rt} is room temperature (~20°C) and A_{LR1} is the area inside the reservoir in the lower section, defined as:

$$A_{\text{LR1}} = \frac{\sqrt{3}}{2} F T F_i^2 - \pi \left(\frac{D_{\text{CIT}}}{2} \right)^2 \quad (27)$$

Between room temperature and operating temperature, this material melts and expands, filling a larger part of the lower reservoir. This volumetric expansion is given by:

$$\Delta V_{L0} = V_{L0}(T_0) - V_{L0}(T_{\text{rt}}) = V_{L0}(T_{\text{rt}}) \times \left(\frac{\rho_E(T_{\text{rt}})}{\rho_E(T_i)} - 1 \right) \quad (28)$$

The absorber expansion from room temperature to operating temperature is:

$$\Delta V_{A0} = V_A(T_i) - V_A(T_{rt}) = m_A \left(\frac{1}{\rho_A(T_i)} - \frac{1}{\rho_A(T_{rt})} \right) \quad (29)$$

The expansion material present in the inner ARC tubes also expands going from room temperature to operating temperature, expelling some liquid volume into the lower reservoir. The total volume of liquid in the inner ARC-tube is:

$$V_{iARC} = \pi L_{iARC} R_{iARC}^2 \quad (30)$$

where L_{iARC} is the total length of the inner ARC-tube, which is equal to the axial distance between the bottom of the upper reservoir and the lowermost part of the inner ARC tube (a distance L_0 from the bottom of the lower reservoir). Since the expansion of liquids into the lower reservoir determines its size, it is not possible to accurately estimate L_{iARC} without including the axial length of the lower reservoir in an iterative solver. In conventional fast reactor fuel assembly geometries, simply providing a reasonable guess for the axial length of the lower reservoir (~ 10 cm) is sufficient, due to the negligibly small amount of liquid that is present in the inner ARC-tube in the lower reservoir. The mass of liquid present in the inner ARC tube at room temperature is:

$$m_{iARC} = V_{iARC} \times \rho_E(T_{rt}) \quad (31)$$

The volumetrically averaged temperature of the liquid present in the inner ARC-tube at the operational state is:

$$T_{iARCO} = \frac{T_i \times L_b + T_{caf} \times L_c + T_o \times L_t}{L_{iARC}} \quad (32)$$

where L_t is the axial distance from the top of the active core to the bottom of the upper reservoir, and L_b is the axial distance from the bottom of the inner ARC tube to the bottom of the active core ($L_t = L_{iARC} - L_c - L_b$). The expansion of the liquid inside the inner ARC tube, going from room temperature to operating temperature is:

$$\Delta V_{iARC0} = m_{iARC} \left(\frac{1}{\rho_E(T_{iARCO})} - \frac{1}{\rho_E(T_{rt})} \right) \quad (33)$$

Finally, (and most importantly) a significant amount of the material in the upper ARC-reservoir also expands into the lower reservoir when going from room

temperature to operating temperature. The mass of material present in the upper reservoir at room temperature is given by:

$$m_{UR2rt} = V_{UR2} \times \rho_E(T_{rt}) \quad (34)$$

The volume of material expanding out of the upper reservoir into the lower reservoir is:

$$\Delta V_{UR20} = m_{UR2rt} \left(\frac{1}{\rho_E(T_o)} - \frac{1}{\rho_E(T_{rt})} \right) \quad (35)$$

In total, the lower reservoir has to accommodate an expansion of:

$$\Delta V_{RT \rightarrow O} = \Delta V_{UR20} + \Delta V_{iARC0} + \Delta V_{A0} + \Delta V_{L0} \quad (36)$$

The different contributions to the expansion (and corresponding volume requirement in the lower reservoir) are shown in Figure 9.

Figure 9, Expansions from room temperature to operating temperature (not to scale)

Section 1 of the lower reservoir must have the available volume:

$$V_{LR1} = V_{L0}(T_{rt}) + \Delta V_{RT \rightarrow O} \quad (37)$$

The corresponding axial length of the section is:

$$L_{LR1} = \frac{V_{LR1}}{A_{LR1}} \quad (38)$$

The geometry of the second section (of length L_{LR2}) is determined primarily by the need for additional shielding of the absorber liquid during standard operation, as well as the pressure-drop caused by the expansion of the coolant-carrying inlet tube, which in this section expands from a circular shape to a larger hexagonal shape. The total axial extent of the lower reservoir is simply the sum of the length of the two sections:

$$L_{LR} = L_{LR1} + L_{LR2} \quad (39)$$

2.3. ARC-system gas

In order for the system to autonomously re-equilibrate after a temperature excursion, the pressure of the gas in the ARC-tube must equal or exceed the hydrostatic pressure of the expansion liquid. The hydrostatic pressure of the expansion liquid is calculated as:

$$p_{hyd} = \Delta h_{es} \times \rho_E(T_s) \times g + \Delta h_{as} \times \rho_A(T_s) \times g \quad (40)$$

where Δh_{es} is the axial distance between the top of the expansion liquid in the lower reservoir at shutdown/refueling temperature, $\rho_E(T_s)$ is the density of the expansion liquid at shutdown/refueling and g is the gravitational constant. Δh_{as} is the axial height of the absorber in the lower reservoir at shutdown/refueling temperature and $\rho_A(T_s)$ is the corresponding density.

Figure 10, Hydrostatic pressures in the ARC-system

The simple requirement on the gas loading pressure in the ARC-tube (p_{gas}) at room temperature is therefore:

$$p_g(T_{rt}) = p_{hyd} + p_{margin} \quad (41)$$

where p_{margin} is an additional pressure added to account for uncertainties. As the ARC system is actuated, the gas is compacted and the pressure may increase considerably. The sealed lowermost section of the upper reservoir is connected to the volume between the ARC tubes in order to reduce the relative compaction of the gas when the system is actuated. If the gas were to be confined to the ARC-tube annulus alone, a strong actuation and expansion of the liquids could cause unacceptably high stress in the ARC-tubes. The total volume of ARC gas at room temperature is given by:

$$V_g(T_{rt}) = [V_{LR1} - V_{L0}(T_{rt})] + V_{bc} + V_{ARC} + V_t + V_{UR1} \quad (42)$$

where V_{UR1} is given by:

$$V_{UR1} = L_{UR1} \frac{\sqrt{3}}{2} FTF^2 - \frac{\pi L_{UR1}}{3} \left[\left(\frac{D_{COT}}{2} \right)^2 + \left(\frac{D_{COT}}{2} \right) \left(\frac{D_{eq}}{2} \right) + \left(\frac{D_{eq}}{2} \right)^2 \right] \quad (43)$$

D_{eq} is the equivalent “radius” of the inner duct hexagon, treated here as if it were one of the axial surfaces of a circular frustrum. Setting $D_{eq} = FTF_i$ gives a reasonable approximation for the resulting volume (V_{UR1}), but an exact estimation of the value of V_{UR1} requires a definition the specific geometry of the connecting piece that transitions from a circular to hexagonal shape. V_t is the volume between the ARC-tubes in the above-core region, defined as:

$$V_t = L_t \pi (R_{oARCi}^2 - R_{iARCo}^2) \quad (44)$$

The gas volume at full ARC-system actuation is:

$$V_g(T_f) = V_{UR1} + V_t \quad (45)$$

The ARC gas compression factor (G_c) is the ratio between the gas volume at room temperature and the gas volume at full system actuation.

$$G_c = \frac{V_g(T_{rt})}{V_g(T_f)} \quad (46)$$

Thus, the gas pressure (treated as an ideal gas) at full ARC-system actuation from compression and temperature increase is:

$$p_g(T_f) = p_g(T_{rt}) \times G_c \times \left(\frac{T_f}{T_{rt}}\right) \quad (47)$$

Depending on the type of absorber liquid used, there may be additional pressure added by gas production from reactions in the absorber. The reaction rate in the absorber liquid during standard operation is given by:

$$R_A(t) = X_g \phi \sigma_a^A N_0^{-\phi \sigma_a^A t} \left[\frac{\text{Reactions}}{s} \right] \quad (48)$$

where ϕ is volume-averaged flux in the absorber liquid at the radial location of peak flux, σ_a^A is the effective volume-averaged one-group capture cross-section of the absorber, X_g is the fraction of absorption reactions that produce gaseous reaction products, and the initial absorber atomic density is:

$$N_0 = \frac{V_A \rho_A N_{Av}}{M_a} [\text{Atoms}] \quad (49)$$

where V_A is the volume of absorber, ρ_A is the absorber density, M_a is the atomic mass and N_{Av} is Avogadro's constant. The number of reactions producing gas from the absorber liquid during the time t becomes:

$$D_A = \int_0^t R_A(t) dt = X_g N_0 (1 - e^{-\phi \sigma_a^A t}) \quad (50)$$

where t is the residence time of the assembly containing an ARC system. D_A can be converted to the number of gas moles produced depending on what type of reaction is occurring and what type of gas is the end product. For the use of lithium as absorber liquid, the capture reaction in ${}^6\text{Li}$ gives:

Given eq. (50) and (51), it is possible to calculate the number of moles of added gas, and the resulting increase in system pressure at end-of-life (EOL) as compared to beginning-of-life (BOL). When using lithium, some fraction of the tritium produced would form solid lithium hydride (LiT), reducing the amount of gas released. In order to keep the analysis general, we simply denote the added pressure by absorber

depletion as p_{dep} . In order to keep the peak gas pressure below a set value (p_{max}) in the ARC-tubes, the volume compression factor must be bounded as:

$$G_c \leq \frac{p_{max}}{p_g(T_{rt}) \times \left(\frac{T_f}{T_{rt}}\right)} \quad (52)$$

Eq. (52) implies that the required volume of the upper gas reservoir for the ARC system (V_{UR1}) is:

$$V_{UR1} \geq \frac{p_g(T_{rt}) \times T_f \times [V_{BC} + V_{ARC} + V_t - V_{LR1} - V_{L0}(T_{rt})]}{p_g(T_{rt}) \times T_f - p_{max} \times T_{rt}} \quad (53)$$

3. ARC impact on core pressure drop

The addition of an ARC installation will have an impact on the pressure drop across the core. We investigated this using correlations presented in the Handbook of Thermal Hydraulic Resistance (Idelchik, 1987). Firstly, the ARC installation adds to the pressure drop of the assembly at the inlet due to the presence of the lower reservoir as shown in Figure 11.

Figure 11, Assembly foot of ARC'ed and standard fuel assemblies

The pressure drop added by the extra flow path length through the longer inlet tube of the ARC'ed assembly can be estimated as:

$$\Delta p = \lambda \frac{l}{D_h} \frac{\rho v^2}{2} \quad (54)$$

where D_h is the diameter of the tube, l is the additional length of tube through the lower reservoir and the smooth wall friction coefficient, λ , is given by:

$$\lambda = \frac{1}{(1.8 \log_{10}[\text{Re}] - 1.64)^2} \quad (55)$$

Pressure drop is also added in the same way through the straight part of the “coolant outlet tube” that is lengthened by the upper reservoir. In addition the coolant flow path contracts from a hexagonal shape at the end of the rod bundle to the smaller-diameter outlet tube, as shown in Figure 12.

Figure 12, Top portion of ARC'd and standard fuel assemblies

For the pressure drop due to the contraction we use:

$$\frac{\Delta p}{\left(\frac{\rho v_0^2}{2}\right)} = \zeta$$

$$= (-0.0125 n_0^4 + 0.0224 n_0^3 - 0.00723 n_0^2 + 0.00444 n_0 - 0.00745)(\alpha_r^3 - 2\pi\alpha_r^2 - 10\alpha_r) + \zeta_{fr} \quad (56)$$

where $n_0 = A_0/A_1$ is ratio of the final cross-sectional area to the initial one and $\alpha_r = 0.01745 \alpha$ where α is the half-angle of the opening of the contraction. The pressure drop then depends on the velocity after the contraction, v_0 , and the density of the fluid, ρ . We assume that the hexagonal inlet cross section can be approximated by a circle of the same hydraulic diameter and that the influence of the shape is minimal. The contribution to the pressure drop by friction, ζ_{fr} , is given by

$$\zeta_{fr} = \frac{\lambda}{8 \sin\left(\frac{\alpha}{2}\right)} \left(1 - \frac{1}{n_0^2}\right) \quad (57)$$

While values vary with the specifics of the system, scoping studies indicate that the pressure drop added by an ARC installation is typically significantly below 1% of the total primary cycle pressure drop. In the example given in Section 6, the added primary cycle pressure is in fact only ~0.2%. Due to the increased pressure drop and the direct reduction of the core fuel volume fraction by the removal of one or more fuel rods per assembly, the ARC installation may require either slightly more pumping power or a slightly more open fuel rod lattice.

4. Actuation time delay

4.1. Heat transfer to the ARC reservoir

Due to thermal inertia, the volume-averaged temperature of the liquid in the upper reservoir (which drives the ARC actuation) will lag the coolant outlet temperature. To accurately estimate this lag, detailed computational fluid dynamics simulations are required, and such studies are on going. While simple analytical studies may be of limited value for actual analysis, they can be beneficial for the understanding of the system and what factors are important in influencing time delays and heat transfer. A simple model to estimate the time delays between coolant temperature and the temperature of the ARC reservoir was developed and is briefly presented here. The problem to solve is described in Figure 13.

Figure 13, Heat transfer to and from the upper ARC reservoir

A state-space system of constant-coefficient differential equations was set up to model the volume-averaged temperature in the reservoir as a function of coolant

outlet temperature. The complicated shape of the liquid-filled reservoir was simplified in the model and represented by an equal-volume cylindrical model. The radial heat flow for a model with 3 nodes in the duct and outlet tube and five nodes in the reservoir is shown in Figure 14. The direction of the arrows in the figure is arbitrary.

Figure 14, Example of radial nodes in 11-node configuration

In the model that was constructed, the following distinct type of heat transfers are included:

1. Convection from flowing coolant and conduction into the middle of the innermost outlet tube node
2. Conduction inside the outlet tube (with an arbitrary number of nodes)
3. Conduction between middle of the outermost outlet tube node and the middle of the innermost reservoir node
4. Conduction inside the reservoir (with an arbitrary number of nodes)
5. Conduction between middle of the outermost reservoir node and the middle of the innermost duct wall node
6. Convection from flowing bypass (inter-assembly) coolant and conduction into the middle of the outermost duct node

The convective heat transfer coefficient h_c , from the flowing bulk coolant to the inner wall of the coolant outlet tube can be calculated from a Nusselt number correlation for liquid metal flow through a circular pipe. Assuming that turbulent flow has fully developed within the tube and that the coolant temperature is constant, the *Notter and Sleicher (1972)* (Notter & Sleicher, 1972) correlation for turbulent flow in a circular pipe can be used[‡]:

$$Nu_T = 4.8 + 0.0156Pe^{0.85}Pr^{0.08} \quad (58)$$

[‡] This correlation is recommended for use in the very comprehensive review of the OECD/NEA liquid metal handbook (OECD/NEA, 2007)

where Pe and Pr is the coolant Peclet and Prandtl number respectively. Eq. (58) is reported to have an accuracy of $\pm 5\%$ in the range of $0.004 < Pr < 0.1$, and $10^4 < Re < 10^6$. The Reynolds number in the coolant is:

$$Re = \frac{\rho v D_{COT}}{\mu} \quad (59)$$

where ρ is the coolant density, v the velocity (inside the coolant outlet tube) and μ the coolant kinematic viscosity. The Prandtl number is defined as:

$$Pr = \frac{c_p \mu}{k} \quad (60)$$

where k is the coolant thermal conductivity. Finally, the Peclet number is defined as the product of the Reynolds and Prandtl number:

$$Pe = Re * Pr \quad (61)$$

If very high Reynolds numbers are present, the *Chen and Chiou (1982)* (Chen & Chiou, 1981) correlation could potentially be used for values of $10^6 < Re < 5 \times 10^6$:

$$Nu_T = 4.5 + 0.0156 Pe^{0.85} Pr^{0.86} \quad (62)$$

The *Chen & Chiou* and *Notter & Sleicher* correlations do not, however, agree well within the range that they are both reported to be valid ($10^4 < Re < 10^6$), and the difference for sodium at 500°C is 25% at $Re=10^6$. Due to the larger range of validity, the *Chen and Chiou* correlation was used as a default in the code developed for this analysis, but the user has the option of switching to the *Notter & Sleicher* correlation.

In certain conditions (such as far in to a loss-of-flow transient), there may be a need to analyze the heat transfer to the ARC reservoir for laminar flow conditions. The Nusselt number of fully developed laminar flow in a circular duct is equal to (OECD/NEA, 2007):

$$Nu_L = 3.6568 \quad (63)$$

The heat transfer coefficient is calculated as:

$$h_c = \frac{Nu \times k}{D_{COT}} \quad (64)$$

The heat transfer from the flowing coolant to the middle of the innermost outlet tube node is then defined as:

$$H_1 = \frac{A_1}{\frac{1}{h_c} + \frac{(w/2)}{k_{HT9}}} \quad (65)$$

where A_1 is the heat transfer area (m^2), w the radial thickness of the nodes inside the outlet tube (m) and k_{HT9} the conductivity of the outlet tube (W/m/K).

The heat transfer between the nodes inside the outlet tube is simply given by:

$$H_x = \frac{A_x}{\frac{(w/2)}{k_{HT9}} + \frac{(w/2)}{k_{HT9}}} = \frac{A_x \times k_{HT9}}{w} \quad (66)$$

where A_x is the heat transfer area appropriate for the specific node. Eq. (66) can also be used for the nodes within the duct, with careful considerations that areas are correctly calculated. The heat transfer between the outermost outlet tube node and the innermost reservoir node, as well as between the outermost reservoir node and the innermost duct node, is given by:

$$H_{KHT9} = \frac{A \times k_{HT9} \times k_K}{wk_K + gk_{HT9}} \quad (67)$$

where g is the radial thickness of the nodes inside the reservoir and k_K the conductivity of the reservoir liquid (assumed to be Potassium). The heat transfer between the nodes inside the reservoir are given by:

$$H_y = \frac{A_y}{\frac{(g/2)}{k_K} + \frac{(g/2)}{k_K}} = \frac{A_y \times k_K}{g} \quad (68)$$

Finally, the heat transfer between the outermost duct node and the bypass coolant flow is given by:

$$H_{z+1} = \frac{A_z}{\frac{1}{h_c} + \frac{(g/2)}{k_{HT9}}} \quad (69)$$

where h_c must be calculated for the flow conditions of the bypass flow. The transient temperatures of the nodes can be calculated using the general equation of the form:

$$MC_p \times \frac{d}{dt} T(t) = Q_{in} - Q_{out} \quad (70)$$

where M is the mass of the component (kg), C_p is the heat capacity at constant pressure (J/kg/K) and Q_{in} - Q_{out} is the net heat transfer rate into the component (W).

For the innermost node of the outlet tube, closest to coolant, eq. (70) can be written as:

$$\begin{aligned} M_1 * C_{p_{HT9}} \times \frac{d}{dt} T_{T1}(t) &= H_1 [T_{Na} - T_1(t)] - H_2 [T_{T1}(t) - T_2] \\ \frac{d}{dt} T_1(t) &= T_1(t) \left(-\frac{H_1 + H_2}{M_1 * C_{p_{HT9}}} \right) + T_2 \left(\frac{H_2}{M_1 * C_{p_{HT9}}} \right) + T_{Na} \left(\frac{H_1}{M_1 * C_{p_{HT9}}} \right) \end{aligned} \quad (71)$$

The rest of the temperatures until the outermost node of the duct wall are defined as:

$$\begin{aligned} M_x * C_{p_y} \times \frac{d}{dt} T_x(t) &= H_x [T_{x-1} - T_x(t)] - H_{x+1} [T_x - T_{x+1}] \\ \frac{d}{dt} T_x(t) &= T_{x-1} \left(\frac{H_x}{M_x * C_{p_y}} \right) + T_x(t) \left(-\frac{H_x + H_{x+1}}{M_x * C_{p_y}} \right) + T_{x+1} \left(\frac{H_{x+1}}{M_x * C_{p_y}} \right) \end{aligned} \quad (72)$$

where y is either outlet tube or duct (HT9) or reservoir liquid (K) depending on where the node is. The last heat transfer, assuming no heat exchange with the bypass flow, is:

$$\begin{aligned} M_z * C_{p_{HT9}} \times \frac{d}{dt} T_z(t) &= H_z [T_{z-1} - T_z(t)] \\ \frac{d}{dt} T_z(t) &= T_{z-1} \left(\frac{H_z}{M_z * C_{p_{HT9}}} \right) + T_z(t) \left(-\frac{H_z}{M_z * C_{p_{HT9}}} \right) \end{aligned} \quad (73)$$

If heat exchange to the bypass flow is included, the equation changes to:

$$M_z * C_{p_{HT9}} \times \frac{d}{dt} T_z(t) = H_z [T_{z-1} - T_z(t)] - H_{z+1} [T_{Na} - T_z(t)]$$

$$\frac{d}{dt} T_z(t) = T_{z-1} \left(\frac{H_z}{M_z * C_{p_{HT9}}} \right) + T_z(t) \left(-\frac{H_z + H_{z+1}}{M_z * C_{p_{HT9}}} \right) - T_{Na} \left(\frac{H_{z+1}}{M_z * C_{p_{HT9}}} \right) \quad (74)$$

where H_{z+1} is the heat transfer between duct and bypass coolant, calculated in a similar manner as that between the internal coolant and outlet tube.

The main approximations in this model are:

- Geometry treated as cylindrical
- No natural circulation modelled inside the reservoir
- No heat loss from the flowing coolant included

The temperature dependence of thermal conductivity and heat capacity is typically ignored and the values set to constants appropriate for the temperatures spans seen in the modelling, but there is a possibility to update these values at each time-step if requested.

The heat-up of the liquid in the reservoir is delayed due to the coolant outlet tube and the temperature change in the liquid is slowed down by the outer assembly duct steel. From a time-response perspective, it is beneficial to minimize the thickness of both of these components. Since liquid metal coolants are not pressurized other than the hydrostatic pressure and the pressure given from the primary coolant pumps to the coolant, the thickness requirement for the coolant outlet tube and above-core duct are quite low (in contrast to the much higher pressure experienced by the inlet tube). Liquid metal reactors have coolant pressure drops in the total primary cycle of up to about 1 MPa, ~70-80% of which is lost within the fuel bundle below the outlet tube (Qvist, Optimization method for the design of hexagonal fuel assemblies, 2015). The resulting maximum pressure differential across the coolant outlet tube is on the order of ~300 kPa. Using the ASME code for pressurized tubes (ASME, 2005) and applying a highly conservative maximum allowable long-term stress of 50 MPa for HT9 steel, the minimum required thickness for the outlet tube is:

$$T_x = \frac{2.5 \times D_{COT}}{2P + 5\sigma_{max}} = \frac{2.5 \times 0.3 \times D_{COT}}{2 \times 0.3 + 5 \times 50} = 0.003 \times D_{COT} \quad (75)$$

This means that for a 10 cm diameter tube, the required thickness to reduce stress to acceptable long-term values is a mere 0.30 mm. While such small thicknesses are not

realistic for structural components of a fuel assembly, this result indicates that a thickness as low as 2 mm would be acceptable, also accounting for any corrosion that may occur. Lateral ribs may be used to enhance both structural rigidity and heat transfer rates in these components.

A sample calculation for an ARC system installed in a sodium-cooled reactor is shown in Figure 15. In this simulation, the coolant is at full flow conditions and the outlet temperature is changed instantaneously from 500°C to 550°C, at time 0s, then reduced from 550°C down to 500°C at 30s and finally increased up to 550°C at 60 seconds. The ARC reservoir in this example is 15 centimeters long and contains 0.93 liters of potassium. The coolant outlet tube and duct are made out of HT9 steel with a thickness of 2.0 mm.

Figure 15, Upper reservoir response to step changes in coolant temperature

The volumetric-averaged temperatures of this simulation are shown in the solid lines of Figure 16, while the dashed lines give the temperatures at each calculation node. This simulation was run with 30 radial nodes in the outlet tube, 100 nodes in the reservoir and 40 nodes within the duct.

Figure 16, Reservoir response to step-changes in coolant temperature

The temperature response of the outlet tube is very rapid, while the volume-averaged temperature of the reservoir liquid (which is the parameter of importance for ARC) shows a larger time lag. To quantify the time-lag between coolant and reservoir temperature, we introduce a single lag-parameter (τ , τ) defined as the value that best fits the simulation of a single step change in coolant temperature using the following type of equation:

$$T_R(t) = T_{Na} + (T_R(0) - T_{Na}) \times e^{-\frac{t}{\tau}} \quad (76)$$

where T_R is the volume-averaged temperature of the reservoir liquid and T_{Na} is the step-changed value of the coolant outlet temperature. The unit of τ is seconds, and after τ -seconds has elapsed, $1-e^{-1} \sim 63\%$ of the difference between the two temperatures has been erased. While this is a rather crude way of quantifying the time-lag, it provides a useful tool to analyze design alterations. The difference between the simulation results and the tau-approximation for an instantaneous change in coolant temperature of $+50^\circ\text{C}$ can be seen in Figure 17.

Figure 17, Simulation results and tau-approximation

The sensitivity of the time-lag to the thicknesses of the coolant outlet tube and the outer duct walls of the upper reservoir were studied by fixing one component to 2.0 mm and varying the other. The results, as seen in Figure 18, show a greater sensitivity to the thickness of the duct due to its relatively larger mass.

Figure 18, Duct and tube thickness effect on time-lag

It is clear that the reference geometry of the upper reservoir, while relatively simple and easily manufacturable, is far from optimal from a heat transfer perspective. Ideally, the reservoir should be heated from “both sides”, but the bypass flow area of a typical assembly geometry is far too small to have an impact on system temperatures even at very high flow velocities. “Annular” reservoir geometries that make use of significant flow and heat transfer from both inside and outside are under

active development. A preliminary conceptual design of such a system is shown in Figure 19.

Figure 19, Concept of an annular upper ARC reservoir for improved heat transfer

4.2. Heat transport delay and heat-up of above-core structures

In addition to the direct time delay due to heat transfer (covered in the previous section), there are two other inter-connected factors that works to further delay communication of the coolant temperature in the core to the temperature of the expansion liquid in the upper ARC reservoir: the coolant transfer time (1) and the thermal inertia of above-core structures (2). The transfer time for the coolant (τ_h) is simply the time it takes for the coolant to flow from the top of the active core to the location of the upper ARC reservoir:

$$\tau_h = \frac{v}{L_t} \quad (77)$$

At full flow in a sodium-cooled reactor, this time delay is short since coolant flow velocity is typically 6-12 m/s, and the axial distance between the core and upper ARC reservoir is 1-3 meters, giving a value for τ_h of less than 0.5 seconds. However, in heavy liquid metal cooled reactors and, most importantly, in the later stages of a loss-of-flow transient, the transport time can grow large and is increasingly important.

As the coolant flows out of the core and up toward the upper ARC reservoir, it will either be cooled or heated by the above-core structures and thereby change its temperature before exchanging heat with the upper ARC reservoir. The impact of the lag that is introduced by this fact is, just as for τ_h , strongly dependent on the coolant velocity. At full flow conditions, the temperature change of the coolant in the core is effectively communicated to both the upper structure and the ARC reservoir with minimal lag. Depending on the thermal inertia of the above-core structures located between the top of the core and the bottom of the upper ARC reservoir, significant temperature lag can be induced at low flow conditions, which means that in a transient with low flow conditions where the core coolant temperature increases quickly, the bulk of this temperature increase is initially transferred not to the ARC reservoir but to heat up above-core reflector and shield rods. Since the impact of this delay is directly dependent on the specific geometry and materials of the above-core structures, there is no way to provide a generic and general expression to evaluate this. Any transient analysis that includes an ARC-system must accurately model the heat transfer between the coolant and the structures/materials located above the core – simply assuming that the upper ARC reservoir coolant inlet temperature is equal to the coolant outlet temperature can lead to severe and non-conservative errors at the later stages of loss-of-flow accidents.

5. Material selections

5.1. Introduction

The recommended material selections for the ARC system have already been reported in ref. (Qvist & Greenspan, An Autonomous Reactivity Control System for Improved Fast Reactor Safety, 2014), and are in summary:

- Expansion liquid: Potassium [K]
(With potential alternatives: Indium, Caesium, and Rubidium)
- Absorber liquid: Lithium [Li]
- ARC structural material: Same steel as cladding and/or duct
(HT9, T91, D9, etc.)

In this section, additional considerations are discussed and a review of the available and needed material data for the chosen materials is performed.

5.2. The mutual solubility of potassium and lithium

One of the requirements for the ARC system to function as designed is that the expansion and absorber liquids maintain axial stratification (*i.e.* do not mix) throughout the temperature range of interest. In 1939, *Böhm and Klemm* found that

lithium and potassium do not alloy and there are neither intermetallic compounds nor solid solutions in the Li-K system using thermal analysis (Böhm & Klemm, 1939). Two published data sets for the mutual liquid solubility of lithium and potassium exists: that of *Dotson & Hand* (1970) (Dotson & Hand, 1970) and that of *Smith* (1974) (Smith, 1974).

The data of *Smith* (1974) were obtained by equilibrating 15 g of Li with 75 g of K in a Mo crucible held in an inert atmosphere. The Li had been purified by treating it with Zr in a Ti crucible at 800°C for more than 100 h. The K had been vacuum distilled at 250°C. Although complete impurity levels were not reported, chemical analyses showed the Li and K to have oxygen concentrations of 62 and 51 ppm, respectively. The results of ref. (Smith, 1974) were not tabulated, but the paper gives the following correlations (here re-written from their original base-10 logarithmic form and temperature converted to Celsius) as a fit to the data:

$$S_{Li \rightarrow K} = \exp\left(14.022 - \frac{4229.9}{T + 273}\right) \text{ [wt. ppm]} \quad (78)$$

$$63^\circ\text{C} \leq T \leq 500^\circ\text{C}$$

$$S_{K \rightarrow Li} = \exp\left(12.664 - \frac{3136.1}{T + 273}\right) \text{ [wt. ppm]} \quad (79)$$

$$181^\circ\text{C} \leq T \leq 600^\circ\text{C}$$

In atom fraction units, the solubility of lithium in potassium is always larger than the solubility of potassium in lithium. According to *Bale* (1988) (Bale, 1989), eq. (64) and (77) can be re-written to represent atom fractions instead as:

$$S_{Li \rightarrow K} = 691.86 \times \exp\left(-\frac{4229.9}{T + 273}\right) \text{ [at. \%]} \quad (80)$$

$$63^\circ\text{C} \leq T \leq 500^\circ\text{C}$$

$$S_{K \rightarrow Li} = 5.62 \times \exp\left(-\frac{3136.1}{T + 273}\right) \text{ [at. \%]} \quad (81)$$

$$181^\circ\text{C} \leq T \leq 600^\circ\text{C}$$

The measurements of *Dotson & Hand* (1970) (Dotson & Hand, 1970) were made by equilibrating 377 g of K with 274 g of Li in a 304SS-type container maintained in an inert atmosphere. The metals had been purified by filtration, hot trapping, and vacuum

distillation. A complete chemical analysis revealed a total impurity content of less than 500 ppm wt.%, including 31 and 6 ppm of oxygen in the Li and the K metals, respectively. According to *Bale (1988)* (Bale, 1989), the authors stated that their results were in fair agreement with the un-published data at 540°C by Tepper at the Mine Safety Appliance Research Company.

Eq. (64) & (80), based on the data from ref. (Smith, 1974) agrees reasonable well with the data of ref. (Dotson & Hand, 1970) (in which there are only three data points for Li in K) for temperatures from 427°C up to 538°C. Unfortunately, no published experimental data has been found for the solubility of Li in K above 538°C.

For K in Li, the data is more scattered, and eq. (79) and (81) based on the data from ref. (Smith, 1974) poorly represents some of the data of ref. (Dotson & Hand, 1970). At 538°C the two sources agree and the relative difference between eq. (81) and the value of ref. (Dotson & Hand, 1970) is less than 6%, while at 666°C the difference is more than a factor of 2.

In 2006, *Zhang* modeled the K-Li system using the CALPHAD approach as a part of a doctoral dissertation (Zhang, 2006), and the resulting phase diagram along with the data of ref. (Smith, 1974) is shown in Figure 20.

Figure 20, The K-Li phase diagram as calculated by Zhang (2006) (**Zhang, 2006**)
(data points from Smith (1974) (**Smith, 1974**))

It is clear that much more extensive experimental data on the K-Li system to higher temperatures is required. However, based on the available data and simulations, a few conclusions can be drawn:

- The two liquids are expected to remain essentially immiscible throughout their respective liquid temperature ranges, with their relative mixing peaking at the boiling temperature of potassium.
- The atomic fraction of K in Li remains below 1% up to 700°C
- The risk for loss of Li to K accelerates at temperatures above about 550°C

Coolant inlet temperatures seen in liquid metal cooled reactor designs are typically in the range from 250°C (in lead-bismuth eutectic, LBE, cooled designs) up to a maximum of about 420°C (in certain lead-cooled designs). Sodium-cooled reactors are typically designed for a coolant inlet temperature in the span 350-395°C. Since the temperature of the lower ARC reservoir is identical to the coolant inlet temperature at all times except for during a transient, the lithium and potassium in the reservoir will

mix approx. according to equations (64) and (77). The expected mixing that will occur in these different reactor types are summarized in Table 1.

Table 1, Expected liquid mixing in lower reservoir for different reactor types

Type of reactor	T_{in} (°C)	$S_{Li \rightarrow K}$ (wt.%/at.%)	$S_{K \rightarrow Li}$ (wt.%/at.%)
LBE (low)	250	0.04 / 0.21	0.08 / 0.014
Sodium (standard)	350	0.14 / 0.78	0.21 / 0.037
Sodium (high)	395	0.22 / 1.23	0.29 / 0.051
Lead (high)	420	0.27 / 1.55	0.34 / 0.061

In a typical sodium-cooled reactor design ($T_{in}=350^{\circ}\text{C}$), about 0.14 wt.% (or 1/700th) of the lithium mass that is loaded into the lower reservoir will go in solution in the potassium. Within the lithium-rich liquid solution, potassium will make up about 0.21 wt.% (or 1/500th) of the mass once equilibrium has been reached. While these values appear small, they should be accounted for explicitly when determining the required total amount of lithium loaded in the system and the reactivity worth of the ARC actuation. Another issue is the lithium in the potassium-rich liquid may have some impact on core reactivity during standard operation. The inner ARC-tube, which goes axially up through the entire active core, is filled at all times with potassium. Up to 0.1% of the core volume can be made up of inner ARC tube potassium. If we assume the solubility limit for lithium in potassium is reached everywhere in the system, up to about 2 at. % of the liquid in the inner ARC tube could theoretically be lithium. This gives an upper bound of lithium volume fraction inside the active core from the non-zero miscibility at approx. 0.002 vol. %.

As temperatures rise, the problem of mixing in the liquids rapidly becomes increasingly severe. At a temperature of 700°C, which is a reasonable peak allowable temperature during a transient, the correlations, while technically not valid at this temperature, indicate that the liquids may mix by several wt. %. Fortunately, this rapid increase in solubility is not a big concern to the operation of the ARC system:

1. Once the system is actuated, the interfacial area between the two liquids is miniscule (the annulus area between the ARC-tubes), making the mixing process at high temperatures very slow. Even with a large area for the liquids to interface, it typically takes several hours to reach equilibrium.
2. The location where the two liquids interface remains below the active core during all transients. Therefore, the temperature at the interface will be more closely correlated to the coolant inlet temperature.

3. It is only in the unprotected loss of heat sink (ULOHS) transient event that we see elevated coolant inlet temperatures for an extended period of time. If we assume the absolute worst for the long-term state following a ULOHS event, the theoretical solubility is 1.6 wt.% Li in K, and 1.3 wt.% K in Li (at 700°C).
4. As temperatures go back down following a transient, the solubilities fall back as well, causing the two liquids to de-mix and return to their original separated states.

While it appears that pure lithium and pure potassium will function as ARC liquids as required, it may be preferential to identify and introduce impurities or alloying materials to both liquids that will cause the bulk liquids to maintain complete immiscibility throughout the liquid temperature range. Using either pure or “optimized” liquids will require an extension of the available experimental data for mutual solubility up to at least 700°C.

5.3. Properties of ARC materials

5.3.1. Introduction

The material properties strictly required for the analysis of an ARC system are:

- Melting temperature
- Density (also gives volumetric expansion)
- Heat capacity
- Conductivity
- Vapor pressure
- Corrosion rate (in of solids in static contact with the liquid)

For the properties of liquid lithium, the main references are three reports:

- *Davison* (1968) (Davison), “Compilation of thermophysical properties of liquid lithium”, which contains a literature review and nearly all the relevant correlations
- *Jeppson, Ballif, Yuan and Chou* (1978) (Jeppson, Ballif, Yuan, & Chou, 1978), “Lithium literature review: Lithium's properties and interactions”, which covers chemical interactions, handling and corrosion studies
- *Alcock, Chase and Itkin* (1994) (Alcock, Chase, & Itkin, 1994), “Thermodynamic properties of the Group IA elements”, which gives an extensive review of heat capacity data for all alkaline metals.

The thermophysical data for potassium are primarily collected from:

- The *Sodium-NaK Engineering Handbook* (1972) (Foust, 1972), which contains very extensive information on all relevant properties of potassium.

The coolant outlet tube should be made from the same material as the fuel assembly duct to minimize stress. While this may in principle be any fast reactor steel, in this analysis we have used the material properties of the low-swelling ferritic-martensitic steel HT9. The references for HT9 thermophysical properties are:

- *Leibowitz and Blomquist* (1988) (Leibowitz & Blomquist, 1988), “Thermal Conductivity and Thermal Expansion of Stainless Steels D9 and HT9”
- *Yamanouchi, Tamura, Hayakawa, and Kondo* (1992) (Yamanouchi, Tamura, Hayakawa, & Kondo, 1992), “Accumulation of engineering data for practical use of reduced activation ferritic steel”, in which a correlation for HT9 specific heat is given.
- The “Nuclear Systems Materials Handbook” (NSMH) by the Hanford Engineering Development Laboratory (HEDL) appears to be an excellent source of HT9 property data, but only small parts of it are publicly available. The HT9 density correlation used for ARC analysis was developed from NSMH data.

5.3.2. Lithium

The melting temperature of pure liquid lithium from the Handbook of Chemistry and Physics (Weast, 1976), as recommended by ref. (Jeppson, Ballif, Yuan, & Chou, 1978), is:

$$T_{mLi} = 180.54 [^{\circ}\text{C}], 453.70 [\text{K}] \quad (82)$$

The recommended density-correlation for liquid lithium, based on a fit to data from several references in *Davison* (1968) (Davison) is:

$$\rho_{Li} = 562 - 0.1 \times T(\text{K}) \left[\frac{\text{kg}}{\text{m}^3} \right] \quad (83)$$

For the heat capacity of liquid lithium, there are significant differences between sources. *Davison* (Davison) tries to resolve this by assigning a constant value for the heat capacity, while the very extensive review work by *Alcock et. al* (Alcock, Chase, & Itkin, 1994) specifically recommends the correlation of ref. (Douglas, Epstein, Dever, & Howland, 1955). In our modeling of the ARC, we follow this recommendation, defined as:

$$T_{m_{Li}} < T < 700K$$

$$C_{p_{Li}} = 4759.4 - 0.838 \times T(K) \left[\frac{J}{kgK} \right] \quad (84)$$

$$700 \leq T < 1600K$$

$$C_{p_{Li}} = 4227.3 - 0.072 \times T(K) \left[\frac{J}{kgK} \right] \quad (85)$$

The correlation for thermal conductivity we use is that presented in the *Handbook of thermodynamic and transport properties of alkali metals* (1985) (Ohse, 1985), which more recently was recommended for use by *Zinkle* (1998) (Zinkle, 1988):

$$455 < T < 1500K$$

$$k_{Li} = 22.28 - 0.05 \times T(K) - 1.243 \times 10^{-5} T(K)^2 \left[\frac{W}{mK} \right] \quad (86)$$

The correlation for vapor pressure used is that recommended by *Davison* (1968) (Davison):

$$800 < T < 1800K$$

$$P_{Li} = \exp \left(23.06 - \frac{18569.2}{T(K)} \right) [Pa] \quad (87)$$

The boiling temperature of lithium is thus given as:

$$T_{b_{Li}} = \frac{18569.2}{23.06 - \ln(P[Pa])} [K]$$

Finally, all types of steel used in fast reactors are corrosion resistant to static liquid lithium at relevant temperatures as seen in the lower reservoir (300-400°C) (Jeppson, Ballif, Yuan, & Chou, 1978).

5.3.3. Potassium

The melting temperature of potassium is (Alcock, Chase, & Itkin, 1994):

$$T_{m_K} = 63.6 [^{\circ}C], 336.6 [K] \quad (88)$$

The density of liquid potassium is (Foust, 1972):

$$\begin{aligned} T_{m_K} < T < 1250^{\circ}\text{C} \\ \rho_K = 0.8415 - 2.172 \times 10^{-4} \times T(^{\circ}\text{C}) - 2.70 \times 10^{-8} \times T(^{\circ}\text{C})^2 \\ + 4.77 \times T(^{\circ}\text{C})^{-12} \left[\frac{\text{g}}{\text{cm}^3} \right] \end{aligned} \quad (89)$$

The heat capacity of liquid potassium is (Foust, 1972):

$$\begin{aligned} T_{m_K} < T < 1150^{\circ}\text{C} \\ C_{p_K} = 838.47 - 0.3672 \times T(^{\circ}\text{C}) + 4.5899 \times 10^{-4} \times T(^{\circ}\text{C})^2 \left[\frac{\text{J}}{\text{kg}^{\circ}\text{C}} \right] \end{aligned} \quad (90)$$

The vapor pressure for liquid potassium is given by (Bowles, 1968):

$$P_K = 0.1013 \times \exp\left(\frac{17.843T - 11081 - 1.026 \times \ln(T) \times T}{T}\right) \quad (91)$$

The boiling temperature of potassium can be calculated using data from eq. (91) as:

$$T_{b_K} = 586.1 \times P(\text{MPa})^{0.3289} + 764.1 [K] \quad (92)$$

The liquid potassium thermal conductivity can be well represented by the following correlation (Foust, 1972):

$$\begin{aligned} 100 < T < 900^{\circ}\text{C} \\ k_K = 56.16 \times \exp(-7.958 \times 10^{-4} T(^{\circ}\text{C})) \left[\frac{\text{J}}{\text{m}^{\circ}\text{C}} \right] \end{aligned} \quad (93)$$

5.3.4. HT9 steel

A curve-fit was made from the data points of HT9 density measurements from the “Materials Handbook for Fusion Energy Systems” that was made publicly available with the PRISM reactor Preliminary Safety Information Document (PSID) (General Electric, 1987). An excellent fit to the density data is obtained by the following correlation:

$$\rho_{HT9} = 7824 - 9.288 \times 10^{-5} T - 0.1859 T^2 \left[\frac{\text{kg}}{\text{m}^3} \right], 298 \leq T \leq 923K \quad (94)$$

The thermal conductivity and specific heat is given by (Leibowitz & Blomquist, 1988) (Yamanouchi, Tamura, Hayakawa, & Kondo, 1992):

$$k_{HT9} = \begin{cases} 17.622 + 2.42 \times 10^{-2}T - 1.696 \times 10^{-5}T^2, T < 1030K \\ 12.027 + 1.218 \times 10^{-2}T, T \geq 1030K \end{cases} \left[\frac{W}{m \times K} \right] \quad (95)$$

$$C_{p_{HT9}} = \begin{cases} \frac{T - 600}{6} + 500, T < 800K \\ \frac{3(T - 600)}{5} + 550, T \geq 800K \end{cases} \left[\frac{J}{kg \times K} \right] \quad (96)$$

With sufficiently low oxygen concentration, steel corrosion rates due to exposure to the static potassium appears to be completely negligible at all relevant temperatures of the system (<759°C) (Fraas, 1967) (DeVan, 1966) (Young & Grindell, 1967) (Grisaffe & Guentert, 1974).

6. The ARCAD code and output examples

The ARC reservoir and tube design process has been completely automated through the ARC Automatic Designer (ARCAD) code-package, the base of which essentially consists of the equations of this article, written in Python code. The general structure of the ARCAD code is shown in Figure 21 and represents the solution method for any ARC implementation.

Figure 21, Main functions and structure of the ARCAD code

For ARCAD to run, it requires extensive information about the core design and the desired characteristics of the ARC system actuation. The principles and methods for determining these characteristics are presented in the accompanying Part II paper, so for the moment they are assumed known. To estimate the reasonable range of values for important parameters (ARC system length, reservoir volumes etc.) in a typical fast reactor system, the reactivity worth of lithium was calculated in a model of the MOX-fueled Advanced Burner Reactor (ABR) (Hoffman, Yang, & Hill, 2006) with the Serpent 2.1.24 code (Leppänen, Pusa, Viitanen, Valtavirta, & Kaltiaisenaho, 2015)

using ENDF/B-VII.0 cross-sections (U.S. Cross Section Evaluation Working Group (CSEWG), 2006).

In this specific version of the ABR, the core volume fraction of one fuel rod (including cladding) is 0.23%. Neutron transport calculations were made by replacing one fuel rod in each assembly with lithium, ranging from natural composition to 90% enriched in ^6Li , which gives the following linear correlation for Δk_{ARCA} :

$$\Delta k_{ARCA}(\$) = -1.282 - 0.17 \times [\% \text{ } ^6\text{Li}] \quad (97)$$

$$\text{for } 7.5\% \leq [\% \text{ } ^6\text{Li}] \leq 90\%$$

Actuation calculations (explained in Part II of this article series) indicates a need for a total reactivity worth of the ARC system in this core of about -1.5\$ with an actuation span between $T_{ac} = 595^\circ\text{C}$ and $T_f = 700^\circ\text{C}$, in order for the core to survive unprotected transients such as loss of flow, loss of heat sink and transient overpower. Using a highly enriched absorber (90% ^6Li) will require the least amount of absorber liquid and the smallest impact on the overall design. At 90% ^6Li (-\$16.5 per % $_{\text{Li}}$) in the absorber, the system will require ~0.1% of the core volume to be covered by absorber at full actuation. Feeding the input values of Table 2 to the ARCAD code produces a suggested design for the ARC system as described in Table 3-Table 5. The pressure drop added by the system and the time delay characteristics are summarized for 4 different flow conditions in Table 6.

The total axial length increase to the fuel assembly by this ARC installation is $L_1 + L_2 = 40.7$ cm. Out of this, 25.5 cm house liquids, 5.2 cm is for the gas reservoir and the remaining 10 cm is the additional shielding at the top of the lower reservoir. At full flow, the reservoir adds about 0.5 kPa of pressure drop.

The peak allowable system pressure is set at 1 MPa (significantly lower than what is allowed in the fuel rods), calculated at a full actuation of the system at “end of life”. For this calculation, the estimation of the developed pressure was made on the basis that the residence time of a fuel assembly is 2118 days. The estimated volume-averaged flux level in the absorber within the shielded lower reservoir is 10^{13} n/cm²/s, giving an estimated fluence received of 1.8×10^{21} n/cm² (disregarding any actuation transients).

Table 2, Actuation characteristics and input parameters for ABR CR=0.75 MOX

Parameter	Value	Description
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U_{ARC}	-150¢	ARC total reactivity worth
ΔT_{ac}	50°C	Temperature rise for absorber to reach core
T_{ac}	595°C	Temperature in upper ARC reservoir when absorber reaches core
ΔT_{f}	105°C	Temperature rise of actuation
T_{f}	700°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 neutron volume-averaged neutron fluence in the absorber liquid in the lower reservoir

Table 3, ARC tube geometry

Parameter	Value	Description
N_{ARC}	1	Number of ARC rods per fuel assembly
V_{f}	0.091%	The volume fraction of the core covered in ARC absorber at full actuation (-16.5\$/% _{Li} * 0.091 = -150 cents)
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}	10	The number of cylindrical wires (with a diameter of 1 mm) that are wrapped around the inner ARC tube in the below-core region
W_{p}	48.1 cm	The axial length of one full rotation of the wires

Table 4, Upper ARC reservoir

Parameter	Value	Description
V_{UR2}	828.3 cm ³	The volume of liquid in the upper ARC reservoir
FTF_i	15.3 cm	Assembly inner flat-to-flat distance
D_{CUT}	14.3 cm	Coolant outlet tube outer diameter
D_{CUTi}	13.9 cm	Coolant outlet tube inner diameter
L_{UR2}	19.66 cm	Axial length of the liquid-filled region of the upper ARC reservoir
V_{UR1}	160 cm ³	Volume of gas reservoir

L_{UR1}	5.2 cm	Axial length of the gas reservoir section of the upper ARC reservoir
L_{UR}	24.9 cm	Total axial length of upper ARC reservoir

Table 5, Lower ARC reservoir

Parameter	Value	Description
V_{LR1}	724.4 cm ³	The volume for liquid and gas in the lower ARC reservoir
D_{CIT}	10.0 cm	Coolant outlet tube outer diameter
D_{CITi}	9.0 cm	Coolant outlet tube inner diameter
L_{LR1}	5.8 cm	Axial length of the liquid-filled region of the upper ARC reservoir
L_{LR2}	10.0 cm	Axial length of shielding piece above lower reservoir
L_{LR}	15.8 cm	Total axial length of lower reservoir
m_a	30.0 g	Mass of absorber loaded in the system per assembly

Table 6, Pressure drop and time constants

Parameter	Flow condition	Value
Added pressure-drop by ARC	Full flow	450 Pa
	50% flow	125 Pa
	25% flow	35 Pa
	5% flow	2 Pa
Approx. heat transfer time constant (tau)	Full flow	3.22 s
	50% flow	3.44 s
	25% flow	3.75 s
	5% flow	4.75 s

While different core designs will require different ARC implementations, the order of magnitude for the variables of importance for core performance has been established through parametric studies using the ARCAD code. It is clear that even for the largest installations of ARC, for instance for use in large, high-power density, high-burnup oxide-fueled sodium-cooled cores, the ARC systems rarely exceeds a combined total length of 50 cm for both reservoirs (or about 10% of the total assembly length), and the impact on the pressure drop and pumping power in the primary system is negligible.

7. Manufacture and assembly process

The ARC system design requires some changes to the conventional fuel assembly manufacturing procedure. The added difficulty stems primarily from the fact that the outer ARC tube needs to be attached (welded) to both the ARC reservoirs and the length and attachment of this tube is different from that of the rest of the fuel rods.

Rods in a typical fuel assembly are attached to, and stand on, pin mounting rails. The mounting rails are thin sheets with an enlarged upper edge that span from one side of the assembly to the other. Key-hole slots in the lower end plugs of the fuel rods slide onto the rails and are trapped by a bulging upper rail edge in the upper end enlargement of the key-hole slot. The mounting rails are positioned by rail pins that pass through the lower area of the rails (below the keyhole), transverse to the rails. The rail pins pass through the nosepiece at each end, which positions the rails with respect to the nosepiece to prevent axial motion. An ARC'ed assembly requires a modified mounting rail and rail pin structure that omits one or more of the rod positions (depending on the number of ARC-tubes used).

To assemble the ARC-type design, the components of the assembly are subdivided (and pre-assembled individually) in to the following pieces:

- The nose piece, which contains the lower ARC reservoir and the complete outer ARC-tube (pre-welded to the lower reservoir). The lower ARC reservoir contains, in solid state, the appropriate amount of absorber and expansion materials.
- The pin mounting rail grid for the rod bundle, with a slightly modified arrangement to allow for one or more ARC-tubes to pass through.
- Stabilization bars (at least 4) that support the mounting grid.
- The fuel rod bundle, missing one or more pins to make room for the ARC tube(s).
- The ARC upper gas reservoir piece, with an opening in the bottom allowing for the outer ARC-tube to enter (and be welded on). The gas reservoir piece is open at the top.
- The upper ARC liquid reservoir, which is fully enclosed (its bottom surface forms the top surface of the gas reservoir below) with the full-length inner ARC tube pre-welded on. The upper ARC reservoir is completely filled with ARC expansion material (in solid state), and has fins spreading out in two for positioning with the duct.
- A “lower duct” piece, long enough to go from the nose-piece to connecting with the fins of the upper liquid reservoir.

- The remaining “upper duct” section (covering the region of the upper ARC-reservoir) and handling socket.

The pieces are shown in axial order from the bottom in Figures Figure 22-Figure 32, with welds (pre-made before assembly of the separate pieces) marked in red. The stabilization bars, which are simple steel cylinders, are not shown separately.

Figure 22, Nosepiece of ARC'ed assembly

Figure 23, Cut-through view of the upper part of the nosepiece. The figure shows the lower reservoir and its shielding region as well as the attachment of the outer ARC-tube

Figure 24, The pin mounting rail. This has been modified to accommodate more stabilization rods (4 in total) and a single ARC-tube

Figure 25, The rod bundle consists of the lower end plug, cladding, fuel (inside the cladding) and upper end plug, and wires wrapped around the rods for spacing.

Figure 26, The upper gas reservoir is open at the top and closed at the bottom and includes a hole through which the outer ARC tube is mounted and welded.

Figure 27, Isometric cut-through view of the hexagonal duct section.

Figure 28, At the top of the lower duct section, there are two reservoir positioning slots on opposite sides, into which the slits attached to the upper liquid reservoirs are inserted from above.

Figure 29, The upper ARC (liquid) reservoir is located on top of the gas reservoir. It has two duct-attachment slits protruding from its sides, and the inner ARC tube is welded through the bottom surface.

Figure 30, Cut-through view of the upper ARC (liquid) reservoir showing the attachment of the inner ARC tube.

Figure 31, Isometric view of the upper duct section and handling head.

Figure 32, Cut-through iso-view of the upper duct section and handling head. The thicker uppermost section of the handling head extends beyond the outer limit of the duct to form the top load pad.

The components and assembly steps for the ARC system were developed for the more advanced design with an annular upper reservoir, since this design options shows more promise. The assembling steps for an ARC'ed assembly of this type are in summary:

- 1) The mounting rails pins are inserted into their slots in the nose piece and the stabilization rods are inserted in their slots through both the pin mounting rails and the nose piece.
- 2) The bottom end of the lower duct piece is welded to the nosepiece.
- 3) The open gas reservoir piece is welded onto the outer ARC-tube.
- 4) The lower duct section is welded on the nose piece.
- 5) The liquid reservoir piece is positioned on top of the gas reservoir piece and slid into its positioning slots in the duct. The inner ARC tube which is pre-welded to the liquid reservoir is inserted into the outer ARC tube through the gas reservoir. The liquid reservoir piece is then welded on (outside and inside) to the gas reservoir piece.
- 6) Finally, the upper duct piece & handling head piece is welded to the lower duct piece.

The assembling steps are shown in Figure 33-Figure 38 with welds (made during the assembly of the separate pieces) marked in red.

Figure 33, Step #1. The pin mounting rails and stabilization bars are inserted into their respective slots in the nose piece.

Figure 34, Step #2. The rod bundle (containing the pre-fabricated complete fuel rods and wire-wraps) is slid onto the pin mounting rails

Figure 35, Step #3. The gas reservoir is welded to the outer ARC tube.

Figure 36, Step #4. The lower section of the duct is welded to the nose-piece

Figure 37, Step #5. The upper liquid is positioned on top of the gas reservoir, with the inner ARC tube inserted into the outer ARC tub and the positioning fins inserted into the duct slots. The liquid reservoir is welded onto the gas reservoir at the inner and outer edges.

Figure 38, Step #6. The uppermost section, consisting of the upper duct section and the handling head, is welded onto the lower duct section.

A view of all components assembled (with the ducts and handling head transparent) is shown in Figure 39.

The most challenging step of the manufacturing of an ARC'ed assembly is the inner edge weld between the gas reservoir and the upper liquid reservoir. This may present a challenge to the welder if the coolant outlet tube diameter is relatively small and the gas and liquid reservoirs are long. All the other operations can be carried out using only slightly modified fuel assembly manufacturing equipment, but several design alterations will be studied that could potentially be made to significantly reduce the complexity of assembling the system. The reservoir connection & positioning piece installed in the duct (see Figure 28) allows the reservoirs to travel axially, thus reducing the risk of breaking the outer ARC tube from thermal stress as its temperature changes.

Figure 39, Iso-view of final assembly (with transparent ducts & handling head)

8. Conclusions and future work

In this paper, the general principles regarding the design and function of ARC system have been laid out. The main contribution of this work is the development of the capability to estimate the sizes of the components and the time delay of actuation of a typical ARC system and its impact on core performance parameters such as pressure drop in a coherent way. It has been shown that a typical ARC installation constitutes a relatively minor adjustment to a typical fuel assembly, increasing its total axial extent by ~5-10% and the total primary coolant pressure drop by ~1%. While the details of the ARC system design are likely to change with future development, for instance

with the further development of an annular upper reservoir, the main principles worked out in this study still forms the theoretical foundation for its design and operation.

The most critical part of the ARC system design appears to be the heat transfer rates to the expansion liquid located in the upper reservoir. The actuation of the system lags the temperatures in the core, and if this lag become too large, there is a potential for the core and ARC temperatures to come out of phase. Future work will focus on further improving the design of each individual component of the system to improve overall performance, specifically aimed at more rapid heat transfer, as well as to simplify both analysis and manufacturing. The directions of future work that have already been identified are:

- Heat transfer rates

The design presented in this paper shows a straight cylinder tube going through the upper ARC reservoir. Large improvements in heat transfer with acceptable increases in pressure drop are expected by designing this region for improved heat transfer. Alternatives that will be studied include using “wavy” surfaces to maximize heat transfer area and using an annular reservoir with the coolant flowing over both surfaces. The simple analysis methods for temperature calculations presented here will be augmented by high-fidelity computational fluid dynamics simulations.

- Actuation speed

Several of the design improvements mentioned in the preceding paragraph are expected to increase the actuation speed. In addition, analysis will be made of the potential to design the upper reservoir walls such that they will be buckled due to thermal expansion driven by coolant temperature increase. The buckling will be in the inward direction; that is, will decrease the reservoir volume. If such a design option could be proven viable, the speed of actuation of the absorber liquid into the core could be greatly improved.

- Design complexity

Effort will be put in to try to simplify the mechanical design of the system. This is particularly true for the below-core region of the ARC-tubes, where requirements to reduce the available volume currently lead to complicated and difficult-to-manufacture designs.

- Material data

The main material data uncertainty is that of the high-temperature mutual solubility of lithium and potassium. If additional funding is made available,

the liquid stratification will be tested experimentally throughout the temperature range of interest.

9. Terminology

Parameter	Description	Unit
ξ_{fr}	Contribution to pressure drop by friction in a contracting section leading up to the upper ARC reservoir	Dimensionless
σ_a^A	One-group neutron absorption cross-section of the absorber liquid inside the lower reservoir at operation temperature	Barns (10^{-24} cm^2)
σ_{max}	Maximum allowed stress	Pa
τ_h	Time lag due to coolant flow distance between the top of the core and the upper ARC reservoir	s
Δh_{as}	Axial height of the absorber in the lower reservoir at shutdown/refueling temperature	m
Δh_{es}	Axial distance between the top of the expansion liquid in the lower reservoir at shutdown/refueling temperature and the top of the upper reservoir	m
Δk_{ARCA}	The change in effective multiplication factor per volume fraction of absorber in the active core	pcm or \$
$\Delta k_{\text{requirement}}$	The total reactivity that the ARC system must provide at full actuation	pcm or \$
Δp	Pressure drop	Pa
ΔT_{ac}	Temperature rise for absorber to reach core	K
ΔT_f	Temperature rise of actuation	K
ΔV_{A0}	The volumetric expansion of the absorber between room temperature and operating temperature	m^3
ΔV_{iARCo}	The volumetric expansion of the liquid inside the inner ARC tube between room temperature and operating temperature	m^3
ΔV_{L0}	The volumetric expansion between room	m^3

	temperature and operating temperature of the expansion liquid always present in the lower reservoir	
$\Delta V_{RT \rightarrow O}$	The total volumetric expansion that the lower reservoir has to accommodate	m^3
ΔV_{UR2o}	The volume of material expanding out of the upper reservoir into the lower reservoir when going from room temperature to operating temperature	m^3
A_{bc}	The area in the annulus between the ARC tubes in the below-core region	m^2
α	The half-angle of the opening of the contraction of the coolant outlet tube	#
A_{bc}	The cross-sectional area inside the annular region between the ARC tubes in the below-core region	m^2
A_{LR1}	The cross-sectional area inside the lower section of the lower reservoir	m^2
A_{UR2}	The cross-sectional area of the upper ARC liquid reservoir	m^2
A_w	Axially averaged area of the ARC tube spacer wire in the below-core region	m^2
C_p	Heat capacity	J/kg/K
D_A	The number of reactions producing gas from the absorber liquid during the time t	# of gas producing reactions (1 or 2 atoms per reaction)
D_{CIT}	Coolant inlet tube outer diameter	m
D_{COT}	Coolant outlet tube outer diameter	m
D_{eq}	The equivalent radius of the inner duct hexagon treated as if it were one of the surfaces of a circular frustrum	m
D_{hxd}	The helix-diameter of the ARC tube space wire	m
D_w	The diameter of the ARC tube spacer wire	m
FTF_i	Assembly inner flat-to-flat distance	m
FTF_o	Assembly outer flat-to-flat distance, including half the distance between assemblies	m
g	Standard gravity (9.81 m/s^2)	m/s^2

G_c	The gas compression factor between room temperature and full system actuation	#
h	Heat transfer coefficient	$W/m^2/K$
k	Thermal conductivity	$W/m/K$
k_{ARC}	The effective multiplication factor with ARC systems at full actuation	#
L_0	The minimum level of the interface between expander and absorber liquids (measured from the bottom of the lower reservoir)	m
L_b	Distance from the bottom of the inner ARC tube to the bottom of the active core	m
L_c	Extrapolated axial length of the active core	m
L_{iARC}	The total length of the inner ARC tube	m
L_{LR}	Total axial length of lower reservoir	m
L_{LR1}	Axial length of the lower (gas and liquid-filled) section of the lower reservoir	m
L_{LR2}	Axial length of shielding piece above lower reservoir	m
L_t	Distance from the top of the active core to the bottom of the upper reservoir	m
L_{UR2}	Axial length of the liquid-filled region of the upper ARC reservoir	m
m_a	Mass of absorber loaded in the system per assembly	kg
M_A	The molar mass of the absorber liquid	g/mol
m_{iARC}	Total mass of liquid inside the inner ARC tube at room temperature	kg
M	Arbitrary component mass	kg
m_{UR2rt}	The mass of liquid present in the upper reservoir at room temperature	kg
N_0	Initial number of absorber liquid atoms	#
N_{ARC}	Number of ARC rods per fuel assembly	Positive integer
N_{av}	Avogadro's constant (6.022×10^{23})	mol^{-1}
Nu	Nusselt number	Dimensionless
N_w	The number of cylindrical wires that are wrapped around the inner ARC tube in the below-core region	Positive integer

Pe	Péclet number	Dimensionless
p_g	Gas pressure	Pa
P_{hyd}	The hydrostatic pressure of the expansion liquid	Pa
$P_{K,Li}$	Vapor pressure	Pa
p_{max}	Maximum system pressure (at full actuation at EOL)	Pa
p_{margin}	Extra margin of gas loading requirement	Pa
Pr	Prandtl number	Dimensionless
R_A	The neutron absorption reaction rate in the absorber liquid	Reactions/s
Re	Reynolds number	Dimensionless
R_{fco}	Fuel rod outer cladding radius	Meters
R_{iARCi}	Inner ARC tube inner radius in the below-core region	Meters
R_{iARCiB}	Inner ARC tube inner radius in the below-core region	m
R_{iARCo}	Inner ARC tube outer radius in the core region	m
R_{iARCoB}	Inner ARC tube outer radius in the below-core region	m
R_{oARCi}	Outer ARC tube inner radius	m
R_{oARCo}	Outer ARC tube outer radius	m
$S_{K \rightarrow Li}$	Solubility of Potassium in Lithium	Weight or atom fraction
$S_{Li \rightarrow K}$	Solubility of Lithium in Potassium	Weight or atom fraction
t	Time	s
T_{ac}	Temperature in upper ARC reservoir when absorber reaches core	K
T_B	Boiling temperature	K
t_c	Fuel rod cladding thickness	m
T_{caf}	The average coolant temperature in the core at the full actuation state ($T_{caf} = [T_f + T_i]/2$)	K
T_f	Temperature in upper ARC reservoir at full actuation	K
T_i	The coolant inlet temperature	K
T_{iARCo}	The volumetrically averaged temperature of the liquid inside the inner ARC tube at	K

	the standard operation state	
T_m	Melting temperature	K
T_{mac}	The temperature in the upper reservoir when the absorber is halfway to the bottom of the extrapolated active core. ($T_{mac} = [T_{ac} + T_o]/2$)	K
T_{max}	Maximum analyzed system temperature	K
T_{mf}	The temperature in the upper reservoir liquid at the middle of actuation.	K
T_{Na}	Reference temperature of the coolant for a step-change evaluation of time-lag response	K
T_o	Coolant outlet temperature	K
T_r	Volume-averaged upper reservoir liquid temperature	K
T_{rt}	Room temperature (25°C)	K
T_x	Minimum required thickness of the coolant outlet tube	m
V_A	The absorber liquid volume	m^3
V_{af}	The absorber liquid volume specified at the state of full actuation	m^3
V_{ARC}	Total volume of ARC absorber liquid inside the active core at full actuation	m^3
$V_{assembly}$	The total volume of a fuel assembly (including half the inter-assembly volume)	m^3
V_{bc}	The volume inside the annular region between the ARC tubes in the below-core region	m^3
V_{core}	Total volume of active core	m^3
V_{iARC}	Total volume of liquid inside the inner ARC tube	m^3
V_f	The volume fraction of the core covered in ARC absorber at full actuation	#
V_g	Total volume of ARC gas at room temperature	m^3
V_{L0}	The volume of expansion liquid that is always present in the bottom of the lower reservoir	m^3
V_{LR1}	The volume for liquid and gas in the	m^3

	lower ARC reservoir	
V_{LRis}	The volume of the steel of the inner ARC tube inside the lower reservoir	m^3
V_{margin}	Extra volume of absorber liquid to account for depletion and uncertainties	m^3
V_t	Volume between the ARC tubes in the above-core region	m^3
V_{UR1}	Volume of gas reservoir	m^3
V_{UR2}	The volume of liquid in the upper ARC reservoir	m^3
V_{URis}	The volume of the steel of the inner ARC tube inside the upper reservoir	m^3
W_p	The axial length of one full rotation of the wires	m
U_{ARC}	ARC total reactivity worth	pcm or \$
X_g	Fraction of neutron absorption reactions in the absorber liquid that produce gaseous reaction products	#
x_r	The increase to the ARC tube outer radius in the below core region (compared to the in-core region)	m
λ	Smooth wall friction factor	Dimensionless
ρ	Component density	kg/m^3
τ	Lag parameter describing how the volume-averaged upper reservoir liquid temperature lags the coolant outlet temperature	s
ϕ	Total volume-averaged neutron flux in the absorber liquid in the lower reservoir	$n/cm^2/s$

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